TRANSACTIONS
OF THE
AMERICAN INSTITUTE
OF
ELECTRICAL ENGINEERS

VOLUME 58
1939

PUBLISHED BY THE
AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS
33 WEST THIRTY-NINTH STREET
NEW YORK, N. Y., U.S.A.

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American Institute of Electrical Engineers

Printed in the United States of America by
The Mack Printing Company, Easton, Pennsylvania

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Preface

This, the 58th volume of the Transactions of the American Institute of Electrical Engineers, is the second to be published under the revised publication policy adopted during 1937. Under this policy, the Transactions contains, together with correlated discussions, all papers presented at national conventions and those papers approved by the AIEE technical program committee for presentation at District meetings. Because of the continued effects of the transition from the former to the new publication policy, the content of this volume is somewhat less than normal.

This volume contains the 1938 and the 1939 annual reports of the board of directors in addition to papers and related discussions presented at the following AIEE District meetings and conventions:

1. 1939 winter convention, New York, N. Y., January 23–27: all papers presented.
2. South West District meeting, Houston, Tex., April 17–19: all approved papers.
3. North Eastern District meeting, Springfield, Mass., May 3–5, 1939: 4 papers; remaining 13 approved papers are scheduled for the 1940 volume.

The present publication policy also provides for the preprinting of a large proportion of the approved Transactions papers and discussions in a segregated Transactions section of the monthly issues of Electrical Engineering; extra sheets are printed at the same time for ultimate binding in the annual Transactions volume. The papers and discussions so published during the year 1939 comprise pages 1 to 674, inclusive, of this volume; pages 675 to 914, inclusive, did not appear in Electrical Engineering.

Full correlation of all material in this volume has been accomplished through the medium of the multientry reference index beginning on page 939. A reference to any of the several subject entries for any technical paper will lead directly to the paper and to any published discussion on that paper.

Statements and opinions given in papers and discussions published in Transactions are the expressions of contributors for which the American Institute of Electrical Engineers assumes no responsibility.

Errata

Page 98, figures 11 and 12: Illustrations should be interchanged.

Page 100, second line: ${}_2\delta_1$ should be δ_{21}.

Page 525, bottom of first column, top of second column: Radio Manufacturers Association should be Refrigerating Machinery Association, and American Cotton Manufacturers Association should be Air Conditioning Manufacturers Association.

Application of Capacitance Potential Devices

By J. E. CLEM
FELLOW AIEE

THERE has been evident need for some time for a general review of the factors influencing the application of capacitance potential devices. This need has been accentuated by the increasing use of bushing potential devices and the extension of the field of application made possible by the introduction of the coupling-capacitor potential device.

At the higher system voltages the cost of transformers for supplying potential for relays has been a serious deterrent to the use of the relay equipment otherwise best adapted to the requirements of the installation. The introduction of the bushing potential device, and particularly of the coupling-capacitor potential devices later, offered a more economical method of obtaining the desired potential. The first applications of bushing potential devices were for synchronizing and voltage indication, and later their use was extended to certain types of relaying. The introduction of the coupling-capacitor potential devices extended the field of application by making practical the use of a nonresonant device and by making available higher outputs. These devices made possible a wider application of the use of carrier-current relay schemes, further increasing the field for the potential devices.

Any misunderstanding of the inherent limitations of these devices may result in a failure to recognize their possibilities, or in a tendency to use the device outside its proper field of application. Therefore, this paper has been written to discuss briefly general considerations in regard to types of devices and the selection of the means of obtaining instrument or relay voltage, to review the accuracy considerations and application requirements, to describe the types of devices, and to present an analysis of the network constituting the device.

General Considerations

TYPES OF DEVICES

Capacitance potential devices are divided into two general classifications: resonant and nonresonant. The resonant device may be used with a bushing or a coupling capacitor, the nonresonant should be used only with a coupling capacitor. In general the outputs from coupling-capacitor potential devices are considerably higher than the output from bushing potential devices.

The resonant device may be adjusted so that the output voltage is in phase with the system line-to-ground voltage. With this adjustment the series reactance is approximately equal to the reactance of the auxiliary capacitor, that is, nearly in parallel resonance with it.

No series reactance is used with the nonresonant device and the phase angle between the output voltage and the system line-to-ground voltage may be adjusted over nearly the entire range from 0 to 180 degrees lead, with maximum output at approximately 90 degrees lead.

SELECTION OF MEANS OF OBTAINING VOLTAGE

The choice of the method of obtaining from the circuit voltage a reduced voltage should be governed by the use to which the voltage is to be put. Obviously an ideal device would give a voltage exactly in phase with, and of constant proportionality to, the circuit voltage. However, an ideal device is not available nor is it of practical necessity.

The most accurate means at present is the conventional potential transformer. Its characteristics may be accurately predetermined and they remain practically constant over the usual range of operating conditions. Potential transformers should be used whenever revenue wattmeters are to be operated or when known and consistent accuracy is the controlling factor.

Potential devices may be used when their variation in accuracy is less than that permissible from consideration of the use to which the voltage is to be put. They can be adjusted for a definite accuracy with a definite set of operating conditions but are subject to much greater variation in accuracy from changes in operating conditions. The resonant device ranks next to the potential transformer in performance, with the nonresonant device next.

For example, if only voltage indication and synchronizing are considered, a nonresonant device would be entirely suitable; if distance relaying is considered the resonant device may be used. Each application should be carefully analyzed to make sure that the accuracy requirements are not beyond the performance of the devices before they are specified.

AVAILABLE OUTPUT WATTS "W"

The gross input from a circuit to a capacitance potential device network is given by the following expression, developed in appendix I

$$W = 2\pi f C_1 E E_2 \sin \alpha \qquad (10)$$

in which f is the system frequency, E and E_2 are, respectively, the line and tap voltage to ground with the angle α between them, and C_1 is the coupling capacitance in farads.

The net possible output of the device will be the watts calculated by use of this equation decreased by the losses in the device. The rating of the resonant devices is less than the possible net to insure suitable performance characteristics.

Accuracy Considerations

As mentioned previously, a potential device is subject to some inaccuracies not present in a potential transformer. It is therefore pertinent to analyze the factors affecting the accuracy to determine the advisable field of application for the devices. The more important

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Paper number 38-66, recommended by the AIEE committee on protective devices, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted March 24, 1938; made available for preprinting May 20, 1938.

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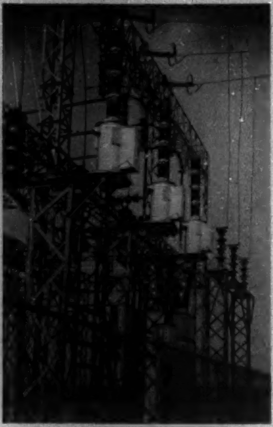

Figure 1. Resonant coupling capacitor illustrating unit type of assembly

factors are leakage, changes in burden, line voltage, system frequency, temperature, and transients.

LEAKAGE

Leakage over the surface of the coupling capacitor or over any insulation in the potential tap circuit may cause appreciable error. However, this error can be eliminated by proper design.

Leakage over the coupling capacitors to the primary winding would cause a change in the phase angle of the current drawn from the circuit and introduce an error into the indication of the device. This, however, has been overcome by constructing the potential device, auxiliary capacitor, and coupling capacitor as a unit so that this leakage current goes direct to ground, without the possibility of affecting the accuracy. See figure 1.

Leakage occurring to ground between the coupling-capacitor terminal and the device winding would also introduce error. However, this has also been overcome by the unit assembly of the parts.

The availability of the coupling capacitor stimulates its use also for carrier-frequency coupling. The carrier circuit must have minimum high-frequency leakage, this is also obtained by the unit assembly, and by the use of Performite-insulated cable with each end shielded from the weather, and supported by vertically mounted oversized insulators.

Bushing potential devices are protected from the effects of leakage because the capacitance is completely enclosed and an armored cable is used for the tap lead.

BURDEN CHANGES

With few exceptions the impedances of potential coils of relays and instruments are essentially constant at all voltages so that the only cause for changes in burden is the connection or disconnection of devices such as voltmeters and synchroscope. Usually the error produced from this cause is relatively unimportant because the load change is relatively small. If the load change is enough to cause too great a change in accuracy, a dummy burden can be arranged to exchange positions with the burden being switched so that no resultant change in burden at all will occur.

A closed magnetic circuit in the gross load on the network may introduce an error. Although the capacitor C_a, for correcting power factor, may be adjusted to give unity power factor at normal voltage, some difficulty may still arise. For example, reference to the diagrams (figure 10 and figure 5) will show that the auxiliary capacitor C_2 is substantially in parallel with the device transformer, the adjustment transformer if used, and with the burden. Under certain conditions this circuit will become unstable and steady overvoltages of a highly distorted wave shape will exist across the transformer, and the relays, etc., which make up the load. This has been overcome in the device network by designing the device and adjustment transformers with low magnetic density and by providing an additional winding in the adjustment unit (see figure 5) so that residual voltage may be obtained without the use of an auxiliary transformer.

Operating engineers should be rather cautious in regard to the use of relays which require the use of auxiliary transformer, so as to avoid the possibility that this trouble may be set up by causes external to the device.

This difficulty has not been encountered with bushing potential devices, probably because of their more limited sphere of application and because the value of the auxiliary capacitance is much lower.

CHANGE IN SYSTEM VOLTAGE

Protective relays are called upon to act at the time of a fault and at this instant the line voltage is usually low. Since the magnetizing current is an appreciable portion of the total load taken by the network, the impedance of the network changes appreciably, and with it the accuracy. The performance of the device should be investigated at the critical fault condition and, if necessary, readjusted to give the desired performance under these conditions.

Ordinary fluctuations in system voltage do not have an appreciable effect on the accuracy of the potential devices.

CHANGE IN FREQUENCY

Variations in frequency affect the accuracy of the devices by changing the relative values of the inductive and capacitive impedances in the network. The effect depends to some extent upon the voltage of the circuit and the burden being carried by the device. The following tabulation gives the approximate variation in accuracy for a 115-kv circuit with the device adjusted for an output of $(50 + j15)$ volt-amperes.

Cycles	R C F	Angle
58	1.021	+1.52
60	1.000	+0.02
62	1.088	−2.65

Under steady-state conditions this effect is ordinarily of no consequence because present-day systems operate at a very steady and constant frequency. Under fault conditions the device will usually have performed its function before the frequency has changed appreciably.

CHANGES IN TEMPERATURE

Changes in temperature affect the capacitance slightly and cause a slight error in the nonresonant device. If the dielectric in the auxiliary capacitor is different from that in the coupling capacitors a slight error will be caused in the accuracy of the resonant device. For the new oil-filled paper-dielectric coupling capacitor and accompanying auxiliary capacitors, the capacitance variation from −40 degrees to +100 degrees centigrade is approximately 3.5 per cent.

Changes in temperature affect the burden and affect directly the accuracy of the nonresonant device as can be seen from the characteristics. The affect on the accuracy of the resonant device is negligible, as can also be seen from the characteristics. Usually the burdens are installed where the temperature changes are not extreme so that the temperature effect is not a serious factor.

TRANSIENTS

Potential devices, consisting of a network of capacitances and inductances, are inherently subject to transient disturbances occurring when system conditions change suddenly, as when a circuit is switched on.

Figures 2*a* and 2*b* are representative of what may occur on switching in. In figure 2*a* trace 1 shows the residual voltage from the potential devices and trace 2 shows the residual current. The oscillatory character of the residual voltage in connection with the large unidirectional component in the residual current gives incorrect information to the directional ground relay and the circuit may be dropped out unnecessarily. In figure 2*b* there is a marked oscillation in the residual current instead of a large unidirectional component. In this case incorrect information will also be given to the directional ground relay and the circuit may drop out.

It should be noted that it requires an error in the current indication as well as in the potential device indication to cause an incorrect operation of the relay.

The incorrect operation of the directional ground relay caused by the inherent transient characteristics of the potential device in combination with the transient error in the current indication have been overcome and successful operation secured by giving the overcurrent relay a sense of discrimination that enables it to distinguish between the true ground fault condition and a switching transient.

Power systems are subject to overvoltages of a semi-steady character and of a transient nature. The first type occurs when a circuit is disconnected at the load end, in which case the voltage will build up to a value seldom exceeding 1.75 times normal when voltage regulators are used. The devices must withstand this overvoltage without becoming unstable and the design has been made accordingly.

The second type occurs from switching. Records show that switching surges above three times normal are of very short duration, and that few even exceed one-quarter of a 60-cycle period. Accordingly the devices are not required to withstand steady voltages in excess of two times normal, but they must withstand transient voltages without becoming unstable. The devices have been designed to remain stable when subjected to long-wave switching surges of three times normal. Sustained transient overvoltages (arcing grounds) in excess of the relief gap setting may cause damage to the gap electrodes from continuous arcing.

Application Requirements

SYNCHRONIZING

The requirements for synchronizing are relatively simple—reasonably good phase angle and voltage indication. Since the nonresonant device may be adjusted over a wide range of phase angle it is well suited for use in synchronizing. The voltage indication to the synchroscope need not be precise so that again the nonresonant device is entirely suitable.

VOLTAGE INDICATION

The accuracy requirement for ordinary switchboard indication of voltage is well within the range of potential devices.

METERING

Resonant potential devices may be used for metering in those cases when a reasonably accurate indication of load or kilowatt-hours is wanted. When revenue metering is considered conventional potential transformers should be used.

RELAYING

The various types of relays commonly used for protection on electrical systems require varying degrees of accuracy of the potential supplied to them. The ordinary directional relay characteristics are such that almost any reasonably good replica of the line potential will be suitable. A shift of several degrees in phase angle or of several per cent in ratio would have no noticeable effect upon the overall operation of the relay system.

On the other hand, high-speed relays of the distance type require an accurate reproduction of the primary voltage or, lacking this, an accurate knowledge of the variation. With the high-speed distance relays the speed of response is great enough that any transient in the voltage supply due to a sudden change in the primary voltage must be investigated to see that there is no shift in phase angle which might cause incorrect operation. If there is simply a time delay transient, the speed of response of the relay system will be somewhat affected, but this, in general, is not serious as compared to a large phase-angle shift during a transient period.

High-speed protection of the carrier pilot type works fundamentally upon the indication of directional relays, and if there be no transient phase-angle shift of large extent, the question of accuracy or fixed error is not of any importance with this type of protection.

It is of more importance to the relay engineer to know the errors of the device than that any attempt should be made to build something capable of supplying a potential having no ratio or phase-angle error. In general, if the variations are known, it is possible to make the requisite relay settings to compensate for them. It should be pointed out that it is really more important that any errors which may exist in either the ratio or phase-angle relation between secondary voltage and the primary voltage be

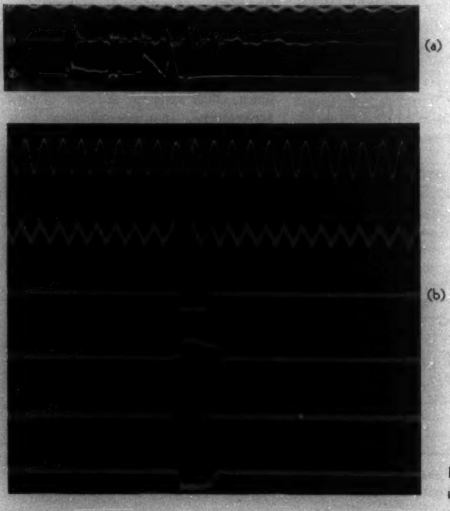

Figure 2. Switching-in transients

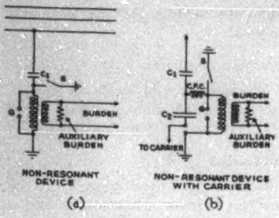

Figure 3. Nonresonant potential device

constant, because if these errors change, then it may be difficult or impossible to obtain the desired operation of the relay system.

It is, therefore, evident in the application of capacitor potential devices to a protective relay system that no hard and fast rule can be laid down as to whether they are suitable or not, but it is a very simple matter to make this decision from the known characteristics of the potential supply and the requirements of the relays.

The types of relays which may commonly be supplied from potential devices are:

Phase directional relays
Balanced current relay restraint coils
Ground current directional relays
Carrier pilot directional relays
Distance relays
  Impedance
  Reactance

These relays have been tabulated in approximately the order of severity of requirements.

A phase-fault directional relay merely has to make a comparison of the angle between current and voltage and determine whether the fault power flow is in one direction or the other. Since the two conditions are 180 degrees apart, and since the relays are customarily arranged to have maximum torque in the middle of the possible angular range of short-circuit currents, an error of even 20 degrees makes only a small change in torque and would never cause a false indication. The ratio error is also of small consequence as it also merely changes the torque slightly. Hence this type of relay will tolerate large errors in the potential device without any adverse effect upon its performance. It is probably safe to say that an error of 20 degrees to 30 degrees and 20 per cent to 30 per cent in the potential device under low-voltage conditions, would be unnoticeable in the action of phase directional relays during short circuits.

One type of high-speed balanced current relay is provided with potential restraining coils in order to facilitate switching operation rather than to provide any real change in the relay characteristic under fault conditions. The requirements are not at all stringent so that ratio errors as high as 30 per cent at low voltage are entirely acceptable. At normal voltage the errors should be kept below 15 per cent if the load over one circuit ever approaches the setting of the relay, while a higher error is permissible where the secondary current never reaches the minimum operating values. Phase angle error is of no consequence whatever during faults but at normal voltage should be kept below 15 degrees where the circuit is heavily loaded and the power factor leading.

The operating torque and hence the time of a directional ground current relay, is a function of the product of the zero-sequence current, the zero-sequence voltage, and some function of the angle between them. In many of these relays the operating torque opposes a resetting spring that provides definite values of minimum operating watts. The requirements are therefore a little more exacting than in the case of the phase relays because actual values of current and voltage are involved in the operation of the relay. On the other hand, there are so many unknown and variable factors in the zero-sequence current path that are not susceptible to exact determination, that there is little necessity of requiring great accuracy in the potential device. The final settings of ground current relays are usually determined by experience so that errors in the potential supply are not very significant. If the error is within 15 degrees and 15 per cent, potential devices should be satisfactory sources of voltage for directional ground current relays.

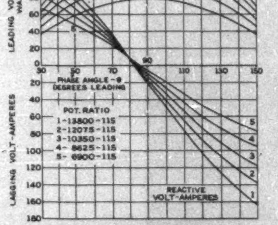

Figure 4. Typical characteristic curves of nonresonant potential device

On carrier pilot relaying the discrimination is obtained by means of phase and ground current directional relays and hence the requirements are quite similar to those enumerated for these types of relays. A somewhat greater accuracy is desirable because the results of relay actions at the two ends are compared via carrier. This is particularly true of a system subject to power oscillations during which the relay must surely block any tripping. Throughout a power swing the current, voltage, and angle between them are constantly changing and are different at any instant at different points in the system.

It is important that the direction of power flow appear to the relays to be in the same direction as it actually is on the line. Otherwise during the changing conditions the relay at one terminal of the line may indicate a reversal of power flow before such reversal actually occurs. The ratio error is of relatively small importance, but the angular error should be held to very close limits if this end is to be achieved. On the other hand, the design of the relay equipment at the present time is such that a 10-degree error would probably not noticeably affect the overall operation and may, therefore, be tolerated.

Distance relays are, in effect, triangulating instruments like a surveyor transit. Any error in the base quantities supplied to them has a direct, and usually linear, effect upon the accuracy of their performance. In a true impedance relay the error in distance measurement is a direct function of the ratio error but angular error would not influence it. Actually the phase relation of the current and voltage does have an appreciable effect upon most impedance relays but in a well designed device it may be made small.

Reactance relays depend upon angular relationships as well as magnitude comparisons but because of the trigonometric function involved, the error in distance measurement resulting from angular errors of current and potential devices, is not a fixed quantity. Upon lines having a high reactance-to-resistance ratio a few degrees error is inconsequential as the distance measured is a function of the sine of an angle near 90 degrees. On the other hand the sine of smaller angles, such as 40 degrees, changes much more rapidly per degree error. Striking an average, it is suggested that the ratio error should be less than five per cent for any distance relay application, when the primary voltage is approximately the value to be expected for a fault at the end of the instantaneous zone, and that the angular error should be below five degrees for reactance relays and 15 degrees for impedance relays under the same voltage conditions.

Types of Devices

Coupling-Capacitance Potential Devices

Nonresonant Device. This is the simplest type of capacitance potential device and consists essentially of a transformer in series with a capacitance as shown in figure 3*a*. The essential features of this device are that the voltage may be adjusted to any desired phase angle, practically from about 30 degrees to 150 degrees, in relation to the line-to-ground voltage, and that once the adjustment is made the total burden must remain constant. This requires the use of an auxiliary burden which must be changed when the useful burden is changed.

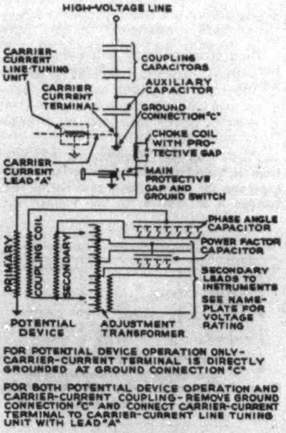
HIGH-VOLTAGE LINE
CARRIER-CURRENT LINE-TUNING UNIT
COUPLING CAPACITORS
CARRIER CURRENT TERMINAL
AUXILIARY CAPACITOR
GROUND CONNECTION "C"
CHOKE COIL WITH PROTECTIVE GAP
CARRIER-CURRENT LEAD "A"
MAIN PROTECTIVE GAP AND GROUND SWITCH
PHASE ANGLE CAPACITOR
POWER FACTOR CAPACITOR
PRIMARY
COUPLING COIL
SECONDARY
SECONDARY LEADS TO INSTRUMENTS
SEE NAME-PLATE FOR VOLTAGE RATING
POTENTIAL DEVICE
ADJUSTMENT TRANSFORMER
FOR POTENTIAL DEVICE OPERATION ONLY-CARRIER-CURRENT TERMINAL IS DIRECTLY GROUNDED AT GROUND CONNECTION "C"
FOR BOTH POTENTIAL DEVICE OPERATION AND CARRIER-CURRENT COUPLING-REMOVE GROUND CONNECTION "C" AND CONNECT CARRIER-CURRENT TERMINAL TO CARRIER-CURRENT LINE TUNING UNIT WITH LEAD "A"

Figure 5. Resonant coupling-capacitor potential device

Typical characteristic curves are shown in figure 4 applying to the devices used with the porcelain-type coupling capacitors. Similar curves apply to nonresonant devices used with the new oil-filled paper-dielectric coupling capacitor except that the output is higher.

Resonant Device. The resonant potential device, when used within its inherent limitation, provides an economical and reliable means of obtaining potential for use with indicating and recording instruments and for relaying purposes. Figure 5 represents the connection for obtaining line-to-ground voltage from one conductor. To obtain phase voltages two or three coupling-capacitor assemblies and potential devices would be required. Characteristic curves are shown in figure 6.

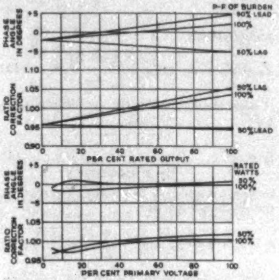

Figure 6. Typical characteristic curves of resonant potential devices

The design of the capacitance potential devices is such that adjustments of voltage and phase angle may be made in steps of one per cent for voltage and one degree for phase angle. These adjustments are made by means of an adjustment unit which consists of an autotransformer giving 69/115 wye volts and 115 volts delta, together with a block of capacitors for phase-angle adjustment, and another block of capacitors for adjusting the net power factor of the burden to unity. Voltage control is by means of taps on the primary side of the autotransformer.

Simultaneous Device and Carrier Coupling. Simultaneous use with carrier of either the resonant or nonresonant device with coupling capacitors requires the addition of a carrier-frequency choke to prevent dissipation of the carrier signal in the potential device. It is essential that this choke be very carefully designed so that it has a very high impedance over the carrier-frequency range and at the same time it must have very low losses. It must be able to withstand the lightning surges which are transmitted to it through the coupling capacitors, and it must also stand the high-potential test of the potential device.

When it is desired to use carrier in conjunction with the nonresonant potential device an auxiliary capacitor as well as a carrier-frequency choke must be added to the network as indicated in figure 3*b*. The addition of the auxiliary capacitance necessitates the addition of a compensating inductance (not shown) to balance out the capacitive volt-amperes drawn by the auxiliary capacitance.

Bushing Potential Devices

Resonant Device. These are, in general, of the same design as those used with coupling capacitors. Taps in the transformer itself take the place of the adjustment transformer. Simultaneous carrier operation is impractical because the capacitances are built into the bushing in such a way that the auxiliary capacitance, tap to ground, cannot be isolated from ground for the carrier connection.

Characteristic curves are shown in figure 6.

Appendix I

Available Output

The available output from a capacitance potential device is the total input to the network decreased by the losses in it. The gross input to the network can easily be determined. In figure 7*a*, E_l represents the net load, or burden voltage (see list of symbols and terms at end of appendix IV), and is drawn in figure 7*b* as the reference vector. Now draw in the net load current I at its angle of lag behind the net load voltage E_l.

Then in figure 7*b* add the voltage drop E_{d2} to the load voltage E_l and obtain the tap voltage E_2, from which is obtained the exciting current I_m and the current I_2

$$E_2 = E_l + IZ_{d2} \tag{1}$$

$$I_m = \frac{E_2}{Z_m} \tag{2}$$

$$I_2 = \frac{E_2}{Z_2} \tag{3}$$

Now find I_1, the current in the coupling capacitor as the sum of the load current, the exciting current, and the current in the auxiliary capacitor.

$$I_1 = I_l + I_m + I_2 \tag{4}$$

From the current I_1 and the impedance Z_1 of the coupling capacitor find the voltage drop across the coupling capacitor, E_1, and locate it in figure 7*b*. It is

$$E_1 = I_1Z_1 \tag{5}$$

Now add E_2 to E_1 and get E as

$$E = E_1 + E_2 \tag{6}$$

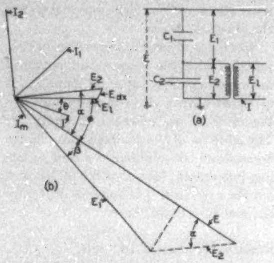

Figure 7. General equivalent circuit and vector diagram of potential device

From figure 7*b* it is easy to see that the power taken from the line is

$$W = EI_1 \sin\beta \tag{7}$$

But

$$I_1 = \frac{E_1}{Z_1} \tag{5}$$

So that

$$W = \frac{EE_1}{Z_1}\sin\beta \tag{8}$$

But from the figure

$$E_1 \sin\beta = E_2 \sin\alpha \tag{9}$$

So that (8) becomes

$$W = 2\pi f C_1 EE_2 \sin\alpha \tag{10}$$

Equation 10 may be used to determine the maximum watts that may be taken from the circuit. The watts available at the load voltage E_l will be the quantity calculated from equation 10 decreased by the losses in the network.

Appendix II

Analysis of Network Circuit

Case 1—Figure 8—Series Reactor in High-Voltage Side of Transformer. As mentioned in appendix I the load current I is net, that is the current in the output winding of the transformer. The lagging component of the burden current is neutralized by the current in the power factor corrective capacitor C_a. Then

$$E_l = IZ_l \tag{11}$$

and the voltage E_t on the input side of the transformer is the load voltage plus the drop through the transformer,

$$E_t = E_l + IZ_d = I(Z_l + Z_d) \tag{12}$$

The exciting current is

$$I_m = \frac{E_t}{Z_m} \tag{2b}$$

and this added to the load current gives the current which flows through the reactor, and the drop across the reactor.

$$I_x = I + I_m = I\left(1 + \frac{Z_l + Z_d}{Z_m}\right) \tag{13a}$$

and

$$E_x = I_x Z_x \tag{13b}$$

The tap voltage E_2 is the input voltage E_t plus the drop across the series reactor E_x.

$$\begin{aligned} E_2 = E_t + E_x &= I(Z_l + Z_d) + I_x Z_x \\ &= I\left(Z_l + Z_d + Z_x + \frac{Z_l + Z_d}{Z_m} Z_x\right) \\ &= IZ_s \end{aligned} \tag{14}$$

and obviously

$$Z_s = Z_l + Z_d + Z_x + \frac{Z_l + Z_d}{Z_m} Z_x \tag{15}$$

Also, the current in the auxiliary capacitor

$$I_2 = \frac{E_2}{Z_2} \tag{3}$$

to which is added I_x to get the current I_1 in the coupling capacitor,

$$I_1 = I_x + I_2 = I\left(1 + \frac{Z_l + Z_d}{Z_m} + \frac{Z_s}{Z_2}\right) \tag{16}$$

The voltage drop across the coupling capacitor is $I_1 Z_1 = E_1$ and the line-to-ground voltage is given by

$$\begin{aligned} E &= E_1 + E_2 \\ &= I\left(Z_s + Z_1 + \frac{Z_l + Z_d}{Z_m} Z_1 + Z_s \frac{Z_1}{Z_2}\right) \end{aligned} \tag{6}$$

$$E = I\left(Z_1 + AZ_s + \frac{Z_l + Z_d}{Z_m} Z_1\right) \tag{17}$$

In (17) A is defined as

$$A = \frac{Z_1 + Z_2}{Z_2} \tag{18}$$

Now re-insert the value of Z_s from (15), divide by A, rearrange, and obtain

$$\frac{E}{A} = I\left[\frac{Z_1}{A} + (Z_l + Z_d)\left(1 + \frac{Z_1}{AZ_m}\right) + \frac{Z_l + Z_d}{Z_m} Z_x + Z_x\right] \tag{19}$$

$$\text{Let } K = 1 + \frac{Z_1}{AZ_m} \tag{20}$$

and then substitute K in (19), rearrange, and divide by K and get

$$\frac{E}{AK} = I\left[\frac{Z_1}{AK} + (Z_l + Z_d) \times \left(1 + \frac{Z_x}{KZ_m}\right) + \frac{Z_x}{K}\right] \tag{21}$$

$$\text{Let } B = 1 + \frac{Z_x}{KZ_m} \tag{22}$$

and substitute B in (21) and obtain, after rearranging and dividing by B

$$\frac{E}{AKB} = I\left(Z_l + Z_d + \frac{Z_1}{AKB} + \frac{Z_x}{KB}\right) \tag{23}$$

$$= I(Z_l + Z_n) \tag{24}$$

In this equation $(Z_n + Z_l)$ is the impedance of the network plus the load when viewed from the capacitance tap and Z_n is defined as

$$Z_n = Z_d + \frac{Z_1}{AKB} + \frac{Z_x}{KB} \tag{25}$$

From (24), (11), (12), (13*b*), the following are obtained

$$\text{Load current } I = \frac{E}{AKB}\,\frac{1}{Z_l + Z_n} \tag{26}$$

$$\text{Load voltage } E_l = \frac{E}{AKB}\,\frac{Z_l}{Z_l + Z_n} \tag{27}$$

$$E_t = \frac{E}{AKB}\,\frac{Z_l + Z_d}{Z_l + Z_n} \tag{28}$$

$$E_x = \frac{E}{AKB}\,\frac{Z_x}{Z_l + Z_n}\left(1 + \frac{Z_l + Z_d}{Z_m}\right) \tag{29}$$

The volt-ampere output of the device is the product of the load voltage and the load current, both net values.

$$VA = E_l I \tag{30}$$

and

$$VA = \frac{E_l^2}{Z_l} \tag{31}$$

From (26),

$$\frac{1}{Z_l} = \frac{\frac{E}{E_l\,AKB} - 1}{Z_n} \tag{32}$$

and then

$$VA = \frac{EE_l}{AKB}\,\frac{1 - \frac{E_l}{E/AKB}}{Z_n} \tag{33}$$

The expressions for the volt-amperes output can be put into a different form as follows. Let

$$\frac{1}{K} = m\epsilon^{jk} \tag{34}$$

and write E as $E\epsilon^{j\phi}$ in which ϕ is the angle from E_l to E, and $\epsilon^{j\phi}$ is the factor which expresses the vector phase position of the voltage E in relation to the voltage E_l.

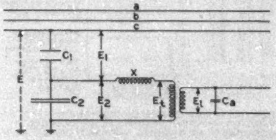

Figure 8. Potential device with phase angle reactor in high-voltage side of transformer

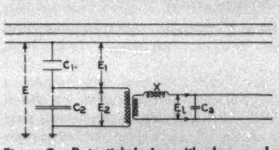

Figure 9. Potential device with phase angle reactor in low-voltage side of transformer

Except when written in this manner E is a vector. When these values are inserted in (33) there results

$$VA = \frac{mEE_l}{AB} \frac{\epsilon^{j(k+\phi)} - \dfrac{E_l}{mE/AB}}{Z_n} \qquad (35)$$

It can be shown that the quantity Z_1/AK is the same as Z_1, Z_2, and Z_m in parallel, and knowledge of this results in the saving of a vast amount of labor if any calculations are made. The proof will be given in two steps to simplify it. First

$$\frac{Z_1}{A} = \frac{Z_1 Z_2}{Z_1 + Z_2} \qquad (36)$$

which obviously is the impedance of Z_1 and Z_2 in parallel. Then

$$\frac{Z_1}{AK} = \frac{Z_1}{A} \frac{1}{1 + \dfrac{Z_1}{AZ_m}} \qquad (37a)$$

from which

$$\frac{Z_1}{AK} = \frac{Z_1/A \,.\, Z_m}{Z_m + Z_1/A} \qquad (37b)$$

which obviously is the impedance of Z_1/A and Z_m in parallel. Therefore Z_1/AK may be written

$$\frac{Z_1}{AK} = \frac{1}{1/Z_1 + 1/Z_2 + 1/Z_m} \qquad (38)$$

The equations for the case with the series reactor in the high-voltage side of the transformer can be used as the general case and all others derived therefrom.

Case 2—Figure 9—Series Reactor in Low-Voltage Side of Transformer; Case 3—Figure 10—Series Reactance Inherent in Transformer. For these two arrangements the factor B becomes unity and the series reactance, whether inherent in the transformer, or in the low-voltage side, may be dealt with as one term except for one reservation. If the reactor is external to the transformer the drop across it may be calculated separately from the drop in the transformer itself.

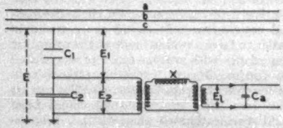

Figure 10. Potential device with phase angle reactance inherent in transformer

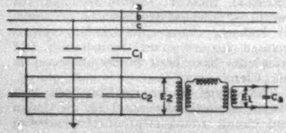

Figure 11. One potential device and three coupling-capacitor assemblies used for obtaining zero-sequence voltage

The equations are

$$Z_n = \frac{Z_1}{AK} + Z_d + Z_x \qquad (38a)$$

or

$$Z_n = \frac{Z_1}{AK} + Z_{dx} \qquad (38b)$$

$$E_l = \frac{E}{AK} \frac{Z_l}{Z_l + Z_n} \qquad (39)$$

$$E_2 = \frac{E}{AK} \frac{Z_l + Z_{dx}}{Z_l + Z_n} \qquad (40)$$

$$E_x = \frac{E}{AK} \frac{Z_x}{Z_l + Z_n} \qquad (41)$$

$$VA = \frac{mEE_l}{A} \frac{\epsilon^{j(k+\phi)} - \dfrac{E_l}{mE/A}}{Z_n} \qquad (42)$$

Appendix III

Case 4—Figure 11—Zero-Sequence Voltage. One Transformer and Three Capacitors. This connection is used to obtain zero-sequence voltage. The load voltage

$$E_l = IZ_l \qquad (11)$$

and the tap voltage

$$E_2 = I(Z_l + Z_{dx}) \qquad (43)$$

from which the exciting current

$$I_m = \frac{E_2}{Z_m} \qquad (44a)$$

$$I_m = I\frac{Z_l + Z_{dx}}{Z_m} \qquad (44b)$$

and the current in the auxiliary capacitors

$$I_2 = \frac{E_2}{Z_2} \qquad (3)$$

$$I_2 = I\frac{Z_l + Z_{dx}}{Z_2} \qquad (45)$$

The current in the coupling capacitors

$$I_1 = I + I_m + 3 \qquad (46)$$

$$I_1 = I\left[1 + (Z_l + Z_{dx})\left(\frac{1}{Z_m} + \frac{3}{Z_2}\right)\right] \qquad (47)$$

Assume that the current I_1 divides among the three coupling capacitors in the proportions a, b, and c so that $a + b + c = 1$, and then

$$\left.\begin{aligned} E_a &= I(Z_l + Z_{dx}) + \\ &\quad aI\left[Z_1 + (Z_l + Z_{dx})\left(\frac{Z_1}{Z_m} + \frac{3Z_1}{Z_2}\right)\right] \\ E_b &= I(Z_l + Z_{dx}) + \\ &\quad bI\left[Z_1 + (Z_l + Z_{dx})\left(\frac{Z_1}{Z_m} + \frac{3Z_1}{Z_2}\right)\right] \\ E_c &= I(Z_l + Z_{dx}) + \\ &\quad cI\left[Z_1 + (Z_l + Z_{dx})\left(\frac{Z_1}{Z_m} + \frac{3Z_1}{Z_2}\right)\right] \end{aligned}\right\} \qquad (48)$$

in which E_a, E_b, and E_c are system voltages line to ground. Let

$$Z_n = \frac{Z_1}{3AK} + Z_{dx} \qquad (49)$$

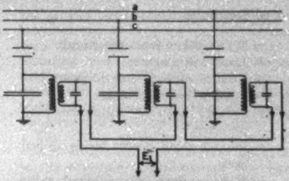

Figure 12. Three potential devices and three coupling-capacitor assemblies used for obtaining zero-sequence voltage

and then the solution of these equations is

$$E_l = \frac{E_o}{AK} \frac{Z_l}{Z_l + Z_n} \qquad (50)$$

$$E_s = \frac{E_o}{AK} \frac{Z_l + Z_{dx}}{Z_l + Z_n} \qquad (51)$$

$$VA = \frac{mE_oE_l}{A} \frac{\epsilon^{j(k+\phi)} - \dfrac{E_l}{mE/A}}{Z_n} \qquad (52)$$

Note that in these equations K is different because the network is different in detail.

Appendix IV

Case 5—Figure 12—Zero-Sequence Voltage; Three Transformers; Three Coupling Capacitors. Rearrange (39) and get

$$\frac{E}{AK} = E_l + IZ_n \qquad (53)$$

Write E_p for E/AK and E_s for the secondary (low) voltage of the transformers, and then for the phases a, b, and c, respectively

$$\begin{aligned} E_{ap} &= E_{as} + IZ_{an} \\ E_{bp} &= E_{bs} + IZ_{bn} \\ E_{cp} &= E_{cs} + IZ_{cn} \end{aligned} \qquad (54)$$

Add and write

$$E_{op} = \frac{E_{ap} + E_{bp} + E_{cp}}{3} \qquad (55)$$

$$E_l = E_{as} + E_{bs} + E_{cs} \qquad (56)$$

$$Z_{on} = \frac{Z_{an} + Z_{bn} + Z_{cn}}{3} \qquad (57)$$

$$I = \frac{E_l}{Z_l} \qquad (11)$$

and then

$$E_l = E_{op} \frac{3Z_l}{Z_l + 3Z_{on}} \qquad (58)$$

$$E_{as} = E_{ap} - E_{op} \frac{3Z_{an}}{Z_l + 3Z_{on}} \qquad (59)$$

The other phase voltages as well as the voltage drop across the series reactance can be determined in a similar manner.

It should be noted that in this case the power factor correction capacitor C_a is not included in the load and, therefore, must be included in Z_n for each transformer.

List of Symbols

W = Watts taken from the circuit

VA = Load volt-amperes

E = Circuit voltage—line to ground

E_l = Load or burden voltage

E_d = Voltage drop in transformer impedance

E_x = Voltage drop across series impedance

E_{dx} = Voltage drop of combined transformer and series impedance either when latter is inherent in transformer, or in output side

E_2 = Tap voltage, or voltage across high-voltage side of transformer when auxiliary capacitance is not used

E_1 = Voltage across coupling capacitors

E_i = Voltage on input side of transformers, used when series reactor is on high-voltage (input) side of transformer

E_a, E_b, E_c = Respective phase voltages to ground

E_x = Pseudo primary voltage of transformers = E/AK

E = Secondary voltage of transformer

I = Load current — burden plus compensator, i.e., net

I_m = Exciting current of transformer

I_2 = Current in auxiliary capacitor

I_1 = Current in coupling capacitor

I_x = Current in series reactor when it is on the high-voltage (input) side of transformer

Z_l = Impedance of load-burden plus compensation, i.e., net

Z_d = Impedance of transformer windings

Z_x = Impedance of series inductance

Z_{dx} = Combined impedance of transformer and series inductance either when latter is inherent in the transformer, or in the output side

Z_m = Exciting impedance of transformer

Z_1 = Impedance of coupling capacitor

Z_2 = Impedance of auxiliary capacitor

Z_n = Impedance of network when viewed from the potential tap

Z_{12}, Z_{23}, Z_{31} = Phase impedances of delta-connected burden

θ = Angle between the net load voltage E_l and net load current I

ϕ = Angle between net load voltage and the circuit line-to-ground voltage

α = Angle between E_2 and E

β = Angle between E and E_1

k = Angle associated with $1/K$

C_1 = Coupling capacitor

C_2 = Auxiliary capacitor

C_a = Power factor corrective capacitor

$A = \dfrac{Z_1 + Z_2}{Z_2}$ = Reciprocal of the ratio of the tap voltage to the circuit line-to-ground voltage at no load

$B = 1 + \dfrac{Z_x}{KZ_m}$

$K = 1 + \dfrac{Z_1}{AZ_m}$ one transformer, and one coupling capacitor

$$\frac{Z_1}{AK} = \frac{1}{1/Z_1 + 1/Z_2 + 1/Z_m}$$

$K = 1 + \dfrac{Z_1}{3AZ_m}$ one transformer, and three coupling capacitors

$$\frac{Z_1}{3AK} = \frac{1}{3/Z_1 + 3/Z_2 + 1/Z_m}$$

$$\frac{1}{K} = me^{jk}$$

a, b, c = Used as subscripts to indicate the respective phases

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Discussion

L. F. Kennedy (General Electric Company, Philadelphia, Pa.): The use of capacitance potential devices for relaying purposes has not been very great because most engineers were inclined to question their accuracy. Mr. Clem has covered the factors affecting accuracy and at the same time given the accuracy requirements for different classes of service. The knowledge of what affects the accuracy and the broad requirements given should certainly be helpful in determining when and where these devices may be used.

In connection with the application requirements, it should be borne in mind that the figures of accuracy given in the paper are naturally those applying to the general case of application of the specific relay involved. There will be limiting cases with the different types of relaying wherein greater accuracy than that specified in the paper will be necessary. I feel it is sufficient merely to call attention to this fact and that all of us will be able to recognize those cases requiring greater accuracy when they arise.

The causes of errors in the potential device are in at least two cases of particular interest to the relay engineer. First, Mr. Clem points out that the use of a closed magnetic circuit in the gross load may introduce an error and even a type of oscillation under certain conditions. Some relay devices of necessity contain closed magnetic circuits, but it seems that the difficulty pointed to by Mr. Clem can be avoided even though these closed magnetic circuits are used, provided their volt-ampere characteristic is linear over the operating range. As an example of this, a recent case showed that oscillation was caused by the use of a wye/delta auxiliary transformer for obtaining zero-sequence voltage. The originally installed transformer had the saturation curve shown by *A* on figure 1 of this discussion. The substitution of a different auxiliary transformer which did not saturate until a much higher voltage was impressed, as indicated by *B*, eliminated the tendency to oscillation. I should like to have Mr. Clem specifically say whether any trouble is liable to be encountered with a linear burden. If a linear burden does avoid these troubles, then the burden under all conditions is the important factor rather than the presence of a closed magnetic circuit.

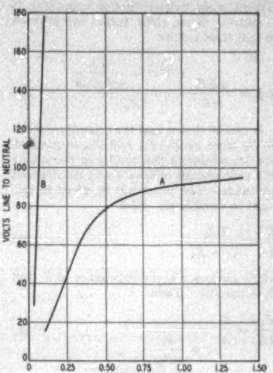

Figure 1

Mr. Clem has pointed out that incorrect relay operation has resulted because of the transients which are inherent in these potential devices. Since these incorrect operations were caused by a combination of transients produced by potential devices and also by transients in the current circuit, the means adopted to prevent incorrect operation actually were applied to current relays in the equipment. To prevent operation on the type of transient shown in figure 2*a*, wherein there is a relatively low-frequency current present caused by the decay of the d-c transient, a high-speed induction-type relay was used which would not respond to this low frequency but would operate in approximately one cycle of normal system frequency. In the second case, figure 2*b*, in an application involving a magnetic type of relay, incorrect operation was avoided by means of a by-pass shunt in parallel with the relay coil providing a path for the high-frequency current.

Mr. Clem has pointed out that the potential supply from the capacitance devices has certain transients not encountered with potential transformers. It is possible theoretically to analyze the expected performance of relays from a supply of this kind. It appears safe to say that the transients which are present will not cause any incorrect operations. They may result in slightly slowing up some of the higher speed types but that appears to be all. There is, however, a great lack of any conclusive field results to verify these theoretical conclusions. It would seem to be in order to have a review made of the operating experience with various types of relays used in conjunction with potential devices in order that their application could be put on the firm basis which always comes from field verification of theoretical considerations.

J. W. Farr (General Electric Company, Pittsfield, Mass.): The more important causes for potential device inaccuracies such as leakage, changes in burden, system voltage, frequency, and temperature, and transients have been briefly discussed in Mr. Clem's paper.

The amount of error resulting from these causes is mostly dependent on the design and circuit arrangement of the potential device.

The following discussion of Mr. Clem's paper applies particularly to ratio and phase-angle errors in a resonant-circuit device when caused by changes in burden and system voltage.

The resonant potential device consists essentially of a voltage transformer for stepping down the tap voltage E_1 to a suitable value for operating relays, instruments, etc., and series inductive reactance Z_2 or Z_{d2} for adjusting the over-all circuit reactance to approximately zero, so that the phase angle will be substantially zero and the circuit will have a minimum amount of regulation. The series reactance may be obtained by connecting a suitable reactor in series with either the primary or secondary side of the transformer or it may be obtained as an inherent characteristic of the transformer itself.

Therefore, in general, three methods or arrangements may be used in designing the resonant-circuit device. These will be referred to as follows:

Case *A*—Voltage transformer with series reactor in the primary or high-voltage side.

Case *B*—Voltage transformer with series reactor in the secondary or low-voltage side.

Case *C*—Voltage transformer with inherent reactance characteristics.

The following comparisons are made to indicate which of these circuit arrangements should be most satisfactory from the standpoint of both operating characteristics and economy of construction.

Case *A* and *B* each use a separate transformer for stepping down the voltage and a reactor to provide the required inductive reactance. Taps are contained in both the transformer and the reactor for making adjustments.

However, cases *A* and *B*, differ in that the reactor is placed in the primary circuit and secondary circuit of the transformer, respectively. The reactor in case *A* must be designed to stand the primary circuit voltages and consequently results in a much larger and more costly device. However, this design has higher accuracy and burden rating for two principal reasons.

First, the transformer operates at nearly constant voltage regardless of load, and its core loss and exciting current are, therefore, constant.

Second, the load current required to supply the internal losses of the transformer must pass through the reactor as well as the bushing capacitances. The net reactance drop (regulation) caused by the transformer loss is, therefore, small.

The reactor in case *B* can be built at low cost because of the low insulation requirements. This circuit arrangement has the following disadvantages.

First, the voltage across the transformer windings is a function of the burden because the reactor is in series between the burden and secondary winding. The core loss and exciting current of the transformer are nonlinear values, varying with this voltage. This leads to considerable circuit regulation with change in burden.

Second, the load current required to excite the transformer is taken directly from the tap plate of the bushing and greatly reduces the available output by causing a high capacitive reactance voltage drop at this point in the circuit.

The arrangement in case *C* entirely eliminates the separate reactor. The required inductive reactance is obtained as a leakage reactance characteristic of the transformer. This circuit is advantageous because the same high degree of accuracy and burden carrying capacity of the case *A* arrangement is obtained, yet the small size and economical construction of the case *B* arrangement is realized.

T. M. Blakeslee (Los Angeles Bureau of Power and Light, Los Angeles, Calif.): Mr. Clem's paper clears up considerable ambiguity that has surrounded the operation of capacitance potential devices. The Los Angeles Bureau of Power and Light has made extensive use of these devices for supplying relay potentials, hot line indication, and synchronizing on the Boulder Dam to Los Angeles transmission lines and has employed them for hot line indication and synchronizing on 110-kv and 132-kv circuits. The devices have been generally very satisfactory.

In connection with the operation of the resonant device the importance of the ground connection on the auxiliary capacitor is not always appreciated. When the device is used for carrier coupling the carrier equipment designer sometimes requires a fuse in the auxiliary-capacitor ground lead although just what protection this fuse offers to the carrier equipment is not clear. If the device is also used for supplying directional relay potential serious consequences are liable to ensue should the auxiliary-capacitor ground circuit become open by the mechanical failure of the fuse or by any other cause.

The curves presented in figure 2 of this discussion show the change in phase angle and ratio of the resonant capacitor potential device that may be expected to result from opening the auxiliary-capacitor ground lead. The curves in this figure are based on the correct adjustment of the potential device prior to the opening of the auxiliary-capacitor ground lead.

P. O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The author's paper is timely in reviewing the application of the various arrangements of capacitance potential devices. It illustrates the changes necessary to meet present day requirements of the industry although differing little fundamentally from the method originally described by us in 1928. As pointed out by the author, it is necessary to review the requirements of each application and compare them with the inherent limitations of potential devices to prevent unsatisfactory field experience. Field tests described by us in 1932 indicated satisfactory operation of the most usual relay schemes involving the several different potential devices.

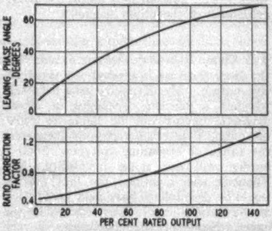

Figure 2. Typical ratio and phase-angle curves of resonant coupling-capacitor potential device resulting from opening the auxiliary-capacitor ground connection

The effects of electrostatic leakage and transients are probably the most important of the accuracy considerations mentioned by the author. The problem of leakage involves the design of insulation surfaces and also elimination of parts exposed to the weather. Unit construction such as illustrated in figure 3 of this discussion eliminates the hazards of exposed connections between the tapped capacitor stack and the potential network. Early experience with bushing potential devices indicated the necessity of adequate insulation of the tap connection. Succeeding designs have introduced improvements in insulation so as to eliminate varying leakage conditions and resulting varying errors with different weather conditions.

Experience has shown that the secondary transient during a fault may be more detrimental to relay operation than when a circuit is switched on and in some cases this point must be carefully considered in the design of the secondary circuit. For example, a high-speed directional element on good lines may operate incorrectly due to the damped-wave oscillation of the potential device which does not immediately go to zero. Suitable precautions in the design of the secondary circuit of the device are necessary so that these damped-wave oscillations will not cause an apparent reversal of power on the relays that should not trip. This is particularly true of the high-speed directional watt element which functions in one cycle or less and, therefore, follows the damped-wave oscillation. The magnitude of the damped wave is of assistance rather than a hindrance to the watt element provided the phase shift does not cause re-

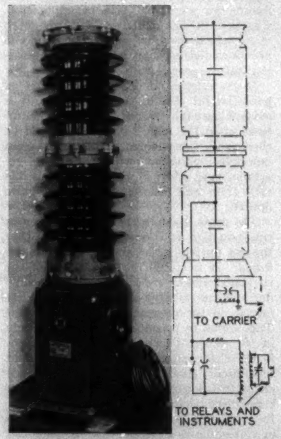

Figure 3. Capacitor potential device, 115 kv

versal and thereby undesired tripping.

We are somewhat surprised at the apparent difficulty which has been experienced with auxiliary potential transformers. We have employed them consistently in the past to obtain zero-sequence voltage. The phenomenon of two stable operating points for circuits involving capacitors and saturating reactances is well known. With proper consideration of the saturating characteristics of the potential transformers, either main or auxiliary, danger of operation in the saturated region with attendant bad wave form may be avoided. The use of auxiliary transformers to obtain zero-sequence voltage has the advantage of requiring fewer wires between the potential source and the switchboard. Attention is called to figure 5 of the paper in which the adjustment transformer is an auxiliary transformer.

The performance characteristics of the zero-sequence arrangements may be much more simply analyzed by the method of symmetrical components. This involves recognition of the fact that the output current and voltage are a function of the zero-sequence voltage of the line only, so that the zero-sequence impedances of capacitors, reactors, and transformers may be used in a simple single phase equivalent circuit to determine the load voltage E_1 and the output, *VA*. This volt-ampere rating is a function of the primary zero-sequence voltage. This voltage varies considerably with the system conditions but for standardization of ratings it should be based on a primary zero-sequence voltage equal to the normal phase-to-neutral voltage rating of the coupling capacitor or bushings.

D. B. Masters (The Commonwealth & Southern Corporation, Jackson, Mich.): This paper forms a very interesting presentation of the characteristics and inherent limitations of capacitance potential devices. It is encouraging to note that great improvements have been made since the device was first developed and that it is finding an increasing field of application. Changes in design have evidently been made which decrease sources of error and permit a more accurate prediction of operating performance.

In considering their use as a source of potential for certain types of relays, it is evident that the effect of the device's limitations upon the relay's operation should be thoroughly investigated. This would seem especially pertinent with regard to reactance-type relays in which the permissible ratio error and phase-angle error are quite limited.

The gross input to the device is given in equation 10. It is our understanding that the net output is the above value less the losses. It is stated that the rating of the resonant devices are less than the possible net to insure suitable characteristics. What factors in the resonant circuit cause this? This seems to permit considerable latitude in the permissible output rating a manufacturer can assign to the device. It would seem highly desirable to define performance characteristics so that ratings of the various types of devices would be on the same basis. It would also seem desirable to give ratings in volt-amperes as is the case for potential transformers.

In the nonresonant device, the phase angle between the primary and secondary voltages can be varied from 30 degrees to 150 degrees. Is the ratio also variable and what is the range? In the resonant device it is stated that the voltage can be adjusted in one per cent steps and phase angle in one-degree steps. Over what range can these adjustments be made?

Curves and data are given which show the variation in phase-angle and ratio-correction factors for various values of operating voltage. It would be interesting to know what the effect of operating a given voltage rated device at a lower system voltage is on the phase angle and ratio for the permissible burden and accuracy; as for example, a 154-kv device operating on a 138-kv system.

In the general discussion and analysis of network circuits it is not stated whether the system neutral is grounded or isolated. Can it be said that the device is equally applicable to either system or are there certain limitations inherent to the one system not encountered in the other?

E. H. Bancker (General Electric Company, Schenectady, N. Y.): The old adage about "the proof of the pudding" is still as valid as when it was first written. It is actual experience with the making and using of a device that eventually results in the ironing out of the wrinkles and the establishment of confidence in its ability to perform certain functions for the benefit of society. The author of this paper has been intimately associated with potential devices almost from their inception and knows both their design characteristics and the operating characteristics which have been experienced with those in service. In his paper he makes available to everyone the experience gained from his association with these devices, so that it is possible to make an intelligent application of potential devices for the supply of voltage to different pieces of equipment with knowledge of what may be expected in the way of results. The mathematical analysis given in the appendix makes it possible to calculate the performance under almost any operating condition which users should find convenient for those instances where the usual ratio and phase-angle correction factor curves do not give sufficient information. This paper should be most helpful in applying potential devices in the future. If used with full knowledge of the results that can be obtained, potential devices become an extremely useful tool in the equipment of the utility engineer.

J. E. Clem: It is very pleasing to note that the discussions are in general of a constructive nature. Mr. Kennedy asks definitely whether a burden containing a closed magnetic circuit will be liable to cause instability if the characteristic is linear over the range of operating voltages. Such a burden will not cause instability. Mr. Kennedy also mentions the fact that it would be desirable to have field verification of the various conclusions reached in the paper. Some of the conclusions in the paper actually are based upon field experience, but it is admittedly desirable that a record of additional field results would be very helpful.

Mr. Farr's discussion is very helpful since he points out the three methods by which the necessary reactance may be obtained and outlines the reasons for the choice of the present standard type of construction.

It is very encouraging that Mr. Blakeslee reports very satisfactory experience from the use of these devices. A fuse in the auxiliary-capacitance ground leads serves no essential purpose and may well be omitted. The curves submitted by Mr. Blakeslee indicate that the presence of the fuse may be a liability.

It is pleasing that Mr. Langguth's discussion emphasizes several of the points brought out in the paper. However, the adjustment transformer in figure 5 is an auxiliary transformer in appearance only. In the design of the device the magnetic characteristics of the main transformer as the adjustment transformer are treated as a unit. A little further perusal of the paper will bring out the fact that the equations given for zero-sequence voltage give the same result as those which might have been obtained using the symmetrical-component method. The equations in the paper give consideration to the fact that the magnetic characteristic of the device is nonlinear.

Mr. Masters, in his discussion, asks several pertinent questions. The rating of the resonant device is less than the maximum possible output in order to keep within desirable phase-angle errors and ratio-correction factors. The present watt rating of these devices is above the maximum that has been required in any to which they have been put to date.

The ratio of the nonresonant device is subject to control as shown in figure 4 of the paper. Taps are provided so that the operating tap voltage may be of any value between 100 per cent and 50 per cent. Ordinary changes in load voltage can be taken care of by the auxiliary burden.

The resonant device is designed so that the correct voltage ratio may be obtained by means of adjustment in one per cent steps in voltage and one-degree steps in phase angle. Correct voltage ratio may be obtained at any burden within the rating of the device. The range of adjustment is selected from consideration of the tolerance permissible in the manufacture of the coupling capacitor and of the auxiliary capacitor.

The effect of operating a device designed for one circuit voltage on another circuit voltage depends, to a certain extent, upon the type of device and on the relative voltages. The foregoing, of course, refer to a complete assembly of coupling capacitor, auxiliary capacitor, if used, and potential device. The potential device itself is the same regardless of circuit voltage. A nonresonant potential device would have its output decreased directly in proportion to the decrease in circuit voltage. If the change in the circuit voltage was not too great, the output of the nonresonant device would not have to be reduced nor would its characteristic curves be changed. If the change in circuit voltage is too great it would be impossible to get correct adjustment of ratio and phase angle on a resonant device.

These devices indicate line-to-ground potential irrespective of whether the neutral is grounded, and are equally applicable to grounded or nongrounded circuits for that purpose.

The Current-Limiting Power Fuse

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FUSES may be used on large central station and industrial systems in locations where a high interrupting capacity is required. The conventional fuse may clear a short circuit in a half cycle or several cycles. Associated apparatus is subjected to electromagnetic forces produced by the high momentary currents. Good engineering practice dictates that all disconnecting switches, contactors, motor starters, etc., shall have short-time current ratings at least equal to the initial root-mean-square fault current which the system is capable of producing at the point of installation. Where these currents are extremely high, a saving in size and expense of such apparatus can be effected if the required short-time current rating can be reduced. Many attempts have been made and accomplishment has been achieved by the use of a fuse which consistently limits the initial peak current to a fraction of that available in the circuit. The purpose of this paper is to describe the new fuse.

The economic value of a power fuse may be materially enhanced if it has, in addition to the usual requirements, the following features:

1. Adequate interrupting capacity for installation in modern central station and industrial systems.
2. Ability to limit the short-circuit current to a magnitude well below that which the system is capable of producing.
3. Ability to interrupt high currents without the generation of gas which must be discharged to the atmosphere or dissipated in a high-pressure container.
4. Silent operation with no discharge of metal parts, liquid, gas, or solid matter.
5. Suitable means of readily detecting a blown fuse.

Fuses having these desirable features in some degree, have been made available in Europe by Allgemeine Elektricitäts-Gesellschaft, Siemens-Schuckert, Brown-Boveri, Société Générale de Constructions Electriques et Méchaniques, and others, and have been partially described in European publications as indicated in the bibliography. These fuses, as manufactured by the various European companies, are similar in general design but differ in details of construction, in materials employed, and in published interrupting ratings. A new fuse has now been developed in the United States, and the authors consider it to be an improvement over the foreign fuses in these three respects. It is hoped that the material presented in this paper will be of value to the European engineers in extending their knowledge of the operating characteristics of this general type of interrupting device.

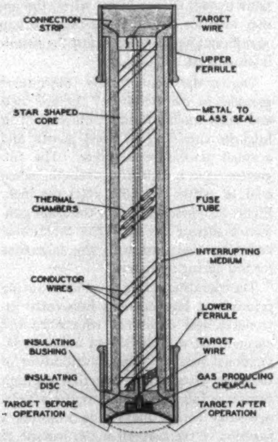

Figure 1. Construction of the current-limiting fuse

Construction of Fuse Unit

The new current-limiting fuse consists of an interrupting element of one or more main conductors wound on a supporting core and embedded in a granular interrupting medium. To these parts are added a housing, terminals, indicator, and thermal chambers as shown in figure 1.

The main conductors are pure silver wires and the supporting core is high-temperature ceramic material. The ends of the wires are welded to copper connection strips which are in turn welded to the terminals or ferrules of the fuse unit. The interrupting medium is in the form of granular quartz and surrounds the conductor wires and occupies the space between the core and the Pyrex fuse tube in which the entire assembly is sealed. The granular quartz is prevented from coming in contact with a short length of the conductor wires, at their centers, by a number of thermal chambers which are formed from high-temperature ceramic material.

A target wire is welded to the upper connection strip and passes through a hole in the center of the core and terminates in a chamber at the bottom containing a small quantity of gas-producing chemical. A thin copper target is located below the chemical chamber and is arranged so that it is cupped inwardly until the blowing of the fuse causes it to reverse its curvature as indicated in figure 1.

Every material used in the current-limiting fuse, with the exception of the target chemical, is inorganic in nature and is subject to the minimum amount of corrosion and deterioration. It is possible to use a wide variety of materials for each of these components but the results of several years of research in this country and Europe indicate that the materials described in this paper provide the most efficient operation of any now available. It is reasonable to expect, however, that further research will uncover new and improved materials which will increase the efficiency of the fuse without altering the fundamental principle of circuit interruption.

Operation

The interruption of an electric circuit by any device is accompanied by the release of system energy in the form of an arc and is followed by voltage disturbance. In a given circuit, the amount of energy released in the arc depends upon arc voltage, current, and the duration of arcing time. Modern improvements in operation of circuit-interrupting devices have been based principally upon limiting the duration of the arc and upon efficient dissipation of arc energy. A reduction in the magnitude of arc current has offered a fertile field for development which, until the advent of the current-limiting fuse, had hardly been touched.

Paper number 38-65, recommended by the AIEE committee on protective devices, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted March 26, 1938; made available for preprinting May 20, 1938.

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The authors wish to acknowledge the assistance of Otto Mayr, Allgemeine Elektricitäts-Gesellschaft, Berlin, in selecting materials, and the work of C. L. Schuck, switchgear engineering department, in establishing design data for the current-limiting fuse.

[1] For all numbered references, see list at end of paper.

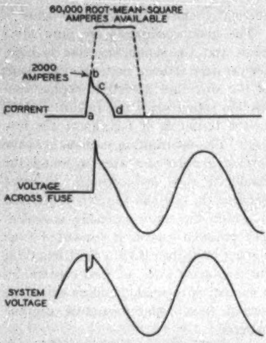

Figure 2. Reproduction of typical magnetic oscillogram of fuse operation

The current-limiting action of the fuse is best explained by reference to the reproduction of a typical magnetic oscillogram of a high-current test, figure 2, which shows the actual current and the current which would have existed had the fuse not been in the circuit. The short circuit begins at *a*, the silver wires volatilize and arcing begins at *b*, and the circuit is interrupted at *d*. A short-circuit root-mean-square current of 60,000 amperes is thereby limited to a maximum (peak) of 2,000 amperes.

When the silver wires volatilize at *b*, the silver vapor expands to approximately 400 times the volume originally occupied by the wire. The force of the expansion throws all of the silver vapor out of the arc path and into the space between quartz granules. It condenses on the surface of the relatively cool granules and is no longer available for current conduction. The arc is thereby confined to the small space previously occupied by the silver wires. This results in an intimate physical contact between the hot arc and the cool quartz granules which causes an exceedingly rapid transfer of heat from the arc to the quartz granules. Most of the arc energy is dissipated in this heat transfer as the pressure generated in the fuse is practically negligible.

The volatilization of the silver conductors and the sudden removal of the vapor from the arc path has the effect of suddenly inserting a very high resistance into the circuit at the instant that the silver wires melt. If the melting occurs before the short-circuit current reaches its normal initial peak magnitude, the rise of current is cut off and the current rapidly decreases to a magnitude, *c*, figure 2, well below that at which melting occurred. At point *c* sufficient resistance has been inserted to produce a power factor approximating unity and the remaining decay of the current is practically in phase with the voltage. Thus the current-limiting fuse tends to select the most favorable instant, the normal zero of both current and voltage, for final interruption with minimum disturbance.

The fuse functions as a current-limiting device on all currents which melt the conductor wires before the current reaches its initial normal peak. Obviously, the fuse cannot limit a current which reaches its peak before the conductor wires melt. Low currents cause the conductor wires to melt within the thermal chambers and to burn back toward the terminals at a rate which is dependent upon the current magnitude. On extremely low currents (currents requiring 30 minutes or more to melt the conductor wires) the arc may exist for several cycles before a gap of sufficient length to interrupt the circuit is introduced.

During the arcing period, the quartz granules in the vicinity of the arc melt and upon cooling form a hollow shell or fulgurite consisting of fused quartz and a small percentage of silver. The fulgurites have a dielectric strength, when cold, in excess of 10,000 volts per inch. Figure 3 shows a core with the main conductors before the fuse has functioned and the same core with the fulgurites formed during operation.

The operation of this novel interrupting principle at high currents inherently introduces new conditions which are not encountered with devices of a more conventional nature. The volatilization of the silver wires and the insertion of a high resistance into the circuit creates a voltage surge with a magnitude which is a function of the current at the instant of melting, the ohmic value of the resistance inserted, and the constants of the circuit. In developing this new fuse, the three factors affecting the surge voltages have been carefully studied with many cathode-ray oscillograms of both voltage and current. The crest of the surge voltage is far below the corresponding basic impulse insulation level[1] and the maximum surge voltage only slightly exceeds the crest of the 60-cycle AIEE one-minute test for rotating machines.[2]

Design and Tests

Some of the earlier known fuses consisted of a metallic conductor surrounded by an interrupting medium of granular or powdered materials. Thousands of these older devices are now in service in the form of low-voltage cartridge fuses, high-voltage potential transformer fuses, and a limited number of high-voltage power fuses. In general, the operation of these devices produces pressures which are liable to disrupt the fuse tube if the current exceeds a few thousand amperes. The operation of the high-voltage designs on extremely low currents is questionable unless some mechanical means[3] is provided to remove the conductor from the tube. The current-limiting action of these older fuses is practically nonexistent. With this background of mediocre success previously experienced with totally enclosed fuses, the design of a high-interrupting-capacity restrained-pressure fuse of this general type seemed, at first, to be impossible. However, "the impossible" has again been accomplished by taking advantage of new and improved materials which were not available to the designers of the older fuses together with new techniques and a better understanding of the interrupting process.

The successful operation of the current-limiting fuse is dependent upon the material selected and the correct assembly and arrangement of all parts with respect to each other. The selection and arrangement of all materials was made by conducting comparative interrupting tests under controlled circuit conditions. The material or arrangement which interrupted a given current at a given voltage with the minimum length of conductor

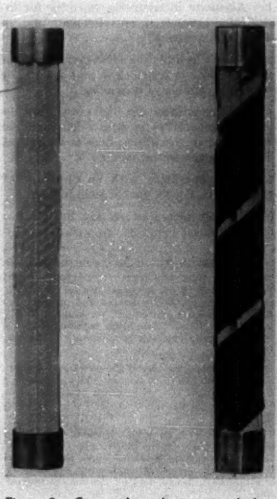

Figure 3. Core and conductor wires before and after operation

wire was selected as optimum. The use of a synchronous closing switch and a cathode-ray oscillograph made it possible to differentiate quite closely between materials which differed only slightly in composition.

The physical characteristics of the interrupting medium are of major importance as this material must: (*a*) remove heat from the conductor wires during normal operation, (*b*) provide a condensing surface for the metallic vapors during circuit interruption, (*c*) cool the arc during circuit interruption, (*d*) produce no gas at arc temperature, and (*e*) provide sufficient dielectric strength after circuit interruption. A number of materials were investigated and the characteristics of granular quartz were such that it was selected as the available material nearest the ideal.

The ceramic material used for the thermal chambers and the core supporting the conductors is exposed to arc temperatures at which ordinary ceramics will melt. The particular material for these parts was especially developed to maintain high resistivity at arc temperatures.

The diameter and number of wires placed in a given tube determines the current rating of the fuse and also the degree of its ability to limit the actual short-circuit current to a fraction of that available in the circuit. The length of the wires, in a given interrupting medium, determines the voltage rating of the fuse. The variation in length of single wires, of various diameters, necessary to interrupt a given voltage is indicated in figure 4. It will be noted that single small wires interrupt low currents with shorter wire lengths than those required for high currents whereas the reverse is true of larger single wires. The use of a large number of wires in parallel makes possible the design of high-current ratings with total wire sections larger than can be used with single wires. The subdivision of conductor wires not only reduces the current density in any one arc path but also provides a cascading effect during low-current operation. That is, a number of low-current arcs in parallel are unstable and one arc tends to take all of the current. If there are originally ten arcs in parallel and one of these arcs takes all of the current, the other nine are temporarily extinguished. At 10 times minimum melting current the one remaining arc is quickly and permanently extinguished. The voltage may restrike an arc successively in any or all of the other nine paths, but each arc in turn will be quickly and permanently extinguished. The use of multiple wires, therefore, reduces the length of wire required for low-current interruption to a value approaching the high-current length, indicated in figure 4, for single wires.

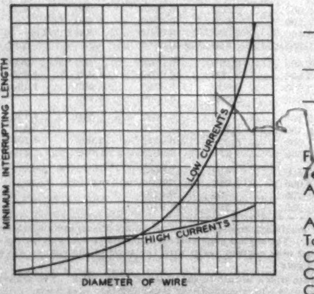

Figure 4. Length of single wires for the interruption of a given voltage

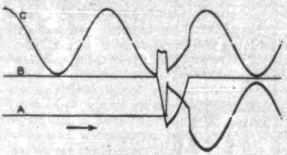

Figure 5*A*

Fuse rating—23,000 volts, 20 amperes
Test voltage—24,000 volts
Available root-mean-square current—5,000 amperes
Actual peak current—1,730 amperes
Total duration—0.0055 second
Curve *A*—Voltage across fuse
Curve *B*—Current through fuse
Curve *C*—Test generator voltage

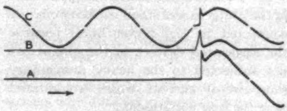

Figure 5*B*

Fuse rating—15,000 volts, 10 amperes
Test voltage—15,000 volts
Available root-mean-square current—12,500 amperes
Actual peak current—840 amperes
Total duration—0.0056 second
Curve *A*—Voltage across fuse
Curve *B*—Current through fuse
Curve *C*—Test generator voltage

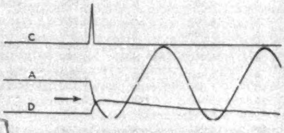

Figure 5*C*

Fuse rating—15,000 volts, 3 amperes
Test voltage—15,000 volts
Available root-mean-square current—10,400 amperes
Actual peak current—230 amperes
Total duration—0.00085 second
Curve *A*—Voltage across fuse
Curve *C*—Current through fuse
Curve *D*—Cathode-ray-oscillograph trip

Figure 5. Typical magnetic oscillograms

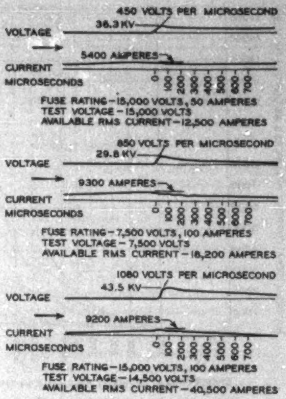

Figure 6. Typical cathode-ray oscillograms

Hundreds of interrupting tests at rated voltage were conducted to establish design data for a line of fuses in standard voltage ratings from 2,500 to 23,000 volts. The results of a few of these tests are given in table I. Typical magnetic oscillograms are shown in figure 5 and typical cathode-ray oscillograms are shown in figure 6. Tests were conducted with the fuse connected in the circuit near the generator terminals with practically no external capacitance or resistance. Additional tests were conducted with capacitors across the fuse to simulate a connected transmission line, and again with a series reactor tending to stiffen the circuit. The operation of the fuse was unimpaired by these conditions. The normal station rates of rise of recovery voltages were considerably modified by the fuse as indicated in table I.

Application

The current-limiting fuse does not expel gas or metallic parts during operation. It may be completely enclosed and

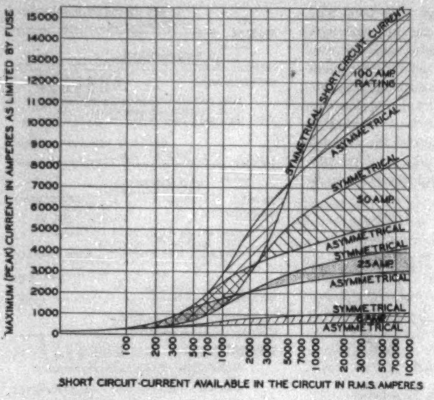

Figure 7. Current-limiting characteristics

mounted with the same electrical clearances used for other noninterrupting devices such as disconnecting switches and bus-bar supports. This feature makes the fuse particularly advantageous for application indoors or in metal-enclosed equipments where space economy is essential.

Since the fuse cuts off the short-circuit current before the available peak magnitude is reached, its application permits the use of associated apparatus with a lower short-time current rating than would otherwise be possible. The selection of connected apparatus may be made on the basis of the actual current which the fuse will pass, as shown in figure 7, rather than on the basis of the current which the circuit is capable of producing.

As an example of the reduction in size of connected devices made possible by the use of the current-limiting fuse, consider a single item, the disconnecting switch. A 7,500-volt 1,200-ampere disconnecting switch has a one-second rating of 60,000 amperes.[4] It is the minimum size switch which may be safely applied in a 6,600-volt 75-ampere circuit with a fault current of 35,000 amperes unless a current-limiting fuse protects the circuit. A 100-ampere fuse will limit the fault current to a peak of 13,500 amperes and permit the use of a 400-ampere switch instead of a 1,200-ampere switch. The same reasoning may be applied to all other devices in the circuit with a substantial over-all saving to the purchaser.

Summary

1. Current-limiting fuses have been developed and tested which will interrupt a short circuit well before the current reaches the maximum value it would attain were the fuse not present.

2. These fuses have many desirable characteristics such as high interrupting capacity, silent operation, no discharge of any kind during interruption, and practically no pressure generation.

3. Such fuses have an ideal application in the protection of small-capacity circuits connected to high-power buses because the associated devices in the circuit are not subjected to the heavy momentary short-circuit current which would exist were the fuse not present.

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Table I. Typical Test Results

Test Number	Rating of Fuse		Test Voltage	*Test Current		Interrupting Time (Seconds)	**Recovery Rate (Volts Per Microsecond)	
	Voltage	†Current		Root-Mean-Square Available	Actual Peak		Available	Actual
1	2,500	25	2,500	25,600	2,200	0.0033	535	140
2	2,500	100	2,500	25,600	10,000	0.0050	535	90
3	2,500	8	2,500	25,600	660	0.0018	535	180
4	2,500	8	2,500	46,400	770	0.0048	368	360
5	5,000	20	5,000	46,500	2,500	0.0025	386	470
6	5,000	8	4,200	77,200	600	0.0003	450	
7	5,000	50	5,000	16,400	5,000	0.0008	883	
8	5,000	8	5,000	1,200	550	0.0051		
9	7,500	125	7,500	58,800	12,300	0.0042	562	900
10	7,500	100	7,500	58,800	13,700	0.0033	562	1,600
11	7,500	100	7,500	58,800	9,900	0.0050	562	1,100
12	7,500	100	8,400	31,800	9,000	0.0050	1,170	
13	15,000	8	14,500	40,500	700	0.0042	600	1,120
14	15,000	60	15,000	9,100	3,400	0.0050		450
15	15,000	60	14,500	31,500	5,800	0.0042	1,060	1,180
16	15,000	8	14,500	40,500	670	0.0025	600	870
17	23,000	8	22,000	12,500	650	0.0042		
18	23,000	8	22,000	15,000	935	0.0042		
19	23,000	8	23,000	200	186	0.0107		
20	23,000	10	25,000	250	380	0.0107		

* The root-mean-square available current is the maximum root-mean-square asymmetrical current which the circuit can produce in the absence of the fuse. The actual peak current is the maximum current permitted by the fuse in the specific test. The actual peak current varies with the angle of the voltage wave at which the short circuit occurs as indicated in figure 7.

** The available recovery rate is the recovery rate which occurs in the absence of the fuse. The actual recovery rate, with the fuse in the circuit, is given only where cathode-ray measurements were made.

† Current ratings are subject to change pending the decision reached by the joint EEI-NEMA committee which is now studying this subject.

Discussion

Table I. Initial Root-Mean-Square Unsymmetrical Short-Circuit Currents Based on Ten Per Cent Subtransient Reactance

Maximum kilovolt-ampere generating capacity	10,000	20,000	50,000	100,000	150,000
Three-phase short-circuit kilovolt-amperes based on 10 per cent reactance	100,000	200,000	500,000	1,000,000	1,500,000
Initial short-circuit current, root-mean-square unsymmetrical					
Amperes at 2,300 volts	43,000	86,000			
Amperes at 4,600 volts	22,000	43,000	109,000		
Amperes at 6,900 volts	14,500	29,000	72,000		
Amperes at 13,800 volts	7,700	14,500	36,000	72,000	109,000

T. G. LeClair (Commonwealth Edison Company, Chicago, Ill.): The development of a current-limiting power fuse is an important forward step in the art of circuit interruption. The advantages of this principle for a-c circuits are comparable to the advantages of the high-speed circuit breaker on d-c circuits because of the reduction in the destructive effect of high currents.

The paper contains a very good description of what takes place when the fuse satisfactorily interrupts the circuit. However, I assume that like all other interrupting devices the fuse must have an upper limit beyond which it will fail to function. I should like to ask Messrs. Prince and Williams what determines this upper limit and what is the nature of the breakdown when the limit is exceeded. Is the limit in interrupting ability measured by the ability of the interrupting material to absorb the power loss in the arc during the period between the melting of the silver conductor and the next current zero? If so, is the interrupting ability then somewhat a variable depending upon the point on the voltage wave at which the short circuit occurs?

We frequently use fuses for the somewhat special purpose of short-circuiting a current-limiting reactor in order to reduce the duty on the circuit breaker in the event of a three-phase short circuit. Would this application with high reactance paralleling a fuse tend to increase the rate of rise of recovery voltage, which is already high due to the characteristic of the fuse itself?

Most of the discussion of this paper is naturally devoted to the essential feature of the new development, which is the speed of interruption and the consequent reduction of short-circuit current. Another feature which is important in the application of fuses, and particularly one of this type, is the time-current curve to determine the melting time of the fuse element at various percentages of normal fuse rating. It would seem to me from the fact that variations can be made in the number and size of fusible elements, that it would also be possible to obtain some measure of control on the shape and time-current curve for initial fuse melting. I would appreciate some comment from Messrs. Prince and Williams on this point.

O. R. Schurig (General Electric Company, Schenectady, N. Y.): One of the important advances made in the development of the current-limiting fuse is represented by the high value of the available short-circuit current in circuits which the fuse will interrupt.

As the authors have pointed out, the fuse has been successfully tested, interrupting available short-circuit currents up to 77,000 root-mean-square amperes. This is accomplished without developing high internal pressure and without expulsion effects, by so designing the fuse that circuit interruption takes place without the decomposition of any products in the fuse and that a current-limiting action occurs causing large short-circuit currents to be cut off before the peak value of the available current has been reached. The resulting benefits in respect to reduced thermal and mechanical effects in the apparatus subjected to the short-circuit currents during operation of the fuse have been brought out in the paper.

It can readily be shown that there is need for a fuse capable of interrupting the higher short-circuit currents. For this purpose, the available initial short-circuit currents (root-mean-square amperes unsymmetrical) in circuits at 2,300, 4,600, 6,900, and 13,800 volts with a maximum generating capacity within the range from 10,000 to 150,000 kva are indicated in table I on the basis of ten per cent subtransient reactance. It is of course recognized that the actual system impedance determining the magnitude of the initial short-circuit current may differ from ten per cent in many systems, considering on the one hand such factors as circuit impedance due to interconnections between separate generating stations, and arc resistance, which tend to lower the short-circuit current, and on the other hand the effects of rotating machines other than generators which tend to increase the initial short-circuit current. However, the values arrived at may be considered as fairly representative of the approximate order of magnitude for the upper limit of short-circuit currents.

It is seen from the table that a 2,300-volt system with 10,000-kva generating capacity at ten per cent subtransient reactance would have a root-mean-square unsymmetrical short-circuit of 43,000 amperes. A system with 20,000-kva generating capacity under similar conditions would have 86,000 amperes unsymmetrical root-mean-square initial short-circuit current.

Similarly a system of 50,000-kva generator capacity would have 109,000 root-mean-square amperes initial unsymmetrical short-circuit current at 4,600 volts, or 72,000 amperes at 6,900 volts.

On the same basis, a system of 100,000-kva generating capacity would have an initial root-mean-square unsymmetrical current of 72,000 amperes at 13,800 volts.

Short-circuit currents as high as those indicated in the table are no longer rare in central-station or industrial circuits. In testing the fuses at available currents as high as approximately 80,000 amperes root-mean-square unsymmetrical, the authors have not exceeded present-day maximum requirements in modern large systems. It is also significant that this current approaches the order of magnitude of the interrupting ratings of the largest oil circuit breakers specified in the NEMA oil circuit breaker standards (publication 36-38).

G. F. Lincks and **S. R. Smith, Jr.**, nonmember (General Electric Company, Pittsfield, Mass.): Messrs. Prince and Williams have presented a development that is a very noteworthy advancement in the art of overcurrent protection. In comparing this new design with existing fuses, too much attention cannot be given to the principles described which produce the necessary consistent, high degree of current limitation below that available in the circuit. Many times tests have shown existing types of oil, expulsion, and other fuses to have current-limiting characteristics. This most generally occurs with the lower current ratings of fuse links. Fuses may be divided into two groupings as regards the consistency and degree of this current-limiting ability.

The first group of fuses comprises the more commonly known oil, expulsion, and other similar types. These employ the heat generated by the arc to aid in the elongation of the arc path and to provide pressure, turbulence, a gaseous medium, or other means of rapidly decreasing the conductivity between the burned ends of the fuse to prevent re-establishment of the arc after the first current zero. While current-limiting action occurs occasionally, it is not a consistent characteristic of this group, and is never attained to the degree shown by the present design described by the authors.

The second group of fuses has two basic functions, which must be met consistently, namely,

1. Instantaneously at the end of the melting period it must in effect introduce a high resistance so as to cause an immediate limitation or decrease in current.
2. It must maintain this resistance at a sufficiently high value to limit the current appreciably below that available in the circuit in which the fuse is connected.

Available current-limiting fuses accomplish this by:

1. Instantaneous physical removal of the fusible conductor throughout the full arc length required for interruption.
2. Subjecting the arc to a high degree of cooling by a closely associated filler material which is capable of high heat absorption without loss of dielectric strength.

The efficiency of such a fuse in limiting current is dependent upon the ability of the long fusible section to volatilize instantaneously, thus immediately creating the ultimate length of arc, coupled with the characteristics of filler in the absorption of heat and the volatilized metal as indicated by the authors. Certain materials are exceedingly efficient in these respects, especially when combined with certain physical arrangements. They function instantaneously and reduce arc current, to a very low value immediately upon the melting of the fuse link, see figure 2 of the paper. Thus the arc energy is minimized to such an extent that little or no pressure is developed within the fuse unit. With less efficient fillers, the current continues to rise, after the fuse link melts, figure 1, the extent being dependent

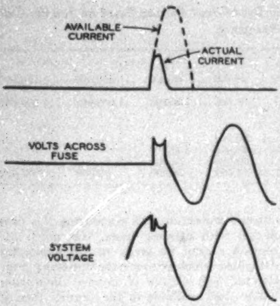


Figure 1

upon the characteristics of the fusible metal, the filler, and the physical arrangement. Naturally, the arc energy and pressure generated within the fuse unit are much greater with this increase in arc current and time. Thus while current-limiting fuses employing less efficient fillers are possible, they present real design problems in making the fuse tube or container of sufficient strength to withstand the greater stresses. So far this, coupled with economical fuse design, has imposed a definite limitation on the interrupting rating of such fuses considerably below that of the type described by the authors. Also, there is a tendency for these fuses with less efficient fillers to be affected more by circuit conditions and the point of the wave at which the fault occurs making them somewhat less consistent. This differentiation showing the marked improvement of the development described by Prince and Williams is based on four or five years' designing and testing and three years' actual service experience with current-limiting fuses of similar physical construction employing both the highly efficient and the less efficient fuse fillers.

In testing fuses in group 1, which depend upon the forces of the arc to help to extinguish it, the root-mean-square current measured at the instant arcing occurs is in general approximately the same as the symmetrical or the asymmetrical current available in the test circuit. Thus the root-mean-square current, initial in arc which the fuse actually interrupts is a true measure of the interrupting ability of fuses in group 1. This is not true with the fuses of group 2 which consistently limit the current to values appreciably below that available in the test circuit. Oscillograph records of the currents from tests with current-limiting fuses only indicate the maximum current the fuses will permit when connected in circuits having available power equal to that of the test circuit.

Therefore, in testing the current-limiting fuses of group 2, it is necessary to determine by oscillograph measurements the symmetrical and asymmetrical currents available in the circuit without the fuse as well as the instantaneous peak current the fuse will permit to flow in that circuit under both symmetrical and asymmetrical conditions. The interrupting rating should be based on the power available in the circuit. The current-limiting efficiency of the fuse is indicated by the comparison of the current permitted by the fuse in circuits of varying magnitude and the currents actually available in these circuits, see figure 7 of the paper. This is of vital importance to anyone applying these fuses from the standpoint of both economics and safety, as it indicates the stress to which connected apparatus will be subjected. Thus current-limiting fuses should have both an interrupting rating and a current-limiting rating. The interrupting rating should be based on the power available in the circuit of greatest magnitude in which the fuse functioned successfully in interrupting a dead short circuit. The current-limiting rating should be based on the maximum instantaneous peak current the fuse will permit during interruption in a circuit having available a root-mean-square current equal to the interrupting rating.

E. J. Burnham (General Electric Company, Schenectady, N. Y.): For a long time there has been a need for a better power fuse that could be used either indoor or outdoor and on circuits at or close to large stations where the available short-circuit current is high.

The fuse described meets these requirements exceptionally well.

In central-station work it is often desirable to connect potential transformers or control power transformers to a bus or a circuit where the available short-circuit current is very high. In the past, fuses have not been available to take care of many cases with the result that it often became a choice as to whether a circuit breaker should be used, or whether the transformer should be connected directly to the bus or line.

The first choice would mean an expensive device and the second choice would mean an interruption in case of transformer failure. The fuse described should, therefore, have a wide use as a means of fusing potential and control power transformers.

Another application for these fuses where an indoor type of fuse is desirable is for fusing the high-voltage side of metal-enclosed unit-type substations where the high-voltage side of the transformers may be 13,200 volts to 23,000 volts.

H. L. Rawlins (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors of this paper are to be congratulated for successfully working out the design of a current-limiting power fuse. The writer feels that this would be no easy task even with fuses of the same general type available from European practice.

In 1931 current-limiting fuses of the same general type as described in this paper were imported from Europe, their design studied, and exhaustive tests conducted to determine the suitability of the fuse for use in this country. As a result of this study it was decided to proceed with the development of the boric-acid fuse for the following reasons.

1. The maximum current rating and kilovolt-ampere carrying capacity of the current-limiting fuse is inherently limited. To cover adequately the field of fuse applications in this country would require two totally dissimilar lines of power fuses.

2. Where a totally enclosed and quiet operating fuse is required, a condenser can easily be attached to the boric-acid fuse.

3. The current-limiting fuse requires a long fusible section. This is undesirable from the standpoint of heat generation at currents near the melting current of the fuse. Even with inorganic material used throughout the fuse, it was feared that excessive temperature rises would occur on the contacts.

4. The benefit to the system and to the protected apparatus from the current-limiting action of the fuse was regarded as more theoretical than practical. On heavy fault currents the boric-acid fuse gives half-cycle operation. This will result in no serious disturbance to the systems and will adequately protect the lighter duty equipment usually employed on the load side of the fuse.

5. Although the interrupting action of the fuse was ideal for potential transformer protection, it was realized that the long fine fusible element could not provide adequate protection on secondary short circuits without causing unnecessary outages on magnetizing surges.

In the study of these fuses it was observed that more reliable operation was obtained on the lower current ratings where the current-limiting action was more pronounced. Although there have been many improvements in design and materials since that time, we are wondering if the maximum limit in current rating at a given voltage is the current rating at which the current-limiting action is not sufficient to insure reliable performance.

In table I actual recovery rates are listed. Are these the actual recovery rates at final interruption, or are they the rate of voltage rise at the time of peak current as shown in the cathode-ray oscillograms? Apparently, they are the latter in which case the term "recovery rate" might be questioned. It would seem that the important factors would be the available recovery rate and the maximum voltage peak occurring during interruption.

D. C. Prince and **E. A. Williams, Jr.:** The authors of "The Current-Limiting Power Fuse" are very grateful for the unusually constructive discussions of their paper. They feel that the replies to the many questions raised will help the ultimate users of this new device to take advantage of several operating characteristics which otherwise might not have been appreciated.

Mr. LeClair draws an interesting comparison between the new fuse and high speed d-c circuit breakers. The two devices are somewhat comparable in speed and both of them reduce the destructive effect of high currents, but the fuse is relatively more effective due to the fact that in the fuse the arc is initiated at its maximum length whereas in the breaker the arc must be drawn out to its maximum length. Since both the arc and the moving parts of the breaker have mechanical inertia, the drawing out necessarily requires a certain amount of time during which the current continues to increase. The current through the fuse does not increase after arcing begins.

The only inherent limitation upon the interrupting ability of the fuse appears to be the ability of the interrupting medium to absorb the power loss in the arc. This means that, at least theoretically, a device of this type may be designed with practically unlimited interrupting capacity by subdividing the arc into many parallel paths so that each path may take only that amount of arc energy which it is capable of absorbing. However, the number of subdivisions which can be made are limited by economics and practical manufacturing considerations. The parallel arc paths must be kept sufficiently far apart to prevent any material effect upon each other and, therefore, the size and cost of the device increases directly with the number of conductors used for any given continuous current rating. It obviously follows that the interrupting rating of the fuse can only be increased to the point at which the annual renewal cost of the fuse approaches the annual cost of operating a circuit breaker.

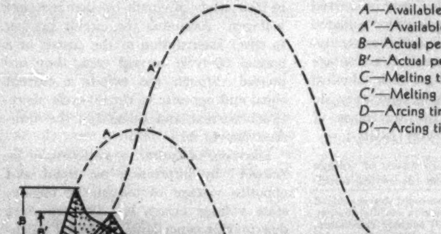
The interrupting capacity of any a-c interrupting device, having sufficient speed to operate within the first few cycles of short-circuit current, is more or less affected by the d-c component of the current. It is rather generally agreed that the introduction of the d-c component of current increases the interrupting duty imposed upon the conventional fuse or circuit breaker as the arc energy is a function of the total arc current. In contrast, the d-c component of currents near the interrupting capacity of the current-limiting fuse definitely decreases the interrupting duty by decreasing the initial rate of rise of the current. With the lower rate of current rise, the fuse melts at a lower instantaneous current, as illustrated in figure 2 of this discussion, and somewhat less arc energy must be absorbed by the interrupting medium. Specifically, the interrupting capacity of the fuse depends upon the point on the voltage wave at which the short circuit occurs and the fuse will interrupt considerably more current with a completely offset current wave than with a symmetrical wave.

If the current-limiting fuse described in the paper is subjected to a current or a voltage above that at which it is designed to operate, the arc energy becomes sufficient to increase the temperature of the interrupting medium (quartz granules) to a point at which it loses its dielectric strength and becomes a conductor. The short-circuit current continues until it is interrupted by a protective device at some other location in the circuit. The damage to the fuse depends entirely upon the duration of the short-circuit current. During failure there is practically no pressure generated and no explosive action. The disturbance is in no way comparable to that encountered under similar conditions with the conventional fuse or circuit breaker in which pressure is generated.

The general conception of the terms "arc voltage" and "rate of rise of recovery voltage," as applied to conventional interrupting devices, must be somewhat modified when applied to the current-limiting fuse. Arc voltage is defined as the voltage across the terminals of an interrupting device during the arcing period. The arc voltage of a conventional interrupting device is very low, relative to the open-circuit voltage, and is usually almost 90 degrees out of phase with a symmetrical short-circuit current. Just previous to arc interruption the voltage tends to be at its peak and just after interruption it rapidly "recovers" to a magnitude approaching twice its normal peak. The arc voltage of a current-limiting fuse, due to the high arc resistance, is practically equal to the open-circuit voltage except for an initial voltage surge as shown in figure 6 of the paper, and is almost in phase with the current. Arc interruption tends to occur at the zero of both voltage and current so that just after interruption the voltage does not suddenly increase but builds up along a normal-frequency wave. The voltage across the fuse "recovers" at the inception of the arc whereas the voltage across the conventional interrupting device "recovers" after the extinction of the arc. In either case, the rate of rise of recovery voltage is a function of both the device and the circuit characteristics. With the conventional device, the circuit characteristics predominate while with the current-limiting fuse the device characteristics predominate in determining the rate of rise and the magnitude of recovery voltage.

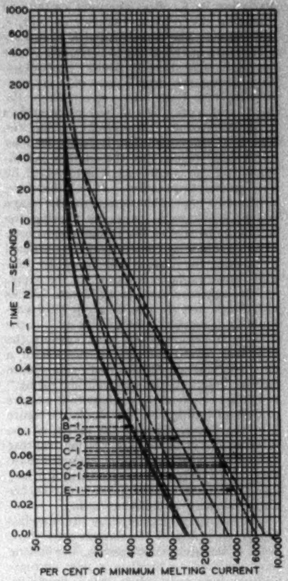
In direct reply to Mr. LeClair's question concerning the effect upon the rate of rise of recovery voltage of operating the fuse in parallel with a reactor, it may be concluded from the above discussion that the effect will be negligible.

A typical melting time-current curve for the current-limiting fuse is compared in figure 3 of this discussion with similar curves for conventional high-voltage power fuses. The curves are plotted in per cent of minimum melting current to eliminate differences due to methods of rating and thereby provide a common basis of comparison which is independent of rating standards. A certain amount of error is introduced by plotting the curves on a per cent of rating basis and many of the individual fuses will not exactly meet the curves. In general, the fuses of low continuous-current ratings will be somewhat faster and the fuses of high continuous-current ratings will be somewhat slower than shown by the curves. It will be noted that the melting time of the current-limiting fuse at the currents shown is not essentially different from that of existing conventional fuses and that the fuse may be co-ordinated with relays and other fuses. Since the melting time is inversely proportional to the square of the current, the speed of operation increases very rapidly as the current increases and the reputed extreme speed of the current-limiting fuse, relative to other fuses, exists principally because of its ability successfully to interrupt higher currents.



A—Available symmetrical wave
A'—Available asymmetrical wave
B—Actual peak current, symmetrical
B'—Actual peak current, asymmetrical
C—Melting time, symmetrical
C'—Melting time, asymmetrical
D—Arcing time, symmetrical
D'—Arcing time, asymmetrical

Figure 2. Comparison of fuse operation on symmetrical and asymmetrical currents



A—Current-limiting fuse
B-1—Manufacturer *B*, fast-operating fuse
B-2—Manufacturer *B*, standard fuse
C-1—Manufacturer C, standard fuse
C-2—Manufacturer C, time-lag fuse
D-1—Manufacturer *D*, standard fuse
E-1—Manufacturer *E*, standard fuse

Figure 3. Comparison of time-current curves

The melting time-current curves of all types of fuses, utilizing a pure fusible metal, have the same fundamental shape as they are hyperbolic in form and may be approximately determined by the general equation:

$$t = \frac{K}{i^2 - I^2}$$

where

t = time to melt at any current i
I = minimum melting current as t approaches infinity
K = the square of current required to melt the fuse in one second when all generated heat is retained in the fusible conductor

Since the current I is arbitrarily fixed by the continuous current rating of the fuse, a variation in K offers the only method of changing the relation between current, i, and time t. The magnitude of K is entirely a function of the physical constants of the material used in the fusible conductors. Considering the specific case of a current-limiting fuse built into a given tube, the total cross section of the fusible conductors is determined by the current rating, the length of the conductors is determined by the voltage rating, and the number and diameter of the conductors is determined by the interrupting rating. It is, therefore, practically impossible to control the shape of the time-current curve by varying the number and size of the fusible conductors. Some measure of control may be obtained by varying the number and size of the thermal chambers shown in figure 1 of the paper.

The suggestion is made by Messrs. Lincks and Smith that the fuse be given a current-limiting rating which they define as "... the maximum instantaneous peak current the fuse will permit during interruption in a circuit having available a root-mean-square current equal to the interrupting rating." It is the opinion of the authors that a series of current-limiting curves, similar to figure 7 of the paper, showing the action of the fuses at all currents below the interrupting ratings will be of more value to the ultimate user. The proposed current-limiting ratings will then be a point on the curve rather than a separate and distinct rating.

The discussion presented by Mr. Rawlins brings out many of the reasons why three years of research, development, and testing were required to produce a new current-limiting fuse even when detail drawings, test data, and samples of certain European designs were at the disposal of the authors.

The new fuse is so designed that the temperature rise is within the limits specified by AIEE standards. The potential-transformer fuse unit is mechanically strong, it will function during secondary short circuits, and it has sufficient thermal capacity easily to withstand magnetizing surges. The reliability of the higher current ratings equals the reliability of the lower current ratings as each individual conductor is designed to interrupt approximately the same current regardless of the number of conductors, the voltage rating, or the continuous-current rating of the fuse. The current-limiting fuse requires no muffler and therefore it is unnecessary to reduce the interrupting rating for indoor application.

Mr. Rawlins has suggested that the maximum continuous-current and voltage ratings of the current-limiting fuse may be inherently limited. No such limitation is recognized except that imposed by the physical size of the unit which may be conveniently handled and economically operated.

Short-time current tests made on disconnecting switches, contactors, and current transformers have shown that these devices may be destroyed during the first half cycle of short-circuit current. The results of these practical tests merely serve to check a well-established theory and to indicate the value of a new protective instrument which limits the current in the first half cycle to a low value thereby protecting utilization equipment, of low short-time current rating, and insuring the supply system against even momentary currents of high magnitude.

Traveling Waves Initiated by Switching

By L. V. BEWLEY
MEMBER AIEE

THE dominating position of the lightning surge as the principal scourge of transmission lines has occupied so much attention in the technical literature that traveling waves developed from other causes have been relegated to a zone of comparative unimportance. But lately, a growing interest and curiosity concerning switching surges makes it imperative to investigate, theoretically, the possibility of initiating traveling waves from the steady state; for both the "energizing" and "de-energizing" transients associated with switching operations are calculable as traveling waves and their successive reflections. The object of this paper, therefore, is to lay the groundwork for a systematic treatment of switching surges by considering the general subject of the initiation of traveling waves from steady-state conditions. In general, such waves are started either by *closing* or *opening* a switch; and the corresponding transients may be called *closing transients* and *opening transients*, respectively. If a "dead" line is connected to a generator, the ensuing transient is called an *energizing surge*, while if a "live" line is disconnected from a generator the transient is called a *de-energizing surge*.

There are at least three methods by means of which a switching transient may be calculated, and these, in the order of their importance, as well as simplicity, will be called: (1) cancellation waves, (2) initiated waves, and (3) steady-state waves. As a matter of fact, the designation is rather loose; for all three are mutually convertible; and the line of demarcation between them is sometimes vague. Also, both "cancellation waves" and "steady-state waves" may be derived from the general equation for "initiated waves." It is rarely indeed that steady-state waves show a practical advantage over the other two, but their physical significance is intriguing, and they do provide variety in the treatment of switching problems. Very briefly:

1. *Cancellation waves* are waves of voltage (or current) impressed on the switch terminals to cancel the steady-state voltage (or current) of the switch, and thereby simulate its closing (or opening).

2. *Initiated waves* are waves computed directly from the general equations for an interruption of circuit.

3. *Steady-state waves* are pairs of fictitious waves which completely duplicate the actual steady-state distributions of voltage and current on the line and at its terminals; so that a change of circuit conditions, as by switching, may be negotiated by computing the subsequent reflections of these waves.

These three methods will now be considered in detail and illustrated by a few simple examples—often the same for each method, so as to allow a direct comparison of their relative merits.

Cancellation Waves

An old artifice for determining the effect of "opening" a switch (probably introduced by Heaviside) is to cancel the current in the switch by impressing an equal and opposite current so that the resultant current is zero and the switch is essentially open. Of course this impressed current, suddenly applied, must flow into the connected network, and will cause some sort of a transient therein. The resultant distribution of voltage or current in the network is the sum of that due to the steady state and the transient; that is

$$\left(\begin{array}{c}\text{Resultant}\\ \text{distribution}\end{array}\right) = \left(\begin{array}{l}\text{Steady-state}\\ \text{distribution if}\\ \text{switch is not}\\ \text{operated}\end{array}\right) + \left(\begin{array}{l}\text{Transient due to}\\ \text{switch cancellation}\end{array}\right) \quad (1)$$

This device was used by Park and Skeats[1] in their study of circuit-breaker recovery voltages. Assuming the circuit breaker to effect interruption of the circuit at a normal 60-cycle current zero, they impressed through the switch a current equal and opposite to the 60-cycle short-circuit current and calculated the transient due to it.

Likewise, of course, a switch may be "closed" by impressing an equal and opposite voltage to cancel the steady-state voltage across it. The transient due to this cancellation is easily calculated, and the resultant distribution is again given by (1).

Paper number 38-55, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted November 17, 1937; made available for preprinting May 6, 1938.

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[1] For numbered references, see list at end of paper.

Referring to figure 1 there is shown a bus on which n lines of surge impedances ($z_1, z_2, \ldots, z_n$) and series impedances $W_k(p)$ (for example, current limiting reactors or transformers) terminate. There are also m generators or motors with series impedances. By putting the voltage of a generator equal to zero a fixed impedance load is specified. By convention, currents (except through the switch) flowing to the bus are regarded as positive, and therefore waves on the line are forward waves if approaching the bus, or backward waves if receding away from the bus. A generalized impedance $Z(p)$, which may or may not include energy sources such as generators, charged capacitances or current-carrying inductances, is to be switched on to or off the bus. The steady-state voltages of the lines and generators are ($E_1, E_2, \ldots$), while the steady-state currents are ($I_1, I_2, \ldots$). The total impedance on the bus, excepting $Z(p)$, is

$$Z_0(p) = \frac{1}{\sum_1^n \frac{1}{z_k + W_k(p)} + \sum_{n+1}^{n+m} \frac{1}{W_k(p)}} \tag{2}$$

The total current flowing through the switch *away* from the bus is

$$I_0 = \sum_1^{n+m} I_k \tag{3}$$

and the steady state voltage on the bus is

$$E_0 = E_k - W_k(p) \cdot I_k \tag{4}$$

Opening the Switch

The opening of the switch is simulated by applying the current $I_0\mathbf{1}$ through it, and this current will cause a bus voltage of

$$e_0 = Z_0(p) \cdot I_0 \tag{5}$$

The backward voltage wave appearing on the kth line then is

$$e_k' = \frac{z_k}{W_k(p) + z_k} Z_0(p) \cdot I_0 \tag{6}$$

and the transient current from the kth generator becomes

$$i_k = -\frac{Z_0(p) \cdot I_0}{W_k(p)} \tag{7}$$

These transient terms superimpose on the steady-state values. For example, the total voltage on the kth line is ($e_k' + E_k$), or the total current in the kth generator is ($i_k + I_k$), etc.

Closing the Switch

The closing of the switch is simulated by impressing across its terminals a voltage $-E_0\mathbf{1}$. This voltage is in series with $Z_0(p)$ and $Z(p)$ and will, therefore, circulate a current

$$i_0 = \frac{-E_0}{Z_0(p) + Z(p)} \tag{8}$$

and the transient bus voltage will be

$$e_0 = -\frac{Z_0(p)}{Z_0(p) + Z(p)} E_0 \tag{9}$$

The voltage wave appearing on the kth line then is

$$e'k = -\frac{z_k}{W_k(p) + z_k} \cdot \frac{Z_0(p)}{Z_0(p) + Z(p)} E_0 \tag{10}$$

and the transient current from the kth generator becomes

$$i_k = \frac{1}{W_k(p)} \cdot \frac{Z(p)}{Z_0(p) + Z(p)} E_0 \tag{11}$$

These transient terms superimpose on the steady-state terms according to (1).

In addition, if $Z(p)$ includes energy sources, such as generators, charged capacitances, or current-carrying inductances; transient terms caused by the discharge of these energy sources must be calculated either from the differential equations of the circuit, taking cognizance of the initial conditions of $Z(p)$, or else by substituting ($E - E_0$) for E_0 where E is the voltage of $Z(p)$ at the instant of switching. It is easier to illustrate the procedure in particular examples, rather than attempt a generalized formulation.

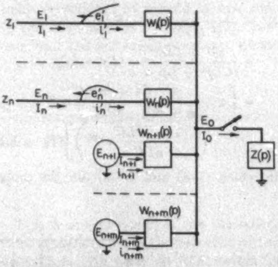

Figure 1. Switching a generalized impedance on a bus with any number of feeders and generators

When the cancellation waves reach the other ends of the lines, they reflect like any other traveling wave, so that the resultant distribution on the system is:

$$\begin{pmatrix}\text{Resultant}\\ \text{distribution}\end{pmatrix} = \begin{pmatrix}\text{Steady-state}\\ \text{distribution}\end{pmatrix} + \begin{pmatrix}\text{Waves of}\\ \text{cancellation}\end{pmatrix} + \begin{pmatrix}\text{Successive}\\ \text{reflections}\end{pmatrix} \tag{12}$$

A few examples will clarify the procedure.

Sudden Grounding of a Charged Line

Figure 2 shows a line, charged to a voltage E, suddenly grounded at one end. The cancellation wave is obviously $e' = -E\mathbf{1}$, which also follows from (10) upon putting $W(p) = 0$, $Z(p) = 0$, and $Z_0(p) = z$. This wave, starting at the switch at the instant when it is closed, cancels the steady-state voltage E at the switch thereafter. The subsequent successive reflections are given by the lattice, and the resultant voltage at the free end of the line, compounded of E, $-E\mathbf{1}$, and the successive reflections, is plotted on the left of the lattice to the same time scale.

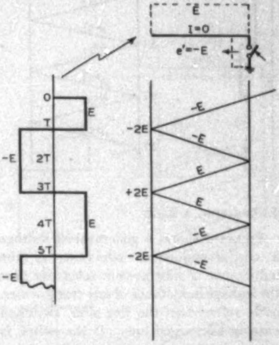

Figure 2. Short-circuiting a charged line

Clearing a D-C Line Short-Circuit

In figure 3 is indicated the removal of a line-to-ground short-circuit of a d-c line. The steady-state current through the switch is $I_0 = E/R$, and therefore the cancellation wave is $e' = zI_0$ where z is the surge impedance of the line. This also follows from (6) upon putting $W(p) = 0$ and $Z_0(p) = z$. When this wave reaches the generator end it reflects from the series resistor R as

$$\frac{(R - z)}{(R + z)} zI_0 = \frac{(R - z)}{(R + z)} z \frac{E}{R}$$
$$= \frac{(1 - z/R)}{(1 + z/R)} \frac{z}{R} E$$

The resultant voltage at the two ends of the line has been plotted for a value of $z/R = 4$. Of interest is the oscillation about normal voltage to four times normal at the opened end. This corresponds to the "inductive kick" of lumped circuits.

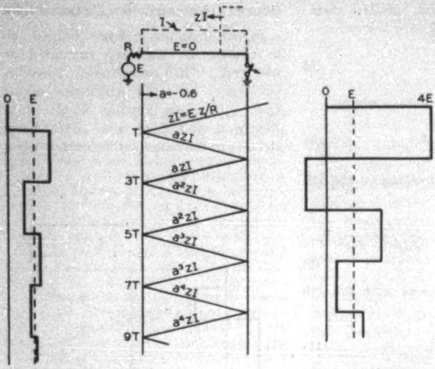

Figure 3. Removal of a short circuit from a d-c line with series resistance

ENERGIZING A LINE

Figure 4 shows a generator of voltage $E \cos \omega t$ and series inductance L (the inductance of subtransient reactance plus the leakage inductance of any transformer, both referred to the line side) switched onto an uncharged line. If the switch is closed at the crest of normal frequency voltage, the voltage across the switch is $-E$, so that the cancellation wave across the switch is $+E$. Putting $Z_0(p) = z$ and $Z(p) = pL$ and $W(p) = 0$ in (10) there is (writing $\alpha = z/L$):

$$e' = \frac{z}{z + pL} E = E(1 - \epsilon^{-\alpha t})$$

The reflection factor at the generator end is $(pL - z)/(pL + z)$, and the $(n+1)$th reflection at the open end gives:

$$2E\left(\frac{p-\alpha}{p+\alpha}\right)^n (1 - \epsilon^{-\alpha t}) =$$

$$2\,E\left\{(-1)^n - \epsilon^{-\alpha t}\left[\frac{(-\alpha t)^n}{\lfloor n} + \sum_1^n (-1)^k \frac{(-\alpha t)^{n-k}}{\lfloor n-k} + \sum_1^n{}_k \sum_1^k{}_r \frac{\lfloor n \,(-\alpha t)^{n-r+1}}{k \lfloor n-k \lfloor r-1 \lfloor n-r+1}\right]\right\}$$

$$= \begin{cases} 2E\{1 - \epsilon^{-\alpha t}\} & \text{for } n = 0 \\ 2E\{-1 + \epsilon^{-\alpha t}[1 + 2\alpha t]\} & \text{for } n = 1 \\ 2E\{1 - \epsilon^{-\alpha t}[1 + 2(\alpha t)^2]\} & \text{for } n = 2 \\ 2E\{-1 + \epsilon^{-\alpha t}[1 + 2\alpha t - 2(\alpha t)^2 + \frac{4}{3}(\alpha t)^3]\} & \text{for } n = 3 \end{cases}$$

These first four reflections are plotted on figure 4 for $\alpha T = 0.4$. The voltage at the open end builds up, in this case, to slightly more than double voltage. For larger values of αT the voltage may reach approximately, $2.5E$ (neglecting attenuation). The energizing transient is not, therefore, severe.

SYNCHRONIZING OUT OF PHASE

Suppose that a line of voltage E_1 is switched onto a bus of voltage E_2. The bus may be regarded as a small capacitance C, fed by a generator of series inductance L, as shown in figure 5. The current in the inductance prior to switching may be neglected. The voltage across the switch is $(E_1 - E_2)$ and, therefore, the cancellation voltage is $-E_0\mathbf{1} = (E_2 - E_1)\mathbf{1}$. In this case $Z_0(p) = z$ and $Z(p) = pL\ /\ (1 + CLp^2)$. The cancellation wave on the line is given by (10) as

$$e_1' = \frac{z}{z + Z(p)}(E_2 - E_1)$$

$$= \frac{z(1 + CLp^2)}{zCLp^2 + Lp + z}(E_2 - E_1)$$

$$= \left[1 - \frac{1}{n-m}\left(\frac{1 + m^2LC}{mLC}\epsilon^{-mt} - \frac{1 + n^2LC}{nLC}\epsilon^{-nt}\right)\right](E_2 - E_1)$$

in which $(-n)$ and $(-m)$ are the roots of $(zCLp^2 + Lp + z = 0)$. This same result can be obtained by superposition, first applying $(-E_1\mathbf{1})$ across the switch, and to this result adding the discharge of the generator into the line with C initially charged to voltage E_2. For a typical case of $L = 0.10$, $C = 0.01$ microfarad, and $z = 500$ ohms there is $m = 0.20$ and $n = 0.005$; and the initial wave is

$$e_1' = (E_2 - E_1)[1 + 1.025\,\epsilon^{-0.2t} - 1.025\,\epsilon^{-0.005t}$$

This has been plotted in figure 5 for the worst possible case of $E_2 = -E_1$. The dip in the wave is due to the bus capacitance. Due to reflections, synchronizing 180 degrees out of phase may result in voltages of three to four times normal.

Initiated Waves

In appendix I the appropriate set of simultaneous equations are derived for calculating directly the waves and transient terms initiated by switching, and it is shown that these are identical with the cancellation waves given by the previous equations (5), (6), and (7) (for *opening* a switch), and (8), (9), and (10) (for *closing* a switch). There is, then, no distinction between "initiated waves" and "cancellation waves"—merely a difference in the point of view from which they are derived—and, therefore, the previous examples may be considered to illustrate either method.

Steady-State Waves

Steady-state waves are a generalization of the well-known fact that the ordinary a-c equations of a transmission line operating in the steady state may be expressed as a pair of waves traveling in

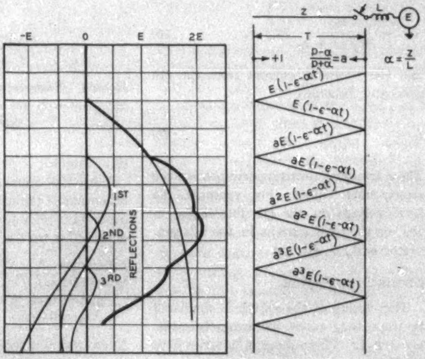

Figure 4. Energizing a line

opposite directions. It is shown in appendix II that these waves are (see figure 6):

$$e_s\left(t - \frac{x - x_0}{v}\right) = \frac{1}{2}\left[E\left(t - \frac{x - x_0}{v}\right) + z \cdot I\left(t - \frac{x - x_0}{v}\right)\right]$$

forward

$$e_s'\left(t + \frac{x - x_0}{v}\right) = \frac{1}{2}\left[E\left(t + \frac{x - x_0}{v}\right) - z \cdot I\left(t + \frac{x - x_0}{v}\right)\right]$$

backward

in which $E(t)$ and $I(t)$ are the steady-state voltage and current, respectively, at point $x = x_0$ on the line. It is also shown in appendix I that the "steady-state" waves not only satisfy the voltage and current distribution along the line, but also duplicate its terminal performance, if e_s' be regarded as the "reflection" at one end and e_s the "reflection" at the other. The associated current waves are

$$i_s = \frac{e_s}{z} \text{ and } i_s' = -\frac{e_s'}{z}$$

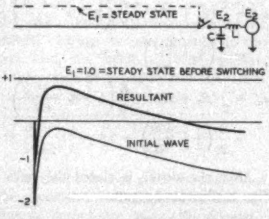

Figure 5. Synchronizing 180 degrees out of phase

If, now, at any point on the line the circuit is changed by a switching operation, the "steady-state" wave *receding* from that point moves away without consequence, but the "steady-state" wave *approaching* that point impinges on the new impedance introduced by the switching and gives rise to a reflection in accordance with conventional traveling-wave theory. If $Z(p)$ is the new terminal impedance, following switching, which may in general contain energy sources such as generators, charged capacitances, etc., then the reflected wave is

$$e' = \left(\frac{Z(p) - z}{Z(p) + z}\right)e_s + \left(\text{Discharge of the energy sources of } Z(p)\right)$$

A few simple examples will clarify the conception. Except in very simple cases it is too complicated as compared with "cancellation waves" or with "initiated waves;" because there are *two* "steady-state" waves of *finite* length (the length of the line) instead of a *single infinite* wave to deal with. This not only necessitates a double reflection lattice, but discontinuities at both head and tail of the waves. Generally, then, if successive reflections are to be taken into account, there is four times the work with "steady-state" waves as with either of the other two methods. It is shown in appendix II, however, that "steady-state" waves always combine in such a way as to yield an equivalent single infinite wave.

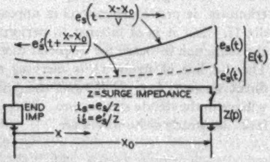

Figure 6. Steady-state waves

SUDDEN GROUNDING OF A CHARGED LINE

Figure 7 shows an open transmission line charged to a uniform voltage E, suddenly shorted to ground at one end. In this case there is no steady-state current, and the voltage is constant; hence $E(t) = E$ and $I(t) = 0$ so that

$$e_s = e_s' = \frac{E}{2}$$

while

$$i_s = -i_s' = \frac{1}{2}\frac{E}{z}$$

It is seen that e_s and e_s' are the reflections of each other at the two ends of the open line. As soon as the switch is closed e_s impinges on a grounded end, $Z(p) = 0$, causing a reflection $-E/2$, while the wave e_s' moving to the left reflects therefrom as a finite rectangular wave $+E/2$. The successive reflections from the two ends shuttle back and forth, as given by the lattice, and the resultant voltage is found in the usual way by combining the successive reflections at proper time intervals, giving the oscillation shown in figure 7.

CLEARING A LINE SHORT CIRCUIT

Figure 8 shows a d-c generator and series resistance R supplying a line short-circuited at its far end. The short-circuit current is $I(t) = E/R$ and the line voltage is $E(t) = 0$, since line resistance is neglected. Therefore, the "steady-state" waves are

$$e_s = \frac{z}{R}\frac{E}{2} \text{ and } e_s' = -\frac{z}{R}\frac{E}{2}$$

At the shorted right-hand junction e_s' is obviously the reflection of e_s. At the left-hand junction there is by (13), appendix II,

$$\begin{aligned} e_s &= \left(\frac{z}{R + z}\right)E + \left(\frac{R - z}{R + z}\right)e_s' \\ &= bE + ae_s' \\ &= \left(\frac{z}{R + z}\right)E + \left(\frac{R - z}{R + z}\right)\left(\frac{-z}{R}\frac{E}{2}\right) \\ &= \frac{z}{R}\frac{E}{2} \end{aligned}$$

in which the first term on the right is the discharge of the energy source (the generator), and the second term the reflection of e_s'.

$$e_s' + e_s = -\frac{z}{R}\frac{E}{2} + \frac{z}{R}\frac{E}{2} = 0$$

thus correctly satisfying the line and terminal conditions.

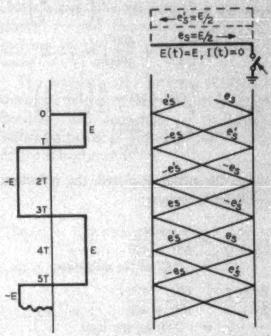

Figure 7. Short-circuiting a charged line

Now clear the short circuit, so that e_s thereafter meets an open-ended line, and reflects completely. The subsequent reflections are given on the lattice of figure 8 for $z/R = 4$, in which the reflection is $e' = e$ from the right and

$$e' = bE + ae = 0.8E - 0.6e$$

from the left. The voltage at both ends of the line oscillates about normal generated voltage, but reaches four times normal at the far end.

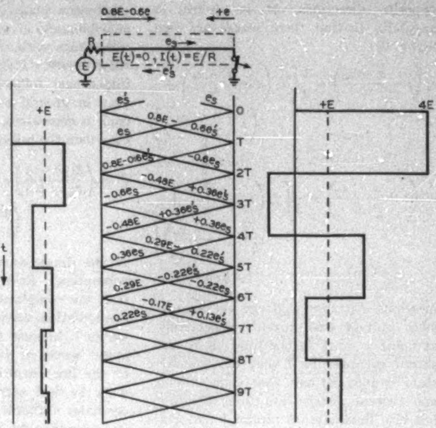
0.8E-0.6e
+e
R
E
e_s
E(t)=0, I(t)=E/R
e_s'
+E
t
e_s'
e_s
0
0.8E − 0.6e_s'
e_s
T
0.8E−0.6e_s'
−0.6e_s
2T
−0.48E
+0.36e_s'
−0.6e_s
3T
+0.36e_s'
−0.48E
+0.36e_s
4T
0.29E
0.22e_s'
0.36e_s
5T
−0.22e_s'
0.29E
−0.22e_s
6T
−0.17E
0.22e_s
+0.13e_s'
7T
8T
9T
+E
4E

Figure 8. Removal of a short circuit from a d-c line with series resistance

Suddenly Grounding and Clearing the Midpoint of a Line

In figure 9 a d-c line is shown with a $R = 100$ ohm load at the far end, and a resistor $r = 100$ ohms ready to be closed in at the midpoint. The line surge impedance is $z = 400$ ohms. There are now two pairs of steady-state waves; one pair on either side of the transition point They are:

$$e_1 = e_2 = \frac{1}{2}(E + zI) = \frac{E}{2}\left(1 + \frac{z}{R}\right)$$

$$= \frac{E}{2}\left(1 + \frac{400}{100}\right) = 2.5\ E$$

$$e_1' = e_2' = \frac{1}{2}(E - zI) = \frac{E}{2}\left(1 - \frac{z}{R}\right)$$

$$= \frac{E}{2}\left(1 - \frac{400}{100}\right) = -1.5E$$

When the switch is closed, the reflection coefficients become:

$$\frac{-z}{2R + z} = \frac{-400}{200 + 400}$$

$= -0.67$ at the midpoint

$$\frac{R - z}{R + z} = \frac{100 - 400}{100 + 400}$$

$= -0.60$ at the load

-1.00 at the generator

The lattice of figure 9 gives the reflection history of the four steady-state waves once the switch is closed, and the resultant voltage at the switch is shown. The voltage suddenly drops to a third of normal, and then builds up to generator voltage in a series of steps at intervals of $2T$, the time of transit of the line.

Suppose, now, after new steady-state conditions have been obtained, that the switch is opened, figure 10. For example, a line-to-ground fault clearing itself. In this case, just prior to the opening of the switch, a current E/R is flowing in the right-hand section of the line, and a current $E(1/R + 1/r)$ in the left-hand section. The steady-state waves are

$$e_1 = \frac{1}{2}(E_1 + zI_1) = \frac{E}{2}\left(1 + \frac{z}{R}\right)$$

$$= \frac{E}{2}\left(1 + \frac{400}{100}\right) = +2.5$$

$$e_1' = \frac{1}{2}(E_1 - zI_1) = \frac{E}{2}\left(1 - \frac{z}{R}\right)$$

$$= \frac{E}{2}\left(1 - \frac{400}{100}\right) = -1.5$$

$$e_2 = \frac{1}{2}(E_2 + zI_2) = \frac{E}{2}\left(1 + z\,\frac{r + R}{rR}\right)$$

$$= \frac{E}{2}\left(1 + \frac{400}{50}\right) = 4.5$$

$$e_2' = \frac{1}{2}(E_2 + zI_2) = \frac{E}{2}\left(1 - z\,\frac{r + R}{rR}\right)$$

$$= \frac{E}{2}\left(1 - \frac{400}{50}\right) = 3.5$$

After the switch is closed the reflection coefficients become:

-1.0 at the generator

0.0 at the midpoint (merely a point of passage)

-0.6 at the load

The lattice of figure 10 gives the history of the reflections and the resultant voltage at midpoint. The voltage suddenly leaps to three times normal, and then oscillates about normal voltage with decreasing amplitude.

This example is somewhat similar to the operating cycle of an expulsion gap when tower footing resistance is involved.

Equivalence of "Steady-State" Waves to a Single Infinite Wave

That the pair of "steady-state" waves is always reducible, in effect, to a single infinite wave plus the steady-state distribution, is proved in general in appendix II. But it is of interest to perform the reduction in detail for a special case. The d-c line of figure 11 is carrying a direct current $I = E/R$, and the line voltage in the steady state is zero. Therefore, the steady-state waves are

$$e_s = \frac{1}{2}\ zI(1 - 1_T) \text{ and } e_s' = -\frac{1}{2}\ zI(1 - 1_T)$$

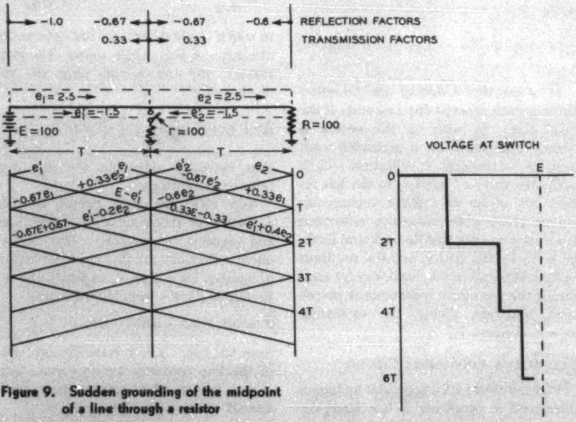
−1.0
−0.67
−0.67
−0.6
REFLECTION FACTORS
0.33
0.33
TRANSMISSION FACTORS
e_1 = 2.5
e_2 = 2.5
e_1' = −1.5
e_2' = −1.5
E=100
r=100
R=100
T
T
e_1'
e_1
e_2'
e_2
0
+0.33e_2'
−0.67e_2'
−0.67e_1
E − e_1'
−0.6e_2
+0.33e_1
T
e_1'−0.2e_2
0.33E−0.33
−0.67E+0.67
e_1'+0.4e_2
2T
3T
4T
VOLTAGE AT SWITCH
0
E
2T
4T
6T

Figure 9. Sudden grounding of the midpoint of a line through a resistor

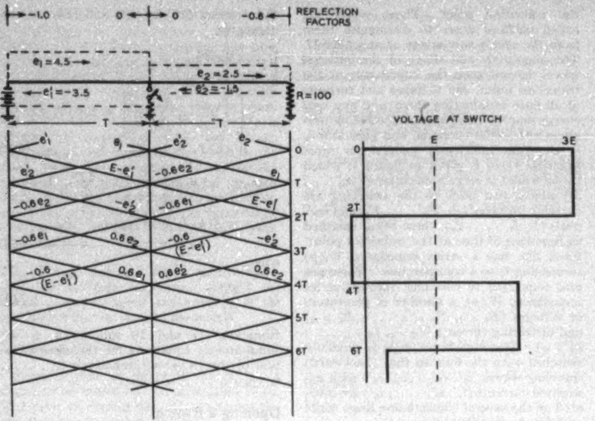

Figure 10. Removing a ground at the midpoint of a line

in which $\mathbf{1}$ is the unit function effective at $t = 0$ and $\mathbf{1}_T$ the unit function effective at $t = T$. Following the opening of the switch, causing an open circuit at the near end, the wave e_s' reflects completely, while at the inductance the reflection factor is

$$a = \frac{pL_0 - z}{pL_0 + z} = \frac{p - z/L_0}{p + z/L_0} = \frac{p - \alpha}{p + \alpha}$$

The double lattice gives the history of the successive reflections of e_s and e_s'. But in accordance with (13) appendix II, the reflection of e_s at the inductance is

$$a\frac{zI}{2}(\mathbf{1} - \mathbf{1}_T) - zI\epsilon^{-\alpha t}$$

in which the second term represents the discharge of the inductance, which initially (at instant switch is opened) is carrying a current I, the negative sign being occasioned by the fact that the wave corresponding to this discharge is leaving the terminal (a backward wave) and must, therefore, be of negative polarity to carry with it a positive current. But

$$\left(a\frac{zI}{2} - zI\,\epsilon^{-\alpha t}\right) = \left(\frac{p - \alpha}{p + \alpha} - 2\epsilon^{-\alpha t}\right)\frac{Iz}{2}$$
$$= -\frac{zI}{2}$$

so that the reflection of e_s is

$$-\frac{zI}{2}(\mathbf{1} + a\mathbf{1}_T)$$

and the first term, being of the same magnitude and polarity as e_s', but infinitely long, simply follows directly behind e_s' and thus prolongs it to infinity.

Consequently the reflection of e_s consists of a term $\left(-a\frac{zI}{2}\mathbf{1}_T\right)$ plus the conversion of e_s' into an infinite wave $\left(-\frac{zI}{2}\mathbf{1}\right)$.

But $\left(-\frac{zI}{2}\mathbf{1}_T\right)$ which arrives at the inductance at time T, leaves the open end at the same time $t = 0$ with the reflection of $\left(-a\frac{zI}{2}\mathbf{1}\right)$, so that the combination is a single wave $(-zI\mathbf{1})$ leaving the open end at $t = 0$. The single lattice of figure 11 gives its history. Of course, the wave $-zI\mathbf{1}$ could have been inferred at once as the cancellation wave, for a current $(-I\mathbf{1})$ suddenly impressed through the switch cancels thereafter the steady-state current I flowing in the switch, so that the switch is opened; and this impressed current through the surge impedance of the line carries a potential wave $(-zI\mathbf{1})$ with it.

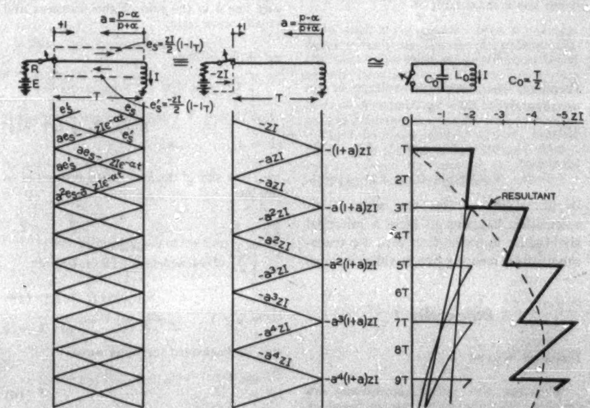

Figure 11. Reduction of "steady-state" block waves to one infinite wave and the equivalence of double and single lattices

The wave $(-zI\mathbf{1})$ arriving at the inductance at time T causes a train of reflections, and according to the single lattice of figure 11 the resultant voltage at the inductance is

$$e = -(1 + a)[\mathbf{1}_{(T)} + a\mathbf{1}_{(3T)} + a^2\mathbf{1}_{(5T)} + \ldots\ldots]zI$$

The $(n + 1)$th reflection yields

$$-(1 + a)\,a^n zI = \frac{-2p}{p + \alpha}\left(\frac{p - \alpha}{p + \alpha}\right)^n zI$$

$$= -2zI\epsilon^{-\alpha t}\sum_0^n{}_k \frac{\lfloor n}{\lfloor n - k} \times \sum_0^k{}_r \frac{(-\alpha t)^{n-r}}{\lfloor r\ \lfloor k - r\ \lfloor n - r}$$

$$= \begin{cases} -2zI\epsilon^{-\alpha t} & \text{for } n = 0 \\ -2zI\epsilon^{-\alpha t}[1 - 2\alpha t] & \text{for } n = 1 \\ -2zI\epsilon^{-\alpha t}[1 - 4\alpha t + 2\alpha^2t^2] & \text{for } n = 2 \\ -2zI\epsilon^{-\alpha t}[1 - 6\alpha t + 6\alpha^2t^2 - \frac{4}{3}\alpha^3t^3] & \text{for } n = 3 \end{cases}$$

These first few reflections and their resultant have been plotted on figure 11.

It is clear that the shorter the line the closer together will the reflections be, and, therefore, the higher the resultant voltage.

It is of interest to obtain an approximate solution, regarding the line as a concentrated capacitance C_0. Since the surge impedance of the line is $z = \sqrt{L/C}$ and the velocity of propagation is $v = l/\sqrt{LC}$ it follows that the total capacitance (line of length l) is

$$C_0 = lC = \frac{l}{vz} = \frac{T}{z}$$

The equation for the discharge of L_0 with initial current I into capacitance C_0 gives a voltage

$$e = -\frac{I}{\omega C_0} \sin \omega t = \frac{ZI}{\sqrt{\alpha T}} \sin \frac{\alpha t}{\sqrt{\alpha T}}$$

in which

$$\omega = \frac{1}{\sqrt{L_0 C_0}} = \sqrt{\frac{\alpha}{T}}$$

This approximate value of the reactor voltage has been plotted in figure 11 as a dotted curve, and is seen to be the final axis of the serrations caused by the actual reflections. For this particular example the constants were:

z = 50 ohms
= surge impedance of a cable
v = 500 feet/microsecond
= velocity of propagation
l = 16 miles
$T = 5{,}280 \frac{l}{v} = 168$ microseconds
L_0 = 0.167 henry
$C_0 = T/z = 3.36$ microfarads
$\alpha = z/L_0 = 300$
I = 200 amperes

The axis about which the reflections occur has a maximum of

$$\frac{zI}{\sqrt{\alpha t}} = zI\sqrt{\frac{v}{\alpha l}} = \frac{18zI}{\sqrt{\text{miles}}}$$

therefore, the maximum voltage is approximately:

Miles of cable	5	10	20	40	100
Maximum voltage	$8(zI)$	$6(zI)$	$4(zI)$	$3(zI)$	$1.8(zI)$

or higher the shorter the cable. This calculation happens to have a practical significance in connection with d-c transmission from mercury-arc-rectifier stations.

Appendix I

Initiated Waves

Whenever the circuit conditions are changed, traveling waves are initiated at the transition point. These waves are called *initiated waves* to distinguish them from the *steady-state* waves of appendix II. The magnitude and shape of the initiated waves depend upon the impedances at the transition point, the voltages and currents of all lines terminating there, and any new energy sources (charged capacitances, inductances with initial currents, and generators). A situation sufficiently general for most practical cases is given in figure 1, which shows n lines of surge impedances ($z_1, z_2, \ldots, z_n$) which, just prior to the switching are carrying voltages ($E_1, E_2, \ldots, E_n$) and currents ($I_1, I_2, \ldots, I_n$), these being specified as functions of time at the transition point. Each line has a series impedance $W_r(p)$ connecting it to a common bus. There are also connected to this bus, through series impedances $W_r(p)$, a number of generators of voltages ($E_{n+1}, E_{n+2}, \ldots, E_{n+m}$) and delivering currents ($I_{n+1}, I_{n+2}, \ldots, I_{n+m}$). The impedance $Z(p)$ is suddenly switched onto the bus, so that (backward) traveling waves ($e_1', e_2', \ldots, e_n'$) with associated currents ($i_1', i_2', \ldots, i_n'$) are initiated on the several transmission lines, while additional currents ($i_{n+1}, i_{n+2}, \ldots, i_{n+m}$) flow from the generators.

If $Z(p)$ contains any energy sources (generators, charged capacitors, or current-carrying inductances), then the total transient voltages and currents may be regarded as the superposition:

$$\begin{pmatrix}\text{Total}\\ \text{transient}\\ \text{terms}\end{pmatrix} = \begin{pmatrix}\text{Transient terms calcu-}\\ \text{lated with zero energy}\\ \text{sources in } Z(p)\end{pmatrix} + \begin{pmatrix}\text{Transient terms calculated as}\\ \text{the discharge of the energy}\\ \text{sources in } Z(p)\end{pmatrix} \quad (13)$$

The second term on the right is easily calculated for any specific case by setting up the differential equations of the circuit, and solving for the discharge of the energy sources of $Z(p)$. The procedure is illustrated by the examples in the text.

The terms due to switching in a "quiescent" $Z(p)$, that is with zero energy sources, are calculated from a set of simultaneous equations as follows. The total voltage on any line k is the sum of the transient and steady-state term,

$$e_k' + E_k = Z(p)\cdot\left[\sum_1^n (I_r + i_r') + \sum_{n+1}^{n+m} (I_r + i_r)\right] + W_k(p)\cdot(I_k + i_k') \quad (14)$$

$(k = 1, \ldots\ldots, n)$

while for any of the generators connected to the bus

$$E_k = Z(p)\cdot\left[\sum_1^n (I_r + i_r') + \sum_{n+1}^{n+m} (I_r + i_r)\right] + W_k(p)\cdot(I_k + i_k) \quad (15)$$

$(k = n+1, \ldots, n+m)$

But for backward traveling waves

$$i_k' = -\frac{e_k'}{z_k} \quad (16)$$

Substituting (16) in (14) and (15) and rearranging

$$\left[1 + \frac{W_k(p)}{z_k}\right] e_k' + Z(p) \times \left[\sum_1^n \frac{e_r'}{z_r} - \sum_{n+1}^{n+m} i_r\right] = Z(p)\cdot I_0 + W_k(p)\cdot I_k - E_k, \; (k = 1, \ldots\ldots, n) \quad (17)$$

$$W_k(p) i_k + Z(p)\left[\sum_1^n \frac{e_r'}{z_r} - \sum_{n+1}^{n+m} i_r\right] = Z(p)\cdot I_0 + W_k(p)\cdot I_k - E_k \quad (k = n+1, \ldots, n+m) \quad (18)$$

where

$$I_0 = \sum_1^{n+m} I_r \quad (19)$$

Equations 17 and 18 supply $(n + m)$ simultaneous equations for the determination of the $(n + m)$ unknowns ($e_1', \ldots, e_n', i_{n+1}, \ldots, i_{n+m}$).

Opening a Switch

If the switch *opens* or *disconnects* a part of the circuit, then $Z(p) = \infty$. Equations 17 and 18 then give

$$\sum_1^n \frac{e_r'}{z_r} - \sum_{n+1}^{n+m} I_r = I_0 \quad (20)$$

which by itself is insufficient for the determination of the unknowns. But

$$e_k' - W_k(p)\cdot i_k' = e_r' - W_r(p)\cdot i_r' = -W_r(p)\cdot i_r \quad (21)$$

Hence by (16)

$$e_r' = \frac{z_k + W_k(p)}{z_r + W_r(p)} \cdot \frac{z_r}{z_k} e_k' \quad (22)$$

$$-i_r = \frac{z_k + W_k(p)}{W_r(p)} \frac{1}{z_k} e_k' \quad (23)$$

Substituting (22) and (23) in (20)

$$\frac{z_k + W_k(p)}{z_k} e_k' \times \left[\sum_1^n \frac{1}{z_r + W_r(p)} + \sum_{n+1}^{n+m} \frac{1}{W_r(p)}\right] = I_0 \quad (24)$$

hence

$$e_k' = \frac{z_k I_0}{z_k + W_k(p)} \cdot \frac{1}{\sum_1^n \frac{1}{z_r + W_r(p)} + \sum_{n+1}^{n+m} \frac{1}{W_r(p)}} = \frac{z_k}{z_k + W_k(p)} Z_0(p)\cdot I_0 \quad (25)$$

and

$$i_r = \frac{Z_0(p) I_0}{W_r(p)} \quad (26)$$

in which

$$Z_0(p) = \frac{1}{\sum_1^n \frac{1}{z_r + W_r(p)} + \sum_{n+1}^{n+m} \frac{1}{W_r(p)}} \quad (27)$$

is recognized as the total impedance as viewed from the open switch. Therefore, $Z_0(p)\cdot I_0$ is the voltage on the bus due to a sudden application of I_0 through the switch *in the direction of* $Z_0(p)$. But this is exactly the equivalent of cancelling the steady-state current existing in the switch prior to opening. The term $z_k/(z_k + W_k)$ in (25) is merely that proportion of the bus voltage which appears as an outgoing wave on the line. Thus when a switch is *opened* the transient term may be calculated by suddenly cancelling the switch current with an equal and opposite current (as function of time) a fact evident from first principles. Therefore:

The effect of opening a switch may be simulated by suddenly impressing through the switch a current equal and opposite to the steady-state current through the switch.

Closing a Switch

Suppose that, in figure 1, the switch is about to be closed on the impedance $Z(p)$. Just prior to closing, the voltage across the switch is the voltage on the bus,

$$E_0 = E_k - W_k(p)\cdot I_k \tag{28}$$

and the total steady-state current flowing *to* the bus is (since the switch is open)

$$I_0 = \sum_1^{n+m} I_k = 0 \tag{29}$$

The transient voltage on the bus is

$$e_0 = e_k' - W_k(p)\cdot i_k' \tag{30}$$

and the total transient current flowing *to* the bus is

$$i_0 = \sum_1^n i_k' + \sum_{n+1}^{n+m} i_k \tag{31}$$

Substituting (28), (29), (30), and (31) in (14) and rearranging

$$e_0 = Z(p)\cdot i_0 - E_0 \tag{32}$$

Now the total impedance as viewed from the bus, excepting $Z(p)$ is

$$\frac{e_0}{-i_0} = Z_0(p) \tag{33}$$

and therefore (32) may be written

$$e_0 = -\frac{Z(p)}{Z_0(p)}e_0 - E_0 \tag{34}$$

or

$$e_0 = -\frac{Z_0(p)}{Z_0(p) + Z(p)}E_0 \tag{35}$$

But this is identically the voltage which would appear on the bus if the open switch voltage E were suddenly cancelled by impressing across it a voltage $-E$. Therefore:

The effect of closing a switch may be simulated by suddenly impressing across its terminals a voltage equal and opposite to the steady-state voltage across its terminals.

No Generators on Bus

If there are no generators on the bus, then equations 17, with $i_r = 0$, suffice for the determination of the voltage waves e_k'.

No Generators on Bus and Zero Series Impedances

If there are no generators on the bus then $i_r = 0$, and if the series impedances are zero $W_k(p) = 0$, then equations 17 give (since $e_1' = e_2' = \ldots\ldots = e_n' = e'$ and $E_1 = E_2 = \ldots\ldots E_n = E_0$)

$$e' = \frac{Z(p)\cdot I_0 - E_0}{1 + Z(p)\sum_1^n \frac{1}{z_r}}$$

$$= \frac{z}{z + Z(p)}[Z(p)\cdot I_0 - E_0] \tag{36}$$

in which

$$z = \frac{1}{\sum_1^n \frac{1}{z_r}} = \text{impedance of all lines in parallel} \tag{37}$$

Appendix II

Steady-State Waves

It is well known that the solutions for voltage and current on a long transmission line operating in the steady state may be expressed in terms of traveling waves. Thus the vector solution for the voltage at any point x (measured *negative* from the receiver) in terms of the receiver voltage E_0 and current I_0 is

$$\mathbf{E} = \mathbf{E}_0 \cosh \sqrt{zy}x - I_0\sqrt{\frac{z}{y}} \sinh \sqrt{zy}x \tag{38}$$

where

$z = r + j\omega L$
= line impedance per unit length

$y = g + j\omega C$
= line admittance per unit length

If the losses are neglected, $r = g = 0$, and

$$\sqrt{zy} = j\omega\sqrt{LC} = j\frac{\omega}{v}$$

$$\sqrt{\frac{z}{y}} = \sqrt{\frac{L}{C}} = z$$

= surge impedance of the line

v = velocity of propagation

then

$$\mathbf{E} = \mathbf{E}_0 \cos\frac{\omega x}{v} - jz\mathbf{I}\sin\frac{\omega x}{v}$$

$$= E_0 e^{j\omega t}\cos\frac{\omega x}{v} - zI_0 e^{j(\omega t - \theta + \pi/2)}\sin\frac{\omega x}{v} \tag{39}$$

The instantaneous value of $\mathbf{E}$ is the *real* of the above, or

$$E(x,t) = E_0 \cos\omega t \cos\frac{\omega x}{v} + zI_0\sin(\omega t - \theta)\sin\frac{\omega x}{v}$$

$$= \frac{1}{2}\left[E_0\cos\frac{\omega}{v}(x - vt) + zI_0\cos\frac{\omega}{v}\left(x - vt + \frac{v\theta}{\omega}\right)\right] + \frac{1}{2}\left[E_0\cos\frac{\omega}{v}(x + vt) - zI_0\cos\frac{\omega}{v}\left(x + vt - \frac{v\theta}{\omega}\right)\right] \tag{40}$$

This suggests the possibility of employing a pair of waves to represent the steady-state conditions on any line, whether d-c, a-c, or any other operating condition. These waves, once known, are then the *incident waves* for any change of circuit conditions, and the reflections may be calculated in routine fashion.

Let the voltage and current in the steady state at the transition point prior to switching be given as functions of time by $E(t)$ and $I(t)$, respectively. Let these conditions be specified in terms of a pair of forward and backward waves e_s and e_s' (the subscript s implying "steady-state" waves):

$$e_s + e_s' = E(t) \tag{41}$$

$$i_s + i_s' = I(t) \tag{42}$$

But if z is the surge impedance of the line

$$i_s = e_s/z \text{ and } i_s' = -e_s'/z \tag{43}$$

so that (43) in (42) give

$$e_s - e_s' = z\cdot I(t) \tag{44}$$

From (41) and (44)

$$e_s = \frac{1}{2}[E(t) + z\cdot I(t)] \tag{45}$$

$$e_s' = \frac{1}{2}[E(t) - z\cdot I(t)] \tag{46}$$

Thus each wave consists of a component equal to half the steady-state voltage distribution on the line, and a component equal to half the steady-state current distribution on the line; the first component reflects as from an open terminal while the second component reflects as from a grounded terminal.

The functions e_s and e_s' at some point x_0 on the line are given by (45) and (46) as *time* functions. If, however, they are to be regarded as true waves, then

$$e_s\left(t - \frac{x - x_0}{v}\right) = \frac{1}{2}\left[E\left(t - \frac{x - x_0}{v}\right) + z\cdot I\left(t - \frac{x - x_0}{v}\right)\right] \tag{47}$$

$$e_s'\left(t + \frac{x - x_0}{v}\right) = \frac{1}{2}\left[E\left(t + \frac{x - x_0}{v}\right) + z\cdot I\left(t + \frac{x - x_0}{v}\right)\right] \tag{48}$$

For example, taking $x_0 = 0$ and $E(t) = E_0 \cos\omega t$, $I(t) = I_0\cos(\omega t - \theta)$, equations

(47) and (48) give the two terms of (40).

While (47) and (48) constitute the specification of the steady-state conditions on the line in terms of traveling waves, it is necessary to show that they are also compatible with the terminal impedances at the ends of the line. This is proved by showing that e_s' is the natural reflection at one end of the line, and that e_s is the reflection at the other end. For example, suppose the line terminates in an impedance Z_1 in series with a generator of voltage E_1. The wave e_s' is composed of the reflection of e_s from Z_1 and the discharge of E_1 through Z_1 to the line. These two terms then are:

$$\begin{aligned} e_s' &= \left(\frac{Z_1 - z}{Z_1 + z}\right) e_s + \left(\frac{z}{Z_1 + z}\right) E_1 \\ &= \left(\frac{Z_1 - z}{Z_1 + z}\right)\frac{1}{2}(E + z \cdot I) + \frac{z}{Z_1 + z} \times \\ &\qquad\qquad (E - Z_1 I) \\ &= \frac{1}{2}(E - z \cdot I) \end{aligned} \tag{49}$$

In the same way e_s may be shown to be the reflection at the other end of the line.

If, now, at any point on the line the circuit conditions are changed, as by a switching operation, the "steady-state" wave *receding* from that point moves away without consequence, but the "steady-state" wave *approaching* the point impinges on the new terminal impedance and gives rise to reflections and refractions in accordance with conventional traveling wave theory. It must be remembered, however, that when the tail of the *receding* wave reaches the terminal impedance at the other end of the line, which it is approaching, it also will cause a transient reflection there. In addition to the reflection of the approaching wave there is also a wave representing the discharge into the line of the energy sources (generators, charged capacitances, and current carrying inductances) of the terminal impedance; so that the total reflected wave from the impedance $Z(p)$ is

$$\bar{e}' = \left[\frac{Z(p) - z}{Z(p) + z}\right] e_s + \left[\begin{array}{l}\text{Discharge of the} \\ \text{energy sources} \\ \text{in } Z(p)\end{array}\right] \tag{50}$$

If the transition point is at some intermediate point on the line, then there is a "steady-state" wave approaching the point from each direction, and the principle of superposition applies as usual.

It remains to show that the two "steady-state" waves of finite length (the length of the line) are equivalent to the single semi-infinite "initiated wave" of appendix I. The total voltage at the transition point, by (50), is

$$\begin{aligned} e &= \bar{e}' + e_s \\ &= \left[\frac{2Z(p)}{Z(p) + z}\right] e_s + \left[\begin{array}{l}\text{Energy} \\ \text{discharge} \\ \text{term}\end{array}\right] \end{aligned} \tag{51}$$

Substituting for e_s from (45) and subtracting the steady-state line voltage E gives the "initiated wave"

$$\begin{aligned} e' &= e - E = \left[\frac{2Z(p)}{Z(p) + z}\right]\frac{1}{2}(E + z \cdot I) + \\ &\qquad [\text{Energy discharge term}] - E \\ e' &= \frac{z}{Z(p) + z}[Z(p) \cdot I - E] + \\ &\qquad [\text{Energy discharge term}] \end{aligned} \tag{52}$$

defined in equation (36) of appendix I. Now e_s and e_s' being finite waves of length T may be represented as

$$e_s(1 - 1_T) \text{ at one end and } e_s'(1 - 1_T) \text{ at other end of line} \tag{53}$$

that is, each as the superposition of two infinite waves of opposite sign, displaced by T. But since $e_s 1$ is the perpetual reflection of $e_s' 1$ at the other end of the line, and since by (41)

$$e_s 1 + e_s' 1 = E(t) \tag{54}$$

it follows that the transient must be due entirely to a wave, of which $(-e_s' \, 1_T)$ is a component, initiated at the transition point at the instant of switching, and of which (52) is the specification. Thus the two steady-state waves of finite length are replaceable by a single infinite wave, and this infinite wave is the "initiated wave" of appendix I or the "cancellation wave" described in the text.

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Discussion

T. J. Carpenter (General Electric Company, Pittsfield, Mass.): In the application of protector tubes, one of the important characteristics which must be known is the transient recovery voltage characteristic when the tube clears, as both the tube's dielectric strength and the recovery voltage are a function of time. If the recovery voltage exceeds the tube's dielectric strength, current will restart and the line will not be cleared by the tube.

From the field tests made on protector tubes, some data have been secured which show that the transient voltage due to opening a simple circuit may be quite accurately calculated even though a number of simplifying assumptions are made. Figure 1 shows the oscillogram; figure 2, the circuit, and the transcribed and calculated voltage of one test. This test was made on a 115-kv system. The phase wires of this double-circuit 30-mile line, with a single ground wire were tied together at the far end from the station making the total length of the line 60 miles. A 40,000-kva generator, 25,000-kva transformer, and the 60-mile length of line were short-circuited to ground by a fused expulsion protector tube located on the station side of the line. The protector tube cleared the circuit after a half cycle of current, and the transient voltage was measured by a cathode-ray oscillograph.

Figure 1. Oscillogram of recovery voltage

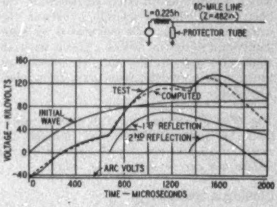

Figure 2. Comparison of computed and transcribed reflections

The equation was set up on the following assumptions: (1) the impressed voltage is considered as a constant direct voltage which is justifiable as the computation is carried out only for a fraction of a half cycle; (2) the current is treated as a sine wave; (3) the arc voltage is taken as a sine wave (used only in the determination of the current); (4) the arc voltage at current zero defines the start of the recovery voltage characteristic; (5) the station capacitance is neglected; (6) the coupling between the two circuits is neglected; and (7) the attenuation is neglected.

The derived equation for these conditions is:

$$\begin{aligned} e = -44 + 92.8[(1 - \epsilon^{-\beta t})1_{0T} + 2\beta t\epsilon^{-\beta t}1_{2T} \\ + 2\beta t\epsilon^{-\beta t}(1 - \beta t)1_{4T} + \ldots] \end{aligned}$$

e is the voltage in kilovolts

β is $\dfrac{Z}{L}$

Z is the surge impedance calculated from the physical constants of the line

L is the equivalent inductance which determines the fault current

1_{nT} is the unit function effective at $t = nT$ where T is the time for a wave to travel the length of the line

In plotting the equation the propagation velocity was taken to check with the oscillogram and works out to be 95 per cent of the velocity of light which checks closely with other investigations.[1]

The discrepancies between the calculated and measured recovery voltages are small, and can be accounted for by errors in the assumptions. For example, the current was distorted by the arc resistance, and the station capacitance and coupling between the looped lines and phases were neglected.

However, an exact solution does not seem to be justified, as the approximate solution gives a very good check. These simplifying assumptions make the solution much easier, and it is suggested that they be kept

Flashover Characteristics of Transformer Condenser Bushings

By H. L. COLE
MEMBER AIEE

TERMINAL bushings for high-voltage power transformers require many electrical tests to demonstrate their fitness for service. A vital part of the complete transformer, they operate under extreme variations of temperature and weather conditions. They are subject to all the variations in load, ambient temperature, and surges that the transformer windings receive, and in addition must be built to withstand severe attacks of ice and snow, rain and hail, hot sun, smoke, fog, dust, and dirt. They must be stronger against flashover than the protective apparatus, and must maintain their strength year in and year out. The terminal bushing is a part of the transformer and not an individual piece of apparatus; under normal service conditions it should be proportioned in strength to the winding, and should be co-ordinated with the winding under impulse voltage conditions.

The purpose of this paper is to discuss the electrical performance of bushings, particularly flashover characteristics, how they are obtained, and the relation of factory and development tests to requirements of service.

To determine the performance of transformer bushings, the following electrical tests are made:

1. Sixty-cycle, or low-frequency tests:
    (*a*) Dry flashover.
    (*b*) Wet flashover.
    (*c*) One-minute hold; dry.
    (*d*) Ten-second hold; wet.
    (*e*) Power factor and capacity.
    (*f*) Temperature rise under loading.
    (*g*) Endurance tests (puncture strength under continuous high voltage at elevated temperatures).
    (*h*) Radio interference tests.
2. Impulse tests:
    (*i*) Full-wave strength, or minimum (50-50) flashover, with $1^{1}/_{2}$ x 40 wave.
    (*j*) Voltage flashover—time lag curve ($1^{1}/_{2}$ x 40 wave).
    (*k*) Flashover on steep wave front.
    (*l*) Puncture strength, bushing immersed in oil.

The above list of electrical tests is by no means complete, but it gives the principal ones made in the development of a line of high-voltage terminal bushings. The one-minute hold test (*c*), and the power factor test (*e*), are made on all condenser bushings and are considered commercial or production tests. The remaining tests are made during development or on a representative number of the bushings only.

(*a*) SIXTY-CYCLE DRY FLASHOVER

One of the earliest purposes of the 60-cycle dry flashover test was to demonstrate that the bushing, under comparatively short-time application of voltage, would flashover at the air end before it would puncture. It was also an indication of strength against switching surges. Before impulse tests were made, the 60-cycle dry flashover was a rough measure of the impulse strength of the air end of the bushing.

At the present time, the value of the 60-cycle dry flashover test is of minor importance in demonstrating the serviceability of the bushing. The wet flashover is the limiting feature with respect to switching surges, the one-minute test is a stronger proof of puncture strength, and the impulse flashover is a direct measure of lightning strength.

Like most of the tests, the 60-cycle dry flashover test must be made separate from the transformer. During the test the oil level at the bottom end of the bushing must be the same as in the transformer itself. The tank of oil on which the bushing is mounted for test must be sufficiently large to prevent corona disturbances at the bottom end of the bushing. The cover should have sufficient area to produce the same effect on the electric field above the tank as on the transformer.

Sixty-cycle dry flashovers of bushings, like tests on sphere gaps and rod gaps, will vary with the air density and must be corrected to a relative air density of 1.0 to obtain a comparison with the performance data recommended by the transformer subcommittee of the AIEE.[1] For all ordinary tests, where the relative air density is within ± ten per cent of unity,

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Paper number 38-72, recommended by the AIEE committee on electrical machinery, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted April 6, 1938; made available for preprinting May 24, 1938.

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1. For all numbered references, see list at end of paper.

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in mind when setting up equations for this type of problem.

REFERENCE

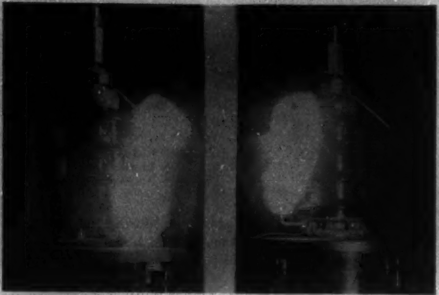
1. THEORY AND TESTS OF THE COUNTERPOISE, L. V. Bewley. AIEE Lightning Reference Book, page 1392.

R. D. Evans and **A. C. Monteith** (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): We quite agree with Mr. Bewley as to the growing interest and curiosity in the subject of switching transients, but we believe that this interest grows out of the recognition of the increasing importance of switching surges. An example of one type of switching transient is the recovery-voltage transient, which is important in connection with the application of interrupting devices, such as circuit breakers, fuses, and protector tubes. This phase of the problem has been of particular interest to us and results from our investigations have been given in two papers, the first presented at the last summer convention and the second at the last winter convention.

In the introduction of Mr. Bewley's paper he discusses only theoretical methods applicable to the study of transients due to circuit changes. We believe that any review of methods for studying transients of this character should include mention of the method utilizing the a-c network calculator. By this method any theoretical circuit or practical system can be simulated in miniature and subjected to circuit changes corresponding to those of the system under consideration. Such an arrangement permits taking into account the effects of initiating and subsidence transients, losses, arc characteristics, etc., and the interaction of these factors. In general, we have found that it is impracticable by the use of analytical methods alone to consider all of these factors. Furthermore, as demonstrated by our recent paper presented at the winter convention, the a-c calculating board method is admirably adapted for the general study of switching transients.

We quite agree that of the three general methods discussed by Mr. Bewley the "cancellation method" is the most useful. In the practical application of this method to three-phase systems, it will be found that the resolution into symmetrical components, as outlined in our earlier paper, will constitute a simplification over "single phase" methods.

It may be inferred from the title and the treatment of the subject that transients arising from switching operations require a different treatment from that which has been developed for the study of lightning surges. It is believed that these problems are essentially the same, as both can be considered as transients arising from changes in circuits. Hence, the work on lightning surges also applies to switching surges.



The flashovers shown here are on a steep wave front that reached nearly a million volts and 90,000 amperes. In these tests the bushing was equipped with a top electrode for control of positive wave flashovers, and a flange electrode for the control of negative wave flashovers

Figure 1. Positive (left) and negative (right) impulse flashover of condenser bushing

it is sufficiently accurate to assume that the flashover is directly proportional to the relative air density. This correction is applied to bushings of all sizes.

Sixty-cycle dry flashovers of a bushing will vary with humidity, and will increase with an increase in humidity, other factors remaining constant. The standard correction proposed by the transformer subcommittee of the AIEE[2] is that the measured flashover voltage shall be increased by three per cent for each grain that the absolute humidity falls below standard (6.5 grains per cubic foot) and decreased by three per cent for each grain above standard, to obtain the dry 60-cycle flashover for standard humidity. This correction applies only to bushings for 23-kv service and higher, and also only when they are gapped. For smaller bushings, the humidity correction is negligible. For ungapped bushings no humidity correction has been specified.

The standard for humidity, referred to in this paper as 6.5 grains per cubic foot is the same as the vapor pressure standard of 0.6085 inches of mercury now being proposed by the transformer subcommittee of the AIEE.

(*b*) Sixty-Cycle Wet Flashover

From the standpoint of ability to withstand switching surges, the 60-cycle wet flashover value of a bushing is of more importance than the dry characteristic. The 60-cycle wet flashover is necessarily lower than the dry; the closeness to which the two can be brought together is to some extent a measure of the efficiency of the design of the bushing and its weather casing.

The following conditions have been proposed by the transformer subcommittee of the AIEE[2] for standard wet flashover tests: 12,000 ohms per centimeter cube resistance of water, with a 0.1 inch per minute precipitation at a 45-degree angle to the bushing. The correction factors for various resistivities are:

Water Resistance Ohms Per Centimeter Cube	Correction Factor
3,000	1.28
4,000	1.19
5,000	1.13
6,000	1.10
8,000	1.04
10,000	1.02
12,000	1.00
20,000	0.99

The measured flashover voltage times the correction factor gives the flashover at 12,000 ohms resistivity. This correction factor is useful because, while it is comparatively easy to regulate the amount of precipitation during a test, the control of the water resistance is difficult and would require a complicated tank supply to maintain a definite resistivity.

The *temperature* of the water affects the resistivity, and also affects the relative air density and absolute humidity. These apparently have little effect on flashover. This feature, however, has not been studied by the writer outside of the range of temperatures (10 degrees centigrade to 25 degrees centigrade) obtained in an ordinary city water supply.

(*c*) Sixty-Cycle One-Minute Hold Test, Dry

In contrast to the flashover tests (which are mainly a check on the design of the bushing), the dry one-minute hold test is a commercial test given all bushings. The object is to check the manufacture of the bushing, the materials used, and the assembly. This test is often employed in the field to determine the suitability of a bushing for service. While the rejection of a bushing because of failure to meet this test is very rare, nevertheless it has been considered a good policy to subject transformer terminals to a higher one-minute test than they receive during the test on the windings. The one-minute hold test is made at a voltage of 75 to 90 per cent of the 60-cycle dry flashover voltage.

(*d*) Sixty-Cycle Ten-Second Hold Test, Wet

This test, made at a voltage of 75 to 90 per cent of the wet flashover voltage, is a special test which serves principally to demonstrate further the efficiency of the design of bushing and weather casing. It is doubtful whether such a test contributes much to the standard of bushing performance, and the test is made only in special cases.

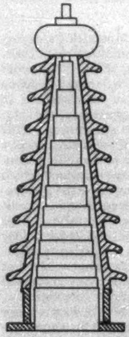
The *following 60-cycle* tests relate to electrical performance of bushings, even though they do not come within the subject of flashover. Reference to them is made in this paper because they are fully as important as the flashover characteristics in determining the suitability of a bushing for service.

(*e*) Power-Factor and Capacity Tests

The power-factor test is a good method to determine the quality of the insulation in the bushing. This test, together with the capacity measurement, is made on all condenser bushings as a check on materials and manufacture.

(*f*) Temperature Rise Under Loading

This is a development test, to determine the safe loading for each size of bushing with a given size of cable or solid



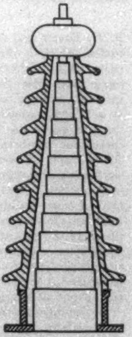


Figure 2 (left). Condenser with uneven steps on upper end

Figure 3 (right). Condenser with even steps on upper end

lead. In service, the lower end of the bushing is in the hottest transformer oil, and the upper end is subjected to higher than ordinary ambient temperatures. It is important to keep the temperature rise of the central part of the bushing low.

(*g*) ENDURANCE TESTS (PUNCTURE STRENGTH UNDER CONTINUOUS HIGH VOLTAGE)

An important development test is that which demonstrates the strength against puncture, over long applications of voltage at high temperature. It is well known that practically all insulating materials have higher power factors at high temperatures. The total loss in a bushing must be sufficiently low so that a stable temperature condition is maintained. If the power factor is too high, the dielectric losses will increase the temperature, which in turn will increase the power factor, increasing the losses, etc., until breakdown will occur. Such a critical condition will not be caught in a one-minute test, but may take several hours to develop.

A method of making an endurance test has been to mount the bushing with the lower end in 100-degree-centigrade oil, and the upper end in 100-degree-centigrade air, and to apply a continuous 60-cycle voltage, raising the voltage ten per cent every eight hours until failure occurs. This is a severe test, but one which is without doubt justified for transformer bushings operating in high temperature localities.

(*h*) RADIO INTERFERENCE TESTS

The standardization of radio interference tests on high-voltage bushings is now under consideration. Tests have been made on old and new bushings, by the method proposed by the joint Edison Electric Institute, National Electrical Manufacturers Association, and Radio Manufacturers Association committees on radio interference.[3]

Source of data: Reference 4

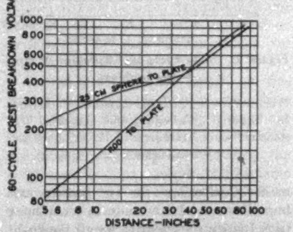


Figure 4. Sixty-cycle flashover voltages for 25-centimeter sphere-to-plate and rod-to-plate gaps

Figure 5. A line of transformer condenser bushings, for standard voltages from 69 kv to 161 kv (inclusive)

(*i*) IMPULSE TESTS; FULL WAVE OR MINIMUM FLASHOVER

Since impulse testing of transformers has become general, it has been proposed to standardize the full wave or minimum flashover voltage of transformer bushings.[1] A positive $1^1/_2 \times 40$ wave has been recommended for this standardization. Until recently, the flashover strength was designated by the "equivalent gap" length of the bushing, meaning the length of standard rod gap in inches which, when connected in parallel with the bushing, would produce 50-50 flashovers between bushing and gap. At the present time the full wave strengths of bushings are measured by voltage values, by sphere gap, or cathode-ray oscillograph. From the standpoint of co-ordination of transformer winding and bushing, the bushing must not have an excessively high impulse flashover. This is also true with respect to negative full-wave values, particularly when a rod gap is used as a protective device. This point is discussed further under actual tests reported for condenser bushings.

The full-wave strength, or minimum impulse flashover of a bushing will vary with the air density and humidity.[2] The correction for air density is the same as for 60-cycle dry flashover and is applied to all bushings—the minimum impulse flashover being directly proportional to the relative air density within the limits under which ordinary tests are made. For variations in humidity, the minimum impulse flashover is $2^1/_2$ per cent lower for each grain below the standard humidity condition of 6.5 grains per cubic foot, and $2^1/_2$ per cent higher for each grain above 6.5. This correction is applied only to gapped bushings, for 23-kv service and above.

A considerable number of impulse flashover tests have been made on bushings under wet or rain conditions, with 0.1 inch per minute water precipitation. When the correction for relative air density and 100 per cent humidity is taken into consideration, the effect of the water reduces the impulse flashover not more than two or three per cent, and is considered negligible. For this reason wet impulse flashover tests are not required to demonstrate the performance of standard bushings.

(*j*) IMPULSE VOLTAGE—TIME LAG CURVES

The impulse flashover voltage of bushings at short time lags is important in determining the amount of protection against winding failure which the bushings will offer, and the factor of safety of the bushing itself against puncture.

(*k*) FLASHOVER ON STEEP WAVE FRONT

With steep wave front surges, on the order of 1,000 to 2,000 kv rise per microsecond, it is important to know the flashover voltage of bushings for three reasons: (1) To determine what protection is afforded the bushing by the protective device—lightning arrester, rod gap, or line insulation; (2) to determine what protection the bushing offers the transformer winding, and (3) to determine the factor of safety of the bushing against puncture. Figure 1 shows photographs of steep wave front flashover, with heavy surge currents.

(*l*) IMPULSE PUNCTURE STRENGTH

The impulse puncture strength of the bushing must be known in order to have complete data for the design and application of the bushing. These tests are made with the entire bushing immersed in oil.

Condenser Bushings and Rod Gaps

Bushing flashover characteristics are closely associated with the dielectric strength of air between various types of electrodes. Since the development of impulse testing measurement of surges by means of the cathode-ray oscillograph, much data have been taken to determine the effect of condenser design, shape of bushing cap, height of flange above the cover, location and design of bushing gaps, etc., on the impulse and 60-cycle flashover of condenser bushings.

It has been known for years that the shape of the electrodes has a decided effect on the impulse flashover strength of air, and that when the positive polarity is applied to the electrode having the higher field intensity, the flashover voltage is lower. A practical study[4] of electrode shapes over a wide range of voltages, made a few years ago, has confirmed previous observations and brought out many important facts, useful in bushing design.

Some of the facts brought out or confirmed in the Bellaschi-Teague paper are:

1. The impulse flashover voltage from point to plane is lower when the point is positive and higher when the point is negative.

2. The 60-cycle flashover occurs with the positive polarity of the wave on the point.

3. A sharp projection extending out from the plane on a rod-to-plane electrode test, will reduce the impulse flashover for tests with negative polarity applied to the rod.

4. A sphere-to-plane gap has higher flashovers (60-cycle and positive waves) than a rod-to-plane gap, up to openings $3\frac{1}{2}$ times the sphere diameter. For larger openings the flashovers are less. See figure 4.

An examination of considerable data taken on condenser bushings previous to the tests described above disclosed that fundamental data on rod and rod-to-plane gaps would be desirable. Previous condenser-bushing data showed that there were several ways in which 60-cycle and impulse flashover voltages could be controlled. The tests[4] confirmed the conclusions drawn and enabled future design work to be made on a firmer basis. These conclusions are:

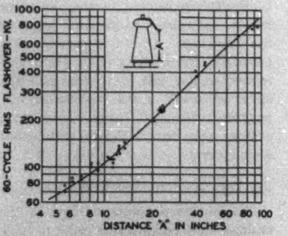


Figure 6. Sixty-cycle dry flashover of condenser bushings

Voltages are corrected for standard air conditions. The full line is for standard rod gaps

5. A pronounced change in both 60-cycle and impulse voltage flashover can be made by changing the length of the condenser steps on the air end of the bushing. A bushing with even steps (see figure 3) on the upper end of the condenser has a lower 60-cycle and positive wave flashover than one in which the steps are long at the top and short at the bottom (as in figure 2). For example, a 115-kv bushing with long steps at the top of the condenser, had a positive-wave flashover of 15 to 25 per cent higher, and a 60-cycle flashover 15 per cent higher than one with equal steps for the entire length of the air end. The positive full-wave flashover was higher than the negative, due to the reduction of field intensity at the top of the bushing.

6. Using the principle that a positive-wave flashover is initiated by, and is lower when a rod or point electrode is provided at the positive terminal, the impulse flashover for both positive and negative waves can be controlled independently, when desired, within practical limits. One way of accomplishing this is by means of positive and negative electrodes, mounted at 180 degrees. With this electrode or gap design, a positive wave applied to the bushing lead will flashover consistently from the cap electrode to the metal flange of the bushing. A negative wave on the lead (positive at the flange) will flash from the point electrode at the flange to the side of the bushing cap. Repeated tests will produce the same phenomena—positive wave from cap electrode to flange, negative from flange electrode to cap—even though the two distances are unequal in length as much as 10 or 15 per cent.

7. It is pointed out in the Bellaschi-Teague paper[4] that the 60-cycle flashover of sphere-to-plane electrodes is higher than a rod-to-plane, up to $3\frac{1}{2}$ times the sphere diameter.

The electric field surrounding a condenser bushing on a transformer cover is in some respects similar to sphere-to-plane or rod-to-plane electrodes.

Bushing caps vary from 4 to 16 inches in diameter, and the distances from cap to flange vary from one to six times the cap diameter. At 138-kv the opening between cap and flange is about $3\frac{1}{2}$ times the effective diameter of the cap. For higher-voltage-class condenser bushings, the 60-cycle flashover may be increased slightly by the addition of a point electrode to the cap.

Sixty-Cycle Tests on Condenser Bushings

A large number of 60-cycle tests on transformer condenser bushings have been made during the past four years. Some of them have been made without bushing gaps, some with special shaped electrodes, some with top and bottom electrodes at 180 degrees with each other, and others with bottom electrodes only. The data in Figures 6 and 7 give all the flashovers measured on bushings with top electrodes, or both top and bottom electrodes in place. The voltages are each an average of five flashovers, and are plotted against the vertical distance in inches from top electrode to flange.

The dry flashover voltages are corrected to relative air density = 1 and for 6.5 grains per cubic foot (0.6085 inch mercury) absolute humidity. The wet flashover voltages are corrected for water resistance in line with the Institute standard correction given above. Actual water resistances were from 3,500 to 7,500 ohms per centimeter cube during the tests.

A number of tests have been made with 0.2 inch per minute precipitation, instead of 0.1 inch per minute. Of 12 bushings tested, up to and including the 69-kv, class (approximately 24 inches length) the wet flashover for 0.2 inch per minute precipitation averages 84 per cent of that for 0.1 inch per minute; the minimum was 77 per cent, the maximum 90 per cent.

Impulse Tests on Condenser Bushings

Impulse flashover data on a large number of condenser bushings are given in figures 8 and 9. $1\frac{1}{2}$ x 40 waves were used. Corrections for air density and humidity have been made, in accordance with Institute standards, for all the bushings tested. In the case of positive waves, the flashovers are plotted against distances from top electrode to flange.

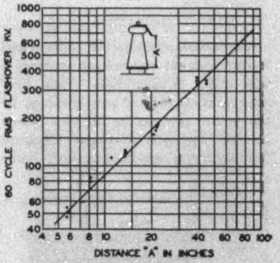


Figure 7. Sixty-cycle wet flashover of condenser bushings

Precipitation 0.1 inch per minute at 45 degrees to bushing. Voltages are corrected for standard water resistance of 12,000 ohms per centimeter cube

For negative waves the distances are from flange electrode to the cap.

The straight line curves in figures 8 and 9, representing two-microsecond and full-wave values are from time lag curves for standard rod gaps. These curves have been published previously, and are in the Bellaschi-Teague paper,[4] figures 3 and 4. The rod gap curves are drawn on the bushing flashover points to indicate that co-ordination of condenser bushings with rod gaps, for both positive and negative waves and down to at least two microseconds time of flashover is accomplished. The data for the bushings, as well as the rod gaps, have been taken directly from time lag curves. The time lags are measured from the 60-cycle crest voltage flashover value to the time of flashover. The variation of tests from the curves is reasonably small, considering the nature of the tests and the fact that they have been made at different times over a period of four years. The impulse flashover data are in reality more consistent than the 60-cycle data.

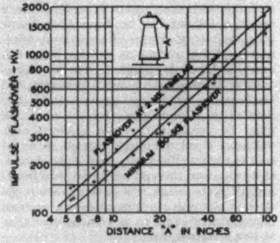


Figure 8. Positive wave impulse flashover of condenser bushings

$1^1/_2 \times 40$ waves were used. Voltages are corrected for standard air conditions. The full lines are for standard rod gaps

USE OF ELECTRODES OR GAPS ON BUSHINGS

There has been some discussion in the Institute proceedings as to the need or desirability of gaps or electrodes on transformer bushings.

Several years ago the design of condenser bushings to meet the co-ordination requirements then prevailing, without the use of gaps, was considered. Even with the flexibility of design provided by the condenser bushing, where considerable range of control of the electric field is possible by location of condenser steps, it has been found desirable to use an electrode projecting out from the flange to obtain the desired negative-wave flashovers. The function of the top or cap electrode is of less importance than that of the flange electrode, as the positive-wave flashover from the plain cap of the bushing to the flange electrode is usually lower than the negative-wave flashover from flange electrode to cap.

A reference to figure 4, which is drawn from data in the Bellaschi-Teague paper[4] shows that sphere-to-plane electrodes have higher 60-cycle flashovers than rod-to-plane, for distances up to $3^1/_2$ times the sphere diameter. For longer distances the flashover is less. The same is true for positive and negative impulse flashovers. The standard requirements for bushing flashover follow closely the rod-to-rod and rod-to-plane. A bushing which has: (*a*) a low flange (bottom of porcelain close to the cover), (*b*) no electrode on the flange, and (*c*) a cap with no sharp edges or gap, will have flashover characteristics which differ from a rod-to-rod curve in a manner similar to the sphere-to-plane curve in figure 4. The extent to which it will depart from the straight line rod-to-rod curve will depend upon how closely (*a*), (*b*), and (*c*) above are met. Whether the flashover will be above, or below the straight line curve will depend upon the relative diameter (effective) of cap and height of porcelain.

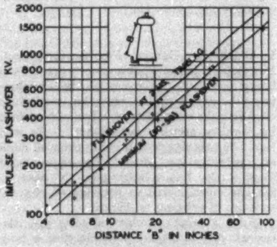


Figure 9. Negative wave impulse flashover of condenser bushings

$1^1/_2 \times 40$ waves were used. Voltages are corrected for standard air conditions. The full lines are for standard rod gaps

From the standpoint of uniformity of design, co-ordination of bushing with protective apparatus, and possible protection to the winding offered by the bushing, the use of a gap on the flange of bushing seems desirable. The writer's experience has been that bushings with gaps have less erratic flashover values (see figure 10, showing negative impulse-time lag curves for bushing with and without gap at flange). A gap will often prevent a flashover to other metal parts of the transformer, such as caps of adjacent bushings or grounded metal projecting above the transformer cover. This is especially true of the gap at the flange of the bushing. A gap is a ready means for fixing the flashover values and adjusting the flashover for special applications. The humidity correction factor is more definitely known for gapped bushings.

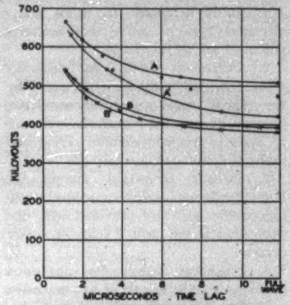


Figure 10. Impulse flashover-time lag curve of bushing, showing spread of points

A-A'—Without gap at flange
B-B'—With gap at flange
Tests made with negative $1^1/_2 \times 40$ wave

Co-ordination of bushing and winding becomes practical when a gap is used. For example, a 138-kv transformer winding has a safe impulse test strength (based on an impulse ratio of 2.2) of 860 kv. If we select a bushing with an equivalent gap of 44 inches (the previous recommended standard), it will have a 60-cycle dry flashover (from figure 6) of approximately 425 kv, a 60-cycle wet flashover (figure 7) of approximately 350 kv. The impulse flashovers would be (from figures 8 and 9) positive minimum flashover 700 kv, negative minimum flashover 800 kv. These data show that without allowance for factor of safety in the winding insulation the bushing is co-ordinated with the winding for full waves and for waves chopped by the bushing at fairly long time lags (three to six microseconds). In other words, the bushing is substantially another co-ordinating gap, of similar characteristics but with slightly higher flashover values than the former standard gap.

The writer knows of only two cases of bushing flashovers on modern power transformers during the past three years. In neither case were the windings damaged. The bushings were gapped. Without gaps, these bushings would have had from 5 to 10 per cent higher positive-wave flashover, and 25 to 50 per cent higher negative wave flashover.

In conclusion, it is desired to point out that the data in this paper refer only to bushings for transformers. The recommendations and conclusions should not be interpreted as recommending the same practice on bushings for other apparatus.

Conclusions

1. It is necessary to make many electrical tests to prove that bushings will meet service conditions.
2. Correction factors for flashover tests of gapped bushings, as proposed by the AIEE transformer subcommittee, hold within reasonable limits as indicated by tests over a considerable period and under widely varying conditions.
3. Present correction factors for humidity do not apply to ungapped bushings unless they are provided with the equivalent in the form of sharp or square corners.
4. Flashover values for ungapped bushings may be erratic or in some cases lower than when equipped with gaps.
5. Ungapped bushings may have very high negative-wave flashovers, and may result in flashovers directly to other windings rather than to ground unless excessive distances are taken.
6. Properly gapped, bushings meet all of the characteristics at present recommended by the AIEE transformer subcommittee. The fact that special electrodes or gaps are furnished may not result in any reduction in guaranteed strengths.
7. Bushings having performance substantially equivalent to the "equivalent gaps" proposed for bushings can furnish a considerable margin of protection to transformer windings without encroaching upon the factor of safety. It is probable that, including the factor of safety, and for a single surge, high-voltage surges at short time lags could be withstood.
8. It is desirable, under normal conditions, to use bushings having characteristics as nearly as possible equal to the "equivalent gaps" previously proposed, because much protection at no cost is thereby obtained, and the location of the initial flashover can be controlled.

References

1. Insulation Strength for Transformers, report of AIEE transformer subcommittee. Electrical Engineering, June 1937, page 753, and discussion in October 1937, page 1297.

2. Recommended Transformer Standards, Putman and Clem. Electrical Engineering, December 1934, page 1596.

3. Methods of Measuring Radio Noise, Bulletin No. *C*-9, Edison Electric Institute.

4. Impulse and 60 Cycle Strength of Air, Bellaschi and Teague. Electrical Engineering, December 1934, page 1638.

Discussion

R. E. Pierce (Ebasco Services Inc., New York, N. Y.): Regarding the question of whether or not bushings should be provided with gaps, the author's reasons for advocating their use are sound. They are a measure of protection to the transformer winding, and control the location of any flashovers.

However, because they do fix the location of flashovers, I feel very strongly that the gap electrodes should not be integral with the bushing itself, as illustrated in the paper, as experience has shown that a gap in this position is certain to cause thermal damage to the porcelain in the event of flashovers, but rather that the gap electrodes should be mounted or arranged on the bushing terminal and transformer cover in such a way that any arcing will be at a good distance from and preferably above the porcelain. Practically, therefore, complete knowledge of the characteristics, correction factors, etc., for the ungapped bushings is essential, in order to intelligently coordinate the insulation.

L. Wetherill (General Electric Company, Pittsfield, Mass.): This paper presents a broad view of the scope of the developmental testing which may be needed to establish the suitability of a line of bushings for modern service conditions. It is well that the magnitude and complexity of such an investigation should be appreciated since it also has a bearing on the circumstances encountered in revision of characteristics of existing bushings.

Quite a large number of tests are discussed, and it seems desirable that a way should be found to eliminate some of the tests which contribute little, if any, additional information. It is pleasing to note that Mr. Cole recommends discontinuance of the wet ten-second hold test. While this test has been used occasionally on circuit breaker bushings, it yields practically no additional information beyond that which is shown by the wet flashover test and the dry one-minute hold test. There certainly seems to be little justification for the retention of this test.

Mr. Cole refers to the one-minute hold test as being more severe than the dry flashover test. This doubtless results from the specific construction under consideration since it has been the writer's experience that, either with small bushings in which the dissipation of heat is easily accomplished without any great temperature rise, or in large bushings in which numerous oil ducts are disposed so as to thoroughly cool the main dielectric, it is possible to apply a one-minute hold voltage which is limited only by the tendency to flashover on the air end. Since the dry flashover test is corrected for atmospheric conditions, whereas the one-minute hold test is independent of atmospheric conditions, the relative severity of the two tests depends on atmospheric conditions. For standard atmospheric conditions the dry flashover test is more severe, but for low air density and humidity the one-minute hold test may, under extreme conditions, become more severe. However, as stated by Mr. Cole, the one-minute test is intended primarily for the purpose of verifying correct manufacture, and it would appear that for this purpose the one-minute test is preferable to the dry flashover since the voltage held is independent of atmospheric conditions.

The usefulness of the 60-cycle dry flashover test may well be questioned since the impulse flashover test is somewhat more severe. The impulse flashover test also has the advantage that it is more representative of the type of overvoltages which occur in service.

The fact that different designs have different characteristics is again illustrated by the 60-cycle endurance test in hot oil. It can be shown theoretically that the temperature rise produced by dielectric losses inside of a bushing having solid dielectric is proportional to the square of the applied voltage and is not affected by the thickness or configuration of the dielectric. The presence of condenser foils modifies this principle somewhat but does not alter the general principle that for low voltages the temperature rise is low and for high voltages the temperature rise is higher, even though the thickness of dielectric is increased to maintain the same stress intensity. As a result, it follows that on low-voltage bushings, there is no danger of a run-away thermal condition as long as suitable materials and thicknesses are utilized. On very high-voltage bushings with solid dielectric, the temperature rises are greater, and the possibility of run-away thermal condition must be considered. Even at very high voltages, if oil ducts are located so as to cool the major dielectric, there is no danger of a run-away thermal condition, and bushings of such design will demonstrate a dielectric strength on endurance test at high temperatures equal to or greater than that at low temperatures.

A similar distinction is found in the application of power factor and capacitance measurement to bushings of different construction. It has been our experience that on bushings which have no floating metal foils or which have floating metal foils with oil ducts between them, there is no tendency for the capacitance to vary more than the amount which is caused by the normal variation in the properties of available materials. Therefore, while we record power factor measurements of bushings, for the purpose of comparison with readings which may later be taken in service, we find that the capacitance readings are not useful in this connection.

It has been our experience that for bushings in excess of 25 kv the best possible radio noise characteristics can be obtained only by the use of liquid filling compounds which will have no tendency to form voids due to cooling or due to electrical discharges caused by overvoltages encountered in service. I believe that these advantageous features of liquid filling compounds are now being generally recognized.

It is satisfying in some ways to be able to measure the impulse puncture strength of a bushing, but we have found it preferable to measure this strength in terms of endurance rather than voltage. It has been our experience that standard bushings cannot be punctured on impulse since they will always flashover on the outside, even though totally immersed under oil. It also appears desirable that the impulse puncture strength should be verified with reference to its relation to the external flashover voltage. We have, therefore, undertaken to establish the impulse puncture strength by means of repeated flashover tests. Investigations with impulse flashover voltages of varying forms ranging from the full 1.5 x 40 microsecond wave to very steep waves rising at a rate of several thousand kilovolts per microsecond have indicated that the severest condition appears to result from the use of steep waves rising at approximately the rate of 1,000 kv per microsecond. We have made numerous endurance tests using this wave with which we expect a bushing to be good for approximately 1,000 applications at intervals of 30 seconds.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): Mr. Cole has presented a very complete paper giving the electrical characteristics of bushings. Unfortunately the paper emphasizes the use of gapped bushings as a protection to transformers against lightning.

There is one point in connection with the characteristics of a gapped bushing that should be mentioned. Unless the arcing length is appreciably reduced by the gaps, the benefits of the gaps in reducing the impulse flashovers disappear on steep wave flashovers when they are most needed. In fact, extremely steep-front waves will often flashover at some place other than on the gap electrode.

This, naturally, leads us to the question of why gaps were put on bushings in the first place. The principal reason was that when commercial impulse testing of transformers was set up a few years ago, it was required that the magnitude of the applied impulse wave be measured by flashing over the bushing. This required controlling the bushing flashover values by means of adjustable gaps. These gaps were provided primarily for this purpose and not as a protection to the transformer in service.

Now that impulse testing of transformers is to be made on a kilovolt basis, instead of on bushing flashover, there is no longer any necessity for the use of adjustable gaps on bushings during impulse tests. The principal objection to bushings is that gaps may give some operators the impression that they will protect the transformer under all conditions.

On account of the faster rise in impulse kilovolts of the volt-time curve of an air gap (as the time to flashover gets shorter), gaps give no positive protection to transformer windings against steep waves unless set much lower than that provided by standard gaps on bushings.

It is strongly recommended that if it is felt that a gap must be used for protection against overvoltage, it should be connected to the circuit a few feet away from the transformer bushing to prevent the possibility of a flashover damaging the bushing.

There is not much question but that some of the statements made in the paper will not clear up but will still further confuse the question of protecting transformers with gapped bushings. The statement "Co-ordination of bushing and winding becomes practical when a gap is used" might lead an operator who does not know the whole story to think that if a bushing has a gap on it with a standard spacing there is no need of providing any further protection. If hit with a direct stroke it is very doubtful if the transformer could stand it. All operators who are using gaps to protect transformers use spacings much lower than 44 inches for 138-kv circuits. One large operating company started with 20 to 22 inches for 110-kv service but later changed to 24 inches; another company changed from 20 or 22 inches to 11 inches.

Other well recognized and effective methods of protecting transformers as well as service, are available and these methods should be used instead of relying on gapped bushings which give only partial protection to the transformer and no protection to service.

It would have been much better if Mr. Cole's paper had pointed out the conditions under which gapped bushings give partial protection, rather than giving the impression that if the bushings are provided with gaps the transformer is co-ordinated which to many means that it needs no further protection.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The improvements in the present condenser bushing, discussed in Mr. Cole's paper, are the outcome of extensive work during the past several years. In view of the more exacting requirements fundamentals have been brought to bear on the various problems particularly where this approach was more conducive to practical results. One instance of this is the design for flashover to meet specified values and also means of readily controlling this important characteristic.

Standing back of this development are the extensive tests in the laboratory. Hundreds of repeated tests, both with long and very steep impulses have been applied to a bushing design or to bushings in combination with transformer windings. Of particular interest to operators is the experience in testing over $1^{1}/_{2}$ million kva of transformers equipped with condenser bushings. Further improvements in bushing design have been suggested through this experience but in particular it has been on this testing ground that ample proof has been given of the present condenser bushings design in fulfilling its specific impulse requirements.

The extent to which the development described by Mr. Cole has been pursued is illustrated in figure 1 of the paper. Here we have virtually a lightning stroke discharge across the bushing. The voltage applied rose at the rate of 3,000 kv per microsecond followed upon flashover by a current discharge of 90,000 amperes having a duration greater than 100 microseconds. While such severe tests may not be altogether a check on all possible service conditions to which a bushing may be subjected in the course of service, nevertheless, it is clearly an indication of the progress that has been made in the field of bushing design.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Mr. Cole's paper lists the electrical characteristics of bushings and shows the large number of tests necessary to show that a bushing is satisfactory for service. Most of these tests are design tests which are not feasible on each and every unit. Control tests and guaranteed tests can be reduced to relatively few. These latter tests can be made on the individual units.

It is of interest to go into the history of insulation co-ordination. Some years ago, statements were freely made that the higher voltage transformers as a whole were stronger against impulse voltages than line insulation, and that the windings in turn were stronger than the bushings. Experience was used as the evidence, and the surge generator had not been developed for use with transformers. Data of a reliable nature were not available for the common insulating materials.

The advent of the surge generator made it possible to check impulse strengths of materials and complete units, and it was found that experience was a poor teacher as shown by the results of the laboratory. In many cases, the strength of transformer windings was found to be lower than the strength of the bushing, and the bushing lower than the line insulation. It was felt that if the relationships between line insulation, bushing, and winding were specified when it was not known whether they could be obtained or not, they must be desirable and they should be obtained if possible. Careful investigation has showed that they could be obtained within reasonable limits, except in the cases of severe or direct strokes. The trend has been to abandon these desirable characteristics because they were found to be difficult to obtain generally. The acceptance of voltage values has led to a big reduction in the requirements and possibilities of a reduction in the impulse strength of equipment as now built.

There is one point which I would like to re-emphasize. That point is covered in Mr. Cole's paper, showing that a considerable degree of co-ordination has been obtained with bushings and insulation for the 138-kv insulation class. When the use of protective gaps was recommended, the gap length given was $38^{1}/_{4}$ inches. From figure 9, the full-wave value for negative waves for a $38^{1}/_{4}$-inch gap is about 700 kv, as high in voltage as the 44-inch gap for positive waves. It is understood that the negative-wave flashover of some types of bushing may be the same or less than the positive flashover. In this case, bushing flashovers might have occurred instead of protective gap flashovers due to insufficient margin between the protective gap and the bushing flashover. This should not have occurred with past designs of transformer condenser bushings due to the fact that the negative-wave flashover has been higher than the positive flashover.

The statement has been made that bushing flashovers invariably damage the porcelain. This is not always true. By the use of gaps properly arranged, the power arc can be largely directed away from the porcelain even though the gap parts are on the cap and flange. This can be demonstrated. Also, the initial flashover, either 60-cycle or impulse, can be made to clear the porcelain and not damage it. These features have not been emphasized, since it has been felt that bushing flashovers should be avoided. The use of a service gap, say $34^{1}/_{2}$ inches long for the 138-kv class, at the terminals of bushings should avoid bushing flashovers and provide a considerable measure of winding protection.

Where arresters are used, such a gap would only flashover in cases when the discharge current was excessive or the arrester was too far away from the transformer to supply protection for steep high surges.

Under these conditions, flashover is bound to occur somewhere, and the gap will locate it so that it will do the least damage and protect the transformer windings.

H. L. Cole: The subjects presented in the paper have resulted in some useful and interesting discussions, and the author appreciates the interest shown.

Mr. Pierce recommends that gap electrodes be away from the bushing and not integral with the bushing itself. If the gap is removed to a point where no damage can be done by flashover, it will probably be far enough away so that the bushing may be said to be not co-ordinated. Such a gap would be special for each transformer and the design would lose much of its simplicity and practicability.

Mr. Wetherill discusses the "endurance test" from the standpoint of type of bushing construction. Now there are a number of different types of bushing construction, all of which have been developed to a high degree of perfection. It was not the intention of the author to discuss differences between types of bushings, but it is important to note that the presence of condenser foils, as Mr. Wetherill describes the application of the condenser principle to a bushing, is what actually distributes the stresses uniformly throughout the dielectric, keeps the temperature at a low value, and makes the use of oil ducts to prevent overheating unnecessary.

It is not proposed that the bushing be used as a principal means of protection for the transformer. What is proposed, and what has been practiced for the past five years, is that a substantial amount of additional protection be obtained by making the bushing with impulse flashover characteristics so that it will have a flashover slightly under the transformer winding strength for as many types of waves as practicable.

Referring to Mr. Montsinger's discussion, the primary reason for controlling bushing flashovers was not for measurement purposes, but to give some winding protection. Originally the operators insisted that bushings have a lower impulse level than the windings. They did not require the use of gaps. Gaps were installed by the manufacturer to obtain either uniformity of results or the desired level. The bushing gapped or ungapped was demonstrated to give some protection to the transformer in service, and was not in itself a measuring device.

Even before commercial testing, there was a demand for co-ordinated bushings. Some engineers disagreed with the idea, saying that it was impractical. The demand persisted, and the writer tackled the problem of producing a bushing which would have as good co-ordination as possible. The present paper describes the progress made and the results obtained.

Co-ordination of Physics With Electrical Engineering

By J. M. BRYANT
MEMBER AIEE

THE early departments of electrical engineering in American universities were outgrowths of courses developed in physics. In most engineering colleges some member of the physics faculty offered application courses in physics for engineering students and after a few years these developed into a complete curriculum with a separate faculty for electrical engineering in parallel with the older civil and mechanical engineering curricula.

The early courses were mostly descriptive in nature with some emphasis on design of electrical machinery. The only text available was that of "Dynamo Electric Machinery," a two-volume text on d-c machinery by S. P. Thompson, an English author. The early American texts in a-c theory were inadequate, with very few proofs, and the minimum of mathematics. The early papers and texts of Doctor C. P. Steinmetz were the first to lay a proper foundation in mathematical development for advances in a-c theory. Practicing electrical engineers were not properly prepared in either physics or mathematics to understand these texts and Doctor Steinmetz adopted calculus methods for the solution of differential equations and series methods for solution of long transmission lines, so that his texts might be of real use to these engineers. To the early text on "Alternating Current Phenomena" he added others, including his well-known text on "Transient Electric Phenomena" and then one on "Electrical Engineering," a book adopted as an elementary text for students in some engineering colleges. In each of these texts, the solution of circuits by the use of the complex number was introduced and the machines were reduced to circuits for their solution. This was a revolution in method, since most previous authors had developed a new theory for each machine and the machine and the circuit did not fit together.

Many colleges were slow to adopt these newer mathematical methods of analysis partly from the inertia and poor preparation of the faculty and partly from the lack of proper emphasis along these lines in the fundamental fields of physics and mathematics. When students, trained in these new methods of analysis, were absorbed by the industry, and more emphasis was placed on fundamentals of analysis rather than on descriptive methods, a distinct gain was possible in the development of better designs for electrical devices and machines and the extension of the field was much accelerated. The rapid advance of electrical engineering in the past 25 years is due in a large measure to the ability of engineers to analyze the problems mathematically and to measure the quantities involved with exactness.

At the present time adequate texts are available in a-c theory for both undergraduate and graduate instruction of electrical-engineering students and as reference texts for practicing engineers. In these texts the authors have adopted mathematical proofs with application to modern machinery and transmission circuits. Much praise is due to the willingness of publishers to encourage better texts in this field when profit has not been their only motive.

In the field of communication the early texts were mostly descriptive of the equipment and circuits with little emphasis on calculation or of background physical principles. Development in research by physicists in the field of electrical discharges in gases and the development of the theory of electron emission have made possible the development of vacuum tubes which have revolutionized the whole field of wire communication and have brought forth the newer field of radio communication. The vacuum tube and improvements in transmission circuits have changed the entire field of transmission of speech and music over wires by introducing repeaters at frequent intervals along the line which with better circuit equalization have made long-distance wire-communication clearer and of higher fidelity than local communication was a comparatively few years ago. With the advance of radio from that of telegraphy to that of telephony, it has been possible to link all phones in one international system. The early courses in vacuum tubes and radio were developed in some universities in the department of physics and in others in electrical engineering.

The vacuum and gas-filled tubes have invaded the power field of electrical engineering and these new applications are rapidly expanding into the fields of circuit control and d-c transmission of power. The early carbon-filament vacuum incandescent lamp was displaced by others using tungsten filaments, at first having a vacuum and later by those filled with inert gases. These advances were brought about by research in the fundamental physical phenomena. Other developments along parallel lines have produced the numerous electric lamps using electrical discharges in gases and vapors at low and medium pressures. Further research by physicists in gas laws and electric discharges have laid a background for the study of lightning, for the X-ray tube, and for various new subdivisions of the atom, giving rise to new products and new technique in both physics and engineering.

The two fields of physics and electrical engineering are again becoming more closely allied, so that courses may be developed by teachers in either department and taken by students in both departments. At the University of Minnesota, courses in acoustics, in electronics, vacuum tubes, and communication, both wire and radio, are taught in electrical engineering while those in the fundamental gas laws and nuclear physics are taught in physics. These courses are taken as majors by graduate students in either department.

In the field of mathematics a similar

Paper number 38-83, recommended by the AIEE committee on education, and presented at the AIEE summer convention, Washington, D. C., June 20-24, 1938. Manuscript submitted May 2, 1938; made available for preprinting May 27, 1938.

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The Co-ordination of Mathematics and Physics With Electrical-Engineering Subjects

By WALTER A. CURRY
FELLOW AIEE

NO DEFENSIVE arguments are required to justify the need and desirability of co-ordination between mathematics, physics, and electrical-engineering subjects; such co-ordination must exist to some degree in every electrical-engineering curriculum and the essential objective of this symposium is to create a discussion of views and to reveal the instrumentalities whereby such co-ordination may be and is achieved.

Fundamentally, the initial objective of an engineering program of studies is to inculcate in the student an appreciation and grasp of that approach to the solution of problems which has been designated as the scientific method, represented by an ordered sequence consisting of observation, analysis, generalization, and application.

This method rests squarely upon the science of physics, which treats of matter and energy, and the science of mathematics, which sets up the philosophy and techniques whereby the quantitative relationships of physical laws and principles may be expressed, investigated, and evaluated. The student entering upon the course in physics, having previously prepared himself in mathematics, is therefore qualified to fuse the two fields in the combined relationship which is basic to the scientific method. Thus, in the Columbia program, the student has a year of mathematics before entering upon the course in general physics, and two years of mathematics and one year of physics before he reaches his first course in electrical engineering subjects.

Training the student, in his physics courses, to search for the fundamental principles upon which a statement of the problem may be formulated, to translate such statement into its mathematical equivalent, to solve this equivalent and to interpret the results so obtained, represents an invaluable educational process. Such training actually expresses co-ordination with subsequent engineering courses to the highest degree, since these courses will utilize the same principles in attacking problems presented by their subject matter.

Such co-ordination does not imply that there shall be no overlapping of subject matter; a student in the electrical-engineering subjects may be effectively "tuned" to new material if the instructor gives a brief résumé of the underlying physical laws, in spite of the fact that these are known to have been discussed in previous courses. This not only refreshes the student's memory, but reemphasizes the alliance between the new applications and the earlier subject matter, problems in both of which are amenable to the same scientific method for their attack and solution.

To keep such résumé within reasonable limits, however, there must be effective co-ordination between the electrical-engineering subjects and physics. At Columbia, this is provided in part through informal and voluntary discussion between instructors in the two fields. It is accepted as part of their duty, that instructors in physics inform themselves specifically as to the knowledge of physics assumed in engineering courses by members of the engineering-school staff, and conversely, those giving instruction in the engineering school are charged with keeping definitely informed on the instruction in general physics. There are available in the electrical-engineering department detailed specifications of the subjects treated in general physics, which vitalized by interpretive discussion, provide those elements necessary to the efficient collaboration of the two fields.

Referring to the student in engineering, the objectives of these courses (in general physics), recently defined by a faculty committee, are as follows:

1. To give the student knowledge of the phenomena of matter and energy, and training in how they can be quantitatively analyzed and classified by measurement and theory, so that he may make use of this knowledge and training as a common basis of introduction for further study in various courses in engineering.

2. To give the student a knowledge of the interrelationships of the different branches of physics, and an understanding of the development of the science of physics, its direction of progress, and the developments that have not as yet proceeded to the point of engineering application.

Thus, the desired co-ordination between the physics courses and the engineering subjects which follow is recognized in the first objective, while the second is essential to the broad engineering program. It may be noted incidentally that only about 60 per cent of the groups which attend the general course in physics are headed for engineering; the course is taught as physics and is designed for all students who intend to

Paper number 38-67, recommended by the AIEE committee on education, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted May 13, 1938; made available for preprinting May 20, 1938.

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co-operation has been experienced as with that of physics. In spite of the handicap of poorer preparation in mathematics of students entering our colleges, the mathematics departments of the universities have been able to prepare these students for their undergraduate work in calculus. By a co-ordination of this work with a greater application of mathematics in electrical-engineering courses during the junior and senior years, the student is now better prepared than formerly at the end of his senior year. The department of mathematics has also co-operated by offering courses in differential equations and advanced calculus, in operational analysis, vector analysis, functions of complex variables and matrices for graduate students in electrical engineering and physics to parallel new methods of analysis required by these students and by industry. Through the use of these fundamental methods electrical engineering has passed from an experimental to an exact science. The hope for future advances in the field of electrical engineering still lies in the application of mathematics and physics to the solution of new problems and the design of new equipment.

Discussion

For discussion see page 43.

major in science, including those who will specialize in physics and those who will enter engineering.

The electrical-engineering subjects rely on the preceding work in physics to establish clear concepts and understanding of the fundamental laws and relationships governing elementary magnetic phenomena, electrostatics, and the laws of current flow in simple d-c circuits. Dimensional relationships and systems of units are important adjuncts. The subject of light, as discussed in the physics course, is planned to establish a sound basis for the subject of illuminating engineering in the subsequent electrical-engineering program, and similarly treatment of sound and wave motion, atomic physics, and the conduction of electricity through gases, form foundation elements for the subjects of communication and electronics. Knowledge of the laws of motion for rotating bodies, treated in physics and mechanics, is manifestly essential to the proper understanding of generator and motor performance discussed in subsequent electrical-engineering courses.

The co-ordination between electrical-engineering subjects and mathematics follows along lines similar to those relating to the work in physics. The earlier mathematics subjects (analytic geometry and the calculus) coming throughout the first and second years, provide a basic training which is essential to all fields of engineering. These subjects are given in the college and are taken by mixed groups, of which possibly 60 per cent are planning to enter engineering. The major objective of this two-year sequence of formal mathematics is the development in the student of an appreciation and clear understanding of the science which is mathematics, coupled with the attainment of a measure of skill and confidence in the application of this science to the solution of problems, many of which have their framework in the fields of mechanics and physics.

This two-year sequence is followed in the third year by a one-semester course in engineering mathematics which discusses such items as:

> Fourier series, line integrals, ordinary and partial differential equations, complex number operations, determinants, elliptic integrals, empirical equations, harmonic analysis, nomographs, probability, gamma and Bessel functions.

Naturally, with a content of this scope projected for a one-semester course, the student does not proceed to great depth in any one of the above items. The objective is rather to acquaint him with them, and to give him some indication of their application to practical problems, particularly in the field of electrical engineering. The subject of differential equations, however, touched upon in a preceding course in the calculus is, in this course, carried considerably further, about one-half of the time being devoted to this item.

This course was instituted at the request of the electrical-engineering department and its objective is "to equip the student with the mathematical mechanism commonly used in all branches of engineering, but pointed particularly toward electrical engineering." It represents a directed effort to advance the student from what may be termed the "minimum" type of educated man to the "superior" type, these types being defined as follows:

> The "minimum" type knows the important physical concepts and is fairly well aware of their function in engineering problems, but he is not very strong in the mathematical management of these physical concepts. His strength is intuitional and experimental. He may be a valuable man.
>
> The "superior" type has all that the minimum type has, but in addition he has learned with his concepts of physics, the mathematical formulation of them. This man has superior capacities for design and research.

The liaison between mathematics and the electrical-engineering subjects is provided by an "engineering-minded" member of the department of mathematics, Professor L. P. Siceloff, who conducts the course in engineering mathematics, and who is the author of the preceding definitions. This link is an effective one and through it there flows an easy interchange of suggestions and requirements which may arise from time to time.

The sequence in mathematics beginning with analytic geometry, continuing through calculus, and concluding with engineering mathematics, provides the necessary skill to enable the student to master the mathematical aspects of his subsequent work in electrical-engineering subjects.

Furthermore, these courses, jointly with those of physics, are directly utilized in the fourth year, by a two-semester course in electrical-engineering problems, the purpose of which is to give the student an indelible realization of the intimate relationship which exists between electrical engineering, mathematics, and physics.

The means chosen for accomplishing this objective is a series of very carefully constructed problems which are so designed that their solution demands clear analytical procedure, combined with a certain degree of resourcefulness and perspicacity, while at the same time the student is required to understand and explain fully the origin of the underlying fundamental ideas and their extension and application to the problem at hand.

In the lectures that accompany this course the student has pointed out to him the connection between the general fundamental idea and the engineering applications. The fundamental ideas are always expressed in their general mathematical form, often using mathematics not available to the student at the time the principle was originally introduced in physics. The student learns the application of mathematical processes to engineering problems and the significance of mathematical operations interpreted in terms of physical and engineering phenomena.

As has been indicated in the foregoing discussion of co-ordinating courses, an essential factor in such co-ordination resides in the establishment of close personal relationships between the instructors of the several departments concerned, enriched by a cordial spirit of co-operation and motivated by a high idealism in the objectives to be sought and attained. But co-ordination, once achieved, cannot be left to maintain itself. The inevitable and necessary revision which the subject matter of an engineering or science course undergoes as time brings new developments and viewpoints in those fields, demands that such co-ordination be re-examined at intervals to insure its continued existence. At Columbia, during the past several years, this has been accomplished through committees, with membership representative of the departments involved, including those of mathematics and physics. Such a committee is serving at the present time, and it is anticipated that similar committees will be appointed from time to time in the future.

In conclusion it may be said that there is an essential unity to all knowledge, and the student in electrical engineering will value and appreciate his early and later courses more highly if there is an efficient continuity throughout his schedule of studies. This will be accomplished if there exists a planned co-ordination of his three major fields, namely mathematics, physics, and electrical engineering.

Discussion

For discussion see page 43.

Mathematics and Physics in Engineering

By MICHEL G. MALTI
MEMBER AIEE

MANY definitions are given of an engineer. For the purpose of this discussion I would define an engineer as a scientist who uses physics and mathematics to promote the welfare of mankind. His use of physics and mathematics presupposes a thorough knowledge of them and demands that he be capable of:

(*a*) Reducing his engineering problems to fundamental physical facts.

(*b*) Expressing the physical facts in mathematical form.

(*c*) Deriving from the mathematical statement the desired mathematical result.

(*d*) Interpreting the mathematical result physically.

These points are so important that they justify further elucidation. Let us illustrate them through the familiar problem of determining the transient current in a series circuit, with resistance and inductance, which is suddenly subjected to a constant electromotive force:

(*a*) The physical facts here involved are that the applied electromotive force is consumed in drops through the resistance and inductance.

(*b*) The mathematical expression of the physical facts is:

$$E = ri + Ldi/dt$$

(*c*) The derivation of the mathematical result consists in solving this differential equation and obtaining

$$i = (E/R) + K\epsilon^{-Rt/L}$$

(*d*) The interpretation of this result is that the current may be anything depending upon K. Hence we resort to the physics of the problem and assert that the current is zero at the instant $t = 0$ when the electromotive force is impressed. This gives $K = -E/R$ and hence the current for this particular initial condition is:

$$i = (E/R)(1 - \epsilon^{-Rt/L})$$

I have used this simple illustration to show the four processes involved in all engineering problems. The degree of ease or difficulty of one or the other of these processes varies with the nature of the problem. But each process exists in every engineering problem no matter how difficult or easy it may be.

Now it so happens that part *c* (the actual derivation of mathematical results) is something that an engineer need not master because he can always resort to the mathematician for the performance of this task. But every engineer should be a master of parts *a*, *b*, and *d* because neither the physicist nor the mathematician can be of assistance to him in this matter.

I doubt if there exists any disagreement among engineers about these statements. We also admit that the majority of engineers lack these essentials otherwise we would not be holding this symposium. We therefore conclude that something is wrong either in our teaching of physics and mathematics or in the proper co-ordination of these subjects with engineering courses or in both.

All these are facts which we agree upon. What we do disagree upon are the real causes of this condition and the remedies to be adopted. I do hope that when this symposium is ended we shall have attained a clearer insight into the real causes of the trouble and shall have adopted a course of action which constitutes a fundamental and effective remedy.

I shall now endeavor to give some of the causes and suggested remedies as I have collected them through talks with educators, students, and engineers and through personal observation and experience. This will then be succeeded by a critical analysis of them and a suggested solution of the problem.

(*a*) *Incompetence of Mathematics and Physics Teachers.* Some educators trace our woes to the teachers of mathematics and physics. They assert that these teachers do not understand the point of view of the engineer and are, therefore, unable to teach him these subjects. Both mathematicians and physicists are prone to emphasize facts which are essential to them but which are utterly useless in engineering applications. Furthermore physicists and mathematicians do not know engineering problems. Hence they are not capable of discerning what is and what is not essential to engineering. Obviously the remedy here consists in having mathematics and physics taught to engineers by engineers.

(*b*) *Lack of Co-operation.* Another group agrees that the viewpoint of the engineer should be stressed in both mathematics and physics courses but are less antagonistic to teachers of mathematics and physics. They are inclined to take a little of the blame upon themselves and ascribe the trouble to lack of proper co-operation between teachers of engineering on the one hand and teachers of mathematics and physics on the other. Some of the remedies here suggested include:

1. Frequent conferences between faculty members with a view of co-ordinating efforts.
2. Actually supplying the departments of mathematics and physics with typical engineering problems which these departments could use for illustrative purposes.
3. Telling the teachers of mathematics and physics what is and what is not essential in engineering and requesting that only essential facts be stressed in teaching physics and mathematics to engineers.

(*c*) *Lack of Time.* A third group blames neither the teachers of engineering nor the teachers of physics and mathematics. They assert that competence and co-operation exist to as high a degree as may be expected. Time or rather the lack of it is the cause of our trouble. Indeed the engineering curriculum is so crowded and the student is so rushed with his courses that there exists no time to stress the mathematical and physical aspects of engineering. To train an engineer to correlate physics, mathematics, and engineering is truly a time-consuming and a very exacting task. Moreover since time cannot be conserved through the elimination or revision of engineering courses there appears three possible remedies.

1. Extension of the engineering course to more than four years.
2. Offering graduate courses in colleges to men who expect to engage in strictly engineering work.
3. Delegating, to industry, the responsibility of training engineers through advanced courses.

(*d*) *Incompetence of Engineering Teachers.* A very small fourth group is willing to shoulder the whole blame and admit that, sometimes, the cause of the trouble can be traced to the very teachers of engineering themselves. This group argues that there is a golden opportunity for engineering teachers to correlate mathematics and physics in their own courses. Such teachers should stress the physical laws and mathematical relations which form the bases of engineering applications in order that the student may form a concrete picture of how physics and mathe-

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Paper number 38-68, recommended by the AIEE committee on education, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted May 2, 1938; made available for preprinting May 23, 1938.

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matics are used by the engineer. If the contention of this group is true, then the remedy obviously consists in having, on our engineering faculties, men who combine a good engineering training with a thorough and comprehensive knowledge of mathematics and physics. Such men should, moreover, possess the gift of conveying their thoughts in a logical, clear, and comprehensive manner to their students. The task of the engineering teachers thus becomes most exacting and their selection requires the most careful scrutiny of college presidents, deans, and department heads.

It would appear that elements of truth exist in the contentions of all four groups. Let us, however, examine them a little more critically.

(*a*) While it is desirable that physicists and mathematicians do appreciate the engineer's point of view, such appreciation is of precious little help to the freshman and sophomore student who has no inkling of what engineering is! To tell a freshman that the laws of gases are used in connection with engines, turbines, and air-conditioning machines might attract his interest. But it means little or nothing to him because he knows nothing about the design or operating characteristics of such machines. Nor would it profit the sophomore much when he is advised that Faraday's law of induction is the basis of the design and operation of all electric machines because he knows little if anything about electric machines. It still means less to an immature student to be told that a linear differential equation of the first order with constant coefficients represents a series circuit with R and L or with R and C because such a circuit is just as mysterious to him as the differential equation itself. Indeed, to an immature freshman and sophomore, engineering is more strange than the physics and mathematics he studies. It would appear, therefore, that the time to show the application of physics and mathematics to engineering problems is not during the freshman and sophomore years but during the junior and senior years when the student is actually engaged in studying engineering. It thus becomes incumbent on the *teachers of engineering* rather than those of physics and mathematics to demonstrate and elucidate the application of these basic sciences to engineering problems.

(*b*) There is much to be gained from conferences between teachers of engineering and of the basic sciences provided these conferences are well planned and well directed and provided that a sincere effort is exerted by all to see one another's point of view. Such conferences should have for their object:

1. The understanding, by mathematicians and physicists, of the processes involved in the solution of engineering problems as outlined earlier in this discussion.
2. The understanding by engineers of the processes of analysis which a mathematician utilizes in formulating and solving his problem and the physicist uses in making his discoveries of physical laws and in correlating physical phenomena.

To my mind it would be disastrous to dictate to a physicist or mathematician what he should or should not teach engineering students or how he should present the subject matter. To dictate the contents of a course would emphasize the utilitarian and exclude the interest aspect of education. To dictate methods of presentation would rob the student of the mental enrichment which comes out of studying several modes of attack and establish in his mind narrow tracks and processes of reasoning which might or might not prove helpful in attacking his future problems.

(*c*) The problem of lack of time is a serious one and I would welcome the extension of the engineering course to more than four years provided all educational institutions throughout the country agree to do so. But I insist that, even in four years, engineering colleges can do better by their students if they require less substitution in formulas, less graph plotting, less slide-rule work, and more brain work. In fact, even if the engineering course is extended to five or six years I would recommend that the present mode of running some courses, with the idea of consuming the student's time rather than exercising his brain, be abandoned, and the courses be so changed that time is spent in thinking rather than in slide-rule pushing and graph plotting.

(*d*) It is obvious that all these recommendations lead to one effective solution —the improvement of engineering faculties. The remedy lies in having engineering teachers who are not only well-trained engineers but who are also good mathematicians and physicists. The time is past when a knowledge of algebra and of Ohm's and Faraday's laws were all the tools that an electrical engineer needed. The time is gone when an engineer jokingly remarks that the place of an integral sign is on the violin. Such mathematics as functions of complex variables, functions of real variables, theory of sets, theory of groups, calculus of variation, ordinary and partial differential equations, advanced algebra (tensors, etc.), and vector analysis are commonly used in the solution of engineering problems. As to physics, the electron theory comprising conduction of electricity through gases, electron emission, photoelectricity, X-rays, etc., and such subjects as quantum mechanics, light, heat, and others are just as essential to the electrical engineer of today as Ohm's law and Faraday's law of electromagnetic induction were to the pioneer engineer. Engineering teachers should realize these facts and prepare themselves to meet present-day demands.

The following evolutionary process may be suggested to improve our present faculties in the larger universities:

(*a*) Have all graduate students in engineering who are working for a degree of doctor of philosophy take mathematics and physics as minors.

(*b*) Appoint assistants from among these graduate students and keep records of their scholarship and teaching ability, as well as their character and personality.

(*c*) Select from among the assistants those who show most promise and encourage them to specialize along various lines. These men, upon receiving their degree of doctor of philosophy should be appointed as instructors.

(*d*) Make research accomplishments and teaching ability necessary (although not sufficient) conditions for promotion.

(*e*) Have the faculty keep in touch with industry in order to follow the developments of the time.

If this policy is adopted by our leading educational institutions every first-class college would, within a brief period of perhaps ten years, have an excellent faculty and the problem we are discussing in this symposium would become of minor importance!

But I hear the voice of the personnel man reminding me that over 80 per cent of our graduates finally end in sales offices or in executive positions with very little possibility of their ever using physics, mathematics, or even engineering and that the remaining 20 per cent or perhaps even 10 per cent who actually do engineering work are trained in special industrial courses! So why all this fuss about knowledge of mathematics and physics and their correlation with engineering subjects? To the personnel man and his ilk I say:

The purpose of all education is mental training. College courses are but vehicles to attain this end. In these courses facts are learned and quickly forgotten. What the student retains are the mental processes which he unwittingly acquired while he pursued these courses. If one's mind is fertile and receptive and his tutors are alert, enthusiastic, and competent, then one's mentality should be so

Courses to Develop Facility in the Use of Mathematics and Physics in the Solution of Engineering Problems

By B. R. TEARE, JR.
MEMBER AIEE

IN RECENT years engineers and teachers have recognized[1,2] that many graduates lack facility in the use of mathematics and physics in the solution of engineering problems. Appearing to know the principles of science that underlie engineering, the young graduates frequently cannot apply them correctly in new situations. It is evident that the development of the requisite facility depends upon first, mastery of the basic sciences and second, adequate training in their application. Thus, there is need for two different kinds of co-ordination of mathematics and physics with electrical engineering: co-ordination of the departmental teaching activities for most effective instruction, and co-ordination of the subject matter in the student's mind for most effective use.

Co-ordination of the first kind may be achieved by close co-operation between the faculties of the departments concerned. Various ways of obtaining the co-operation are available.[3,4] To cite an example, there is a committee for this purpose at Yale University, composed of representatives from the mathematics, physics, mechanics, and various engineering departments. Committee meetings, held frequently, provide opportunities for the interchange of points of view and for suggesting, where desirable, a redistribution of the emphasis placed on particular topics in the various courses. Although the first type of co-ordination is recognized as essential, it does not remove the necessity for the other type.

The student has been well instructed in the basic sciences but too often has not received adequate training and practice in their co-ordinated use. Such training and practice can be given by means of a course organized for this purpose. For example, the advanced course in engineering of the General Electric Company,[2] working with a selected group of graduates in industry, has for one of its objectives the development of facility in the application of mathematics and physics to the solution of engineering problems, and is notably successful in attaining this end. It is the purpose in what follows to discuss a program to give such training to the student before he leaves college.

For the last six years the electrical engineering department at Yale has given junior and senior courses of three hours each comprising a unified two-year program of this type, and also a parallel one-year graduate course. The subject matter, and manner of conducting the courses are selected for the most effective co-ordination of the basic sciences with electrical engineering. The introduction of new topics is subsidiary to the development of a thorough understanding of the old.

Since the aim is to develop facility in the use of mathematics and physics in the solution of engineering problems, naturally the chief emphasis is on problem solving, both in class discussions and in assignments. As far as possible, problems are selected which present new situations, where the solutions are to be obtained not by substitution in handbook formulas but by the application of such fundamental principles as, for instance, Newton's laws of motion, or Fourier's law of heat conduction. Two examples, representative of the more difficult assignments, are given for illustration.

1. It is proposed to support a motor by means of an elastic mounting in order to reduce vibrations that are transmitted through the mounting to the supporting foundation. The vibrations originate in centrifugal forces due to mechanical unbalance, or in pulsating electromagnetic forces, and occur at definite frequencies. The effectiveness of the mounting is to be measured by a quantity called transmissibility, which for vibrations of a given frequency is the ratio of the steady-state amplitude of the force transmitted to the base through an elastic mounting to that transmitted through a rigid mounting.

For an elastic mounting, which has damping, and considering only vertical vibrations,

(*a*) Determine the transmissibility in terms of frequency and system constants.

(*b*) Illustrate the result by sketched curves of transmissibility as a function of vibration frequency for various values of damping.

(*c*) Discuss the value of an elastic mounting which has little damping, if the vibration frequency is about half the natural frequency of the vertical vibrations of the motor on its mounting.

2. In a proposed thermocouple ammeter, the current to be measured is passed through a thin straight metal strip supported between two massive terminal blocks. One junction of a thermocouple is placed at the midpoint of the metal strip, the other junction on one terminal block, but electrically insulated from it. The thermocouple is connected to a galvanometer.

(*a*) Express the temperature difference between junctions in terms of the voltage drop between terminal blocks, dimensions, and thermal constants.

(*b*) What length of heating element will give the largest temperature difference between junctions for a given power loss?

(*c*) What are the disadvantages of this type of meter?

That these problems are of practical engineering interest is shown by the fact that they were presented in technical papers appearing during the last five years.[5,6] The students were not given the

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Paper number 38-82, recommended by the AIEE committee on education, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted May 2, 1938; made available for preprinting June 7, 1938.

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[1] For all numbered references, see list at end of paper.

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developed, when he completes his college training, that he can:

(*a*) Study independently of teachers.
(*b*) Synthesize isolated facts.
(*c*) Generalize from fundamentals.
(*d*) View facts objectively.
(*e*) Visualize things.
(*f*) Do original work.

I know of no mental faculties that are more essential to an engineer than the above. This holds true whether he be a salesman, a designer, an executive, a teacher, or a gang boss. Moreover I know of no courses which develop these mental faculties more intensively and more sharply than mathematics, physics, and engineering.

Discussion

For discussion see page 43.

references until after they had obtained solutions.

In all the assignments, except the relatively few exercises employed for drill in new topics, the student is given an unfamiliar situation in which simplifying assumptions must usually be made in order to obtain a solution, and in which as far as possible he must select the variables with which to work. The required physical principles and mathematical techniques, however, will have been covered in class work and perhaps in other assignments.

Through the duration of the courses much emphasis is placed on the orderly arrangement of thought processes in the solution of problems. The arrangement which is applicable to problems of the deductive type, has been discussed elsewhere[1] and may be summarized in the following steps:

I. Stating the problem and specifying the desired result. The student is to use his own words and illustrate with sketches where possible.

II. Formulating a plan of solution, which includes

(*a*) Selecting a principle to be applied;

(*b*) Making whatever simplifying assumptions may be necessary;

(*c*) Planning the treatment as far as it may be foreseen.

III. Stating in precise English the implication of the chosen principle in the particular problem.

IV. Translating the statement of III into mathematical form which includes

(*a*) Specifying notation;

(*b*) Selecting co-ordinates;

(*c*) Writing III as an equation.

V. Solving IV*c*, which usually includes introducing additional relations, and perhaps also additional simplifying assumptions. Should a solution of IV*c* be impossible, a fresh start applying a new principle is to be made.

VI. Testing the solution

(*a*) For dimensional homogeneity;

(*b*) With extreme values assigned to variables;

(*c*) For reasonableness in the light of experience;

(*d*) For possible effect of the simplifying assumptions.

While this formulation may appear inflexible, it is so only as regards the inclusion of all the steps and in order. Many problems require the use of more than one principle; in other words the desired solution can be considered to result from the simultaneous application of a number of principles. However, this organization of thought processes implies that but one is to be chosen to start the solution, and there may be some latitude in the choice. Moreover, there may be another kind of selection to make since some physical laws are merely different expressions of the same fundamental truth. Any one of these may be used in a given case, although often a particular one leads to a more direct solution.

The principles which are discussed in the courses and used in the solution of problems comprise the general fundamental laws of mechanics, heat transmission, electrostatics, magnetostatics, and circuit theory, such as Newton's laws of motion, conservation of energy and of momentum, the principle of superposition, Fourier's law of heat conduction, Newton's law of cooling, Coulomb's and Gauss's laws for electric and magnetic fields, Ampere's and Kirchoff's laws, Faraday's electromotive-force law, and the constant-linkage theorem, to name but a few. The student has encountered most of these previously in mechanics and physics and indeed can usually state them with fair accuracy. He is expected, however, to develop such understanding of the laws, their range of application, and the physical quantities they relate, that he can select appropriate ones for the solution of a new problem.

Considerable time is devoted to the review and use of familiar mathematics and to the treatment of more advanced techniques, including the solution of second-order linear differential equations with constant coefficients, elementary vector analysis, Fourier series, symmetrical components, and dimensional analysis. These new topics are introduced in the solution of particular problems and later generalized.

The analogous behavior of electric circuits, mechanical systems, and heat conduction systems, expressed in exactly similar equations, provides a natural unification of portions of mathematics and physics with engineering, and is of great assistance in augmenting the student's understanding. Likewise the broad analogy among the space fields of electrostatics, magnetostatics, gravitation, thermal and electric conduction, and fluid flow described by similar vector equations and identical field maps, furnishes another natural means of integrating the subject matter. A consideration of the points of difference between analogous systems is as helpful in building up understanding as the mention of matters of similarity.

Classes are conducted as seminars, where each student contributes to the discussion, rather than by formal lectures. Supplementary laboratory exercises are given in the junior year, in which the student is required first to solve a given problem theoretically and then verify his result experimentally. In the senior year the work culminates in projects, individual development problems usually requiring work in the laboratory, and short enough so that two or three can be completed. Such projects, for example, may be the combined theoretical and experimental study of dynamic braking of d-c machines, or of the performance of thyratron inverters. About a quarter of the senior course is devoted to this kind of activity.

The relation of these two undergraduate courses to the program as a whole has been described elsewhere.[1] They are required of all the undergraduates in electrical engineering, and each course is allotted one-fifth of a year's time. They are co-ordinated with each other and with the other electrical engineering courses to unify the program as a whole. At the end of the senior year the student's grasp of the entire technical portion of his work in all four years is tested in a searching departmental, or comprehensive, examination.

In conclusion, the courses to develop facility in the use of mathematics and physics in engineering problems have these essential features:

(*a*) Training in an orderly thought process;

(*b*) Integration of the student's knowledge of the basic sciences;

(*c*) Extensive practice in the solution of actual engineering problems.

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Discussion

For discussion see page 43.

Co-ordination of Mathematics and Physics With Electrical-Engineering Subjects

By THEODORE H. MORGAN
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IN RECENT years all branches of engineering have experienced rapid growth and progress. Technological advancement has been accompanied by an increasing realization of the breadth and scope of the profession. College curricula have been altered and adjusted in attempts to meet changing conditions. We have been forced to realize that the solution of our educational problems is not to be found in an increased amount of work or in adding further courses to already overcrowded curricula, but rather in more complete co-ordination and unification within the programs themselves. Due to the manner in which the engineering curricula have been built up through the years, new subjects being added from time to time as was found necessary, we are in danger of thinking of a curriculum as being composed of separate blocks, i.e., so much mathematics, so much physics, chemistry, and so forth. Our problem is to break down the well-defined boundaries of these blocks so that they will be combined to form a unified and coherent whole. We might compare the curriculum to a painting for which the artist carefully chooses a number of colors which are not used separately in blocks, but are harmoniously blended and intermingled in a complete picture.

Let us look at mathematics and physics. When taught only as the science of order, giving quantitative and magnitudinal relations of pure numbers, mathematics loses much of its value to the engineer. The student should become familiar with this subject as providing an indispensable and powerful tool of the profession. In teaching mathematics, as the beauty and mystery of pure numbers is unfolded to his mind, the student should learn at the same time the practical value of the subject in dealing with physical relations. This can best be accomplished through a high degree of co-ordination between the courses in mathematics and those dealing with the physical sciences, of which the subject of physics is perhaps the best example. The co-ordination that I have in mind requires close relationship between departments. The instructor in mathematics must be thoroughly conversant with the course in physics, and vice versa, so that the work is carried forward simultaneously—the mathematician applying the laws of physics in his mathematical developments at the same time that the physicist is building in the student's mind an understanding of a fundamental law which is being given definite form and expression in mathematical symbols. The learning of general mathematical principles, manipulations, and technique, should go hand in hand with their use and application. The student would thus realize that in mathematics he has a powerful and precise instrument which can be used with perfect exactitude to solve physical problems. As this usefulness became increasingly evident both mathematics and physics would take on new interest and importance in his mind.

The subject of physics by its very nature covers a greater field than any other course in the curriculum. It is the foundation for all engineering courses, which are largely specialties in physics having important practical value. It thus becomes essential that the engineering student form proper habits of thought in such matters. His introduction to principles which are new to him should be accomplished in such a way as to appeal to his common sense and intuition. In contrast to the memorization of stated formulas the student should be required to perform for himself the much more important and difficult task of expressing physical laws and facts in mathematical terms. Once this is accomplished the mathematical developments and manipulations would follow simply and easily. The acquisition of fundamental knowledge would go hand in hand with the development of scientific analysis, requiring deep-seated understanding and proper habits of thinking and reasoning, and the student would recognize physics and mathematics as belonging to fundamental science, together forming the foundation of engineering.

Furthermore, the student who elects an engineering course has a natural interest in and zeal for his chosen field of work. The proposed plan would capitalize upon this enthusiasm by permitting the student to start his college course with work which he would recognize as possessing the essential properties of engineering, instead of feeling that he must wait for about two years before starting work of this character. Not only would his interest be greatly enhanced thereby but aptitudes would be discovered at an earlier date.

Under the present plan a student may come to graduation without full appreciation of the unity that exists throughout all of his work. Relationship between courses, while an interesting speculation, may never be fully understood. Individual courses are frequently viewed as something which must be passed and thus "gotten out of the way" before more advanced work can be undertaken.

The question might be raised as to whether certain important educational losses would not be sustained through the introduction of such a plan. There is no doubt that the study of mathematics and physics as pure science can have great value to the engineering student. The very nature of these subjects gives the student a broad philosophy and viewpoint with respect to science which it is difficult to develop in engineering courses because of their limitations. These values need not be lost but might even be increased by proper teaching of the co-ordinated work. To accomplish this, great care should be taken to insure that the student is made to fully appreciate the *complete unity* and the *simple beauty* that exists in natural law. Co-ordination of science subjects would give a better opportunity to develop a more complete understanding of the underlying correlations between the various elements of science. Respect and esteem for the unity, beauty, and truth in natural law would be enhanced by a fuller and deeper appreciation of the interrelationships and parallelisms which constantly present themselves.

Considering the co-ordination of mathematics and physics as the first step, the second step in our proposed program would be to unify and co-ordinate all electrical-engineering courses themselves. These subjects can all be brought together into one closely woven whole and developed as one broad course from the initial elementary work to the time of graduation. Laboratory and classroom courses should not exist separately, but each division of the work should receive

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Paper number 38-76, recommended by the AIEE committee on education, and presented at the AIEE summer convention, Washington, D. C., June 20–24, 1938. Manuscript submitted May 2, 1938; made available for preprinting May 25, 1938.

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investigation in the laboratory at the same time it is being discussed in the classroom. Thus theory and practical understanding would be developed together in a logical manner. Comprehensive knowledge and understanding would thus be obtained as a result of thorough investigation from different points of view.

The third and final step required to complete our program of unification would be the bringing together of the co-ordinated work in mathematics and physics with similarly organized work in electrical engineering. The problem here is to make the student realize that he has not completed a course which can be left behind and forgotten, but rather that he is going forward into more advanced work which is a continuation of what has gone before requiring constant use of the principles he has learned. In order to carry this out with smoothness and harmony, instructors in each department should be thoroughly familiar with that portion of the work of the other department which lies close to the transition point. Even with the best co-operative spirit on the part of the faculties concerned it will take considerable time to plan such a program and effectually put it into operation. Many difficulties will exist in any institution, but no worth-while accomplishment in education is easy of attainment.

At Worcester Polytechnic Institute the previously outlined general plan is well underway for the electrical-engineering curriculum. The order in which the separate steps were undertaken has been somewhat different from that outlined herein. Our initial effort was devoted to co-ordinating thoroughly all of the undergraduate work given by the electrical-engineering department. The curriculum was completely revised four years ago in order that this work might go forward and the present senior class is the first to graduate under the new plan. There is unanimous agreement on the part of the department staff that the results show marked improvement in the students' comprehensive knowledge and general understanding.

Our second step is to co-ordinate the work of the physics and electrical-engineering departments. The problem has received much discussion and is being attacked with a fine spirit. Small groups composed of members of both departments are now working together on the many details. It is realized that if lasting good is to be accomplished much care and attention must be given to every step. In order to help the student appreciate the union between physics and electrical engineering the staff of the latter department will stress the importance of work in physics by frequently referring back to principles previously covered in physics courses, thus emphasizing the continuity. Similarly, members of the physics department will attempt further to awaken student interest by pointing out practical applications of the principles taught by them.

The objection may be raised that the amount of ground covered in a co-ordinated curriculum of this nature necessarily would be less. The answer is that the student's knowledge, understanding, and power of analysis would be increased. The measure of a college education never was, and never should be, the number of items covered in the separate courses of the curriculum. Many years ago the college with which I am associated adopted as a motto "Pauca Fideliter"—a few things faithfully. As we look forward to greater accomplishment in engineering education we must break down the barriers between the individual subjects so that the unity which exists so abundantly in nature shall permeate the separate parts and consolidate them into one complete whole.

Discussion

Royal W. Sorensen (California Institute of Technology, Pasadena): This symposium comprising the several papers has, indeed, been interesting and very instructive. It is so complete as to leave little that may be said in the way of conclusions; for, after all, all of us assembled here are not only engineers, but are professional teachers. We have, or should have, attained this professional standing by virtue of being men who can discern and devise our own ways of finding and overcoming students' impedimenta to learning. I don't agree with the ideas which some have presented, that the best teaching is always the one which makes learning easy. In fact the lifetime work of an engineer is that of doing new things. Invariably starting friction is greater than running friction. A future engineer should, therefore, while he is a student, learn to avoid being dismayed by heavy starting friction: that is he should, at least a part of the time, have the stimulus of unraveling problems in his own way even though that may be a very difficult one. Naturally as a teacher I try my best to present subject matter in such a way as to be readily understood; but I am not overly exercised if the students find the explanations difficult to understand. Will not these men, as they go out into practice after graduation, often be placed in the position of having to take instruction from men difficult to understand? And is it not a part of their business as students to learn how to interpret difficult and sometimes befogged instructions?

Reference has been made to poor instruction in high schools in science and mathematics. I am of the opinion that the present high-school graduates who enter our engineering schools with good entrance requirements, are today better prepared in science and mathematics than was the case a score of years back. Are we not prone to judge preparation by what we require rather than by actual standards of today as compared to those which prevailed a generation ago?

The California Institute of Technology has for several years recognized the expanding scope of engineering and has included a large amount of humanities in its courses. This fact combined with the expanding field of engineering technique has made it seem wise to grant for four years work the degree of bachelor of science in engineering, rather than specifying a particular branch of engineering. At the present time, by doing an additional year of work, our students may obtain the degree, master of science in some special branch of engineering, such as electrical, mechanical, or civil. Our meteorology and aeronautics departments, however, have deviated from this plan, and at the end of the fifth year are granting the degree, bachelor of science in aeronautical engineering or in meteorology as the case may be. My years of teaching experience have led me to look upon the four-year engineering courses as dual-purpose courses—the major purpose, perhaps, being that of a general education rather than a professional education, the professional education to be obtained by added years. With this thought in mind I do not advocate extending what may be known as the regular engineering courses to five-year or six-year courses, but I rather think it better to have our present terminal, four-year courses and add thereto such work as seems desirable for engineering preparation. Undoubtedly this means that an increasing number of men will spend at least five and preferably more than five years in college work in preparation for an engineering career. Such a program may introduce a dangerous situation if we encourage men to go on beyond the fifth year for a doctorate degree and use for courses leading to the doctorate degree, simply additional technical work of the type which has been given in our undergraduate courses. Men selected and encouraged to go on for doctorate degree courses should be limited to men who have outstanding analytical ability and who can without difficulty master advanced courses in mathematics and physics. In fact a large part of the graduate work leading to a doctorate degree for engineers should, in my opinion, be work done under the instruction of our ablest mathematicians and physicists.

My own experience with younger teachers, in fact even with men pursuing graduate work, used as teachers, has been most excellent. By carefully selecting the men who are to do the teaching and introducing them to the work first through laboratory courses and then in the classroom, we find the results equal to and frequently better than the results sometimes obtained by older teachers. This is no doubt due to the great enthusiasm of the young men who wish to try teaching, coupled with their recent contact as young undergraduates with the problems as they appear to the undergraduate.

Returning to the undergraduate courses, I question very much the advisability of including in the first four years of work, courses in transients, long transmission lines, and higher mathematics courses, because I think, for the large group of students, the first four years of work can best be spent in acquiring training in the simple fundamentals of science and engineering plus a considerable amount of general cultural education including such subjects as English, public speaking, history, economics, geology, etc. This thought, I suspect, agrees with Professor Malti, who suggests that we have all graduate students working for a doctorate degree take mathematics and physics as minors. With this I agree, except that the minor should comprise a large part of the graduate students' course work. Professor Malti seems to recommend the appointment of men who have just received their doctorate degrees as instructors. I think it would be much better to have such men enter industry for several years before being selected as instructors. Malti's comments regarding ability to do research as a prerequisite to teach, and regarding having the faculty keep in touch with industry are pertinent.

E. M. Strong (Cornell University, Ithaca, N. Y.): For something over ten years I have met nearly all of the students in engineering at Cornell as they come fresh from mathematics and physics to begin their study of electrical fundamentals in the sophomore or junior years. The place and nature of mathematics and physics in the engineering curriculum continuously interests me and I am appreciative of the cross section of current thought provided by this symposium.

I do not believe that we need here to sell the idea that mathematics and physics have in the electrical curriculum a place which is both important and steadily growing. Anyone really interested in the symposium is alive enough to be well aware of this. What he wants to know is "How can the faculty and curriculum best function to accomplish the admitted objective?"

I am surprised that no one has mentioned the economic factor in the situation because with few exceptions it is a most potent element in the whole program. Possibly there is something in the local atmosphere here which taboos recognition of economic restrictions. There are several angles to the economic problem. Let me mention two of outstanding importance:

1. The use of instructorships to subsidize graduate study and research.
2. Insufficient supervised computation.

In physics and mathematics particularly we find too many young men holding instructorships who have no interest in teaching except as it alleviates the immediate financial stress during their graduate study. Sometimes they seriously lack essential qualifications for teaching and especially for the teaching of the less mature student in the freshman and sophomore years who consequently functions as the "guinea pig" or the "goat" for these "instructors." Students during these years, more than at any time later, need the best *teaching* talent on the faculty. However good the curriculum and however good the administration it can produce results only in so far as the personnel which ultimately and *directly* contacts the student is competent not only in technical knowledge but also in ability to impart that knowledge and to inspire enthusiasm in the student. No man can do this except that he be genuinely interested in the student—he cannot do it if his mind is filled with graduate study or research to the exclusion of all else in the mad scramble for a degree by June 1.

The problem is largely an *economic* one for faculty and graduate student alike. I have no solution to offer.

Returning now to item number 2 (insufficient supervised computation). Engineering faculties are confronted with an increasing difficulty in obtaining from the public schools of the country any dependable foundation in mathematics—let alone physics or chemistry or habits of study and conduct. In college the departments of mathematics and physics lack facilities for building up the student in these deficiencies. One of the most successful methods employed in engineering instruction is the computing period which provides for the student a time and place to develop what we call engineering habits in the approach to problems and their solution—not only time and place but the sympathetic supervision which makes for more efficient work in the earlier years, in particular, than can be found in home study alone.

The problem is primarily an *economic* one to provide the required facilities of staff and room.

There is one other phase of the general problem about which I am moved to comment. I am fully in sympathy with any of the true co-operative movements which some of the authors have outlined for us. However, there are two kinds of "co-operation." One of these is not found defined under the word itself in Webster but is formally defined under "coercion." Engineering says to physics: "Your work isn't giving our students the foundation we need—will you co-operate in a revision?" "Yes, of course we'll co-operate." "Well, here's what we want—so and so and thus." "But I can't teach that, it doesn't do justice to physics." "Well, if you don't want to co-operate, we'll give it ourselves." Whether this work is then taught in engineering or in physics the result is deplorable.

Engineering does not go to English and dictate that only technical and scientific English be taught—nor to history for exclusively technical or scientific history. For the most part it hasn't had the face to go to language and insist on technical German or French, exclusively. If and when engineering in a university cannot co-operate with the rest of the university it might as well set up by itself as a technical school because any university affiliation is then not only unprofitable but possibly detrimental to its functioning.

I quite agree with Doctor Malti that these university departments of mathematics and physics must not be "bulldozed" into teaching their work according to rigorous engineering specifications. Let them have something of a cultural as well as the strict utilitarian purposes. Let them be supplemented or complemented by work in engineering, possibly concurrent in part, which will supply the point of view and the missing factors which no mathematician or physicist can be expected to provide in good faith with his own profession.

Again the question is an economic one—can it be done without increase in facilities?—can it be done in a four-year program?

M. L. Manning (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The paper by M. G. Malti is well written and offers among other things valuable suggestions for a proposed program of engineering teacher training.

I should like to contribute to this plan of engineering teacher training from the standpoint of the practicing engineer.

For selected teachers of engineering not only graduate work equivalent to a master of science degree, or better, with minors in mathematics and physics would be desirable but also following this work with at least a three- to four-year program of practical experience in industry leading to a doctor of philosophy degree. Since engineering is about three-fourths common sense a practical basis is necessary. This experience should be in design work followed by research and development problems. In this way a not-too-theoretical frame of mind is formulated but attention is also focused on the economics of the problem. The lack of this training is often so noticeable when former teachers of engineering enter into practical work. In setting up a problem the mathematical or physical beauty so engrosses the thought that all sight is lost of the practical side or how the results are to be obtained in the laboratory.

For an example, let us consider the response of a three-mesh transformer network to an impressed impulse voltage wave. Two thoughts are uppermost in the transformer design engineer's mind; namely, the initial and final or steady-state solutions. In the intricacy of the solution, it is easy to overlook these two important practical considerations. Because of the oversight results are sometimes expressed which lead to incorrect conclusions.

If the teacher were trained to recognize such important facts by experiencing them in industry, by actually designing and following the manufacture of electrical machinery, by obtaining a knowledge of the dollars and cents part of the picture, and by noting the attitude of industry toward these problems and toward educational work as a whole, a decided improvement in the quality and relationship to industry of teaching would be noted. An attempt should, of course, be made to make the equalization balanced; that is, not to lose sight of the educational as well as the industrial processes simultaneously.

It might be argued that such a program is difficult to fulfill, but it seems to the writer a staggered plan could be formulated. Due to the uneven keel of industry at times the absorption of this teacher training by industry would vary but over a period of time a program could be devised.

For an example, let us consider the graduate program of the Westinghouse Electric and Manufacturing Company as a suggested training course. The work is co-ordinated at all plants. This means a student or engineer at the South Philadelphia works may receive graduate credit for a subject if he is transferred to the East Pittsburgh works, or to the Sharon works, etc. During the past year over 1,500 engineers and students participated in this plan and about 600 received graduate credit under the University of Pittsburgh—Westinghouse arrangement. A comprehensive program was also organized in the transformer division, the Sharon works. The average attendance for the courses was 92 per cent. The subjects included "differential equations for electrical engineers," "symmetrical components" and a well-organized transformer course. This latter course consisted of 31 sessions followed by comprehensive examinations. The classes were conducted by members of the transformer engineering department who were specialists on the particular topics studied. These topics were from one to five sessions in length. The course content follows:

1. **Fundamentals (five*).** The general differential equations, transformer vector diagrams, physics of the transformer, etc.
2. **Practical distribution transformer design problems (two).**
3. **Fundamental considerations of reactance and stray losses (two).**
4. **Iron losses (two).**
5. **Magnetic forces (one).**
6. **Magnetic noise (one).**
7. **Radio interference (one).**
8. **Insulation (two).**
9. **Heating and cooling of transformers (two).**
10. **Condenser bushings (two).**
11. **Tap changers (two).**
12. **Inertaire equipment and oil filters (one).**
13. **Instrument transformers (one).**
14. **Distribution transformers (one).**
15. **Power transformers (one).**
16. **Constant-current regulators (one).**
17. **Preventive autos—series transformers (one).**
18. **Three-winding transformers (one).**
19. **Current-limiting reactors (one).**
20. **A-c welding transformers (one).**

This course is mentioned only to suggest how other courses in electrical apparatus might be outlined for a program of engineering teacher training.

The method of presenting engineering problems as proposed in the paper by B. R. Teare, Jr., is commendable. The fact must not be overlooked that self-education at least in part is the only practicable way in which most students can acquire higher mathematics and physics. Most students fail to grasp the concept of expressing physical relations in mathematical form. The technical student often finds, upon entering a class in engineering, that he has to deal with mathematics under a new form. The particular engineering subject he is studying must be translated into mathematical terms and this causes difficulty. The sense of perspective is often lacking. This can be developed as the student becomes more mature provided he makes actual use of mathematics.

Skill in the use of mathmatics is the really essential thing. Use of arithmetic with algebra, perhaps, or a simple diagram often leads to more satisfactory results than others secured through elaborate processes involving lengthy equations and complicated operations. In the latter, errors are apt to occur, and the common-sense part of the problem is likely to be overlooked. Assumptions may be made to facilitate calculations which are physically unwarranted, as one loses sight of the physical problems in the intricacy of the mathematical solution. Abstract mathematical studies, if pursued as a kind of intellectual exercise, may produce a pure mathematician, but these studies may unfit a man for practical engineering. A tool expert is not necessarily a successful manufacturer, nor is a mathematician necessarily an engineer.

Mathematics is used in engineering to express the quantitative relations of natural phenomena. The mathematician delights in the relations. He may divorce them from the phenomena and give them abstract expression. The engineer is concerned with the natural phenomena and demands the physical conception. The medium of expressing these relations is of secondary consequence.

In summarizing, the engineer is concerned with applied mathematics. The ability to state a problem, to recognize the elements which enter into it; to see the whole problem without overlooking some important factor; to use good judgement as to the reliability or accuracy of the data or measurements which are involved and the ability to interpret the result; to recognize its physical significance; to get a common-sense perspective of its meaning and the consequences which may follow; to note the bearing of the various data on the final result, such abilities as these are of higher order than the ability to take a stated problem and work out the answer. It is this sort of judgment and insight which makes mathematics useful.

We should emphasize not the subject matter nor the students but the method of teaching. Aside from the imparting of knowledge and technical ability, the teaching of the use of mathematics and physics in engineering problems gives opportunity for training in the use of logical methods, and in the drawing of intelligent conclusions from unorganized data which will make efficient men, whether they follow pure engineering or semitechnical, or business pursuits. Such teaching does not come from the textbook, it comes from the teacher. He must be in sympathy with engineering work and have a just appreciation of its problems and its methods. He must be imbued with the spirit and the ideals of the engineer.

M. S. Coover (Iowa State College, Ames): Professor Curry has presented an admirable discussion on the means of co-ordinating the three major component parts of training for engineers and more particularly electrical engineers. His paper reads very much as I would hope that mine might read if I were called upon to expand the objectives as they have been written just recently for the curriculum in electrical engineering at the Iowa State College.

The problem that concerns me most is the crowding that seems to be so necessary in order to stay within a normal four-year program. The subject matter of a majority of courses is assigned to the student in such rapid sequence that even the better-than-average student finds it difficult to make a really satisfying useful assimilation either from his own viewpoint or that of his instructor. This problem may not be so serious for these institutions who have the opportunity to hand pick their students, but it is a problem the solution of which is not so easy for publicly supported institutions.

At the college which I represent there is a growing tendency on the part of the truly conscientious student to realize the value of the best training that he can get if he is to reach a satisfying place in this highly competitive social and scientific age. Accordingly, we find more engineering students taking advantage of our summer school offerings in order to lighten the number of credit hours during the winter months, thereby making available more time for the remaining courses. By this process he is spreading his training and mental efforts over the equivalent of five academic years within the space of four calendar years.

If we as teachers are intending to give to the student the best that it is possible to give him we should not only make every effort closely to co-ordinate our own work within a given department but as Curry points out, maintain a closer personal relationship among the several departments concerned.

Malti has done a good job of stating the processes involved in the handling of engineering problems from the purely technical viewpoint.

He makes the statement that something seems to be wrong either in our teaching of mathematics and physics to engineers or in the proper co-ordination of these subjects with engineering courses or both. What serious-minded student, teacher, or engineer has not entertained similar thoughts from time to time?

No doubt some of the reasons for these undesirable situations are present in various degrees in most educational institutions. Some of the remedies which Malti suggests are good. Physicists and mathematicians cannot help but improve their teaching of engineering students if they will take a closer interest in engineering and engineers. Since mathematics and physics combine to form the foundation upon which the engineering educational structure is to be built, co-operation among those concerned is essential if the structure is to be useful.

The desirability of competent teachers with an adequate education cannot be disputed. However, a man with an armful of degrees is not necessarily the best teacher. The individual is more likely to be a good teacher if he has had a reasonable amount of responsible commercial or industrial seasoning. If he has demonstrated his ability, he is more likely to know his subject better, to appreciate its significance, and therefore, more likely to be enthusiastic and inspiring, thus developing and stimulating the student's intellectual curiosity. Simply to specify a certain type of academic training for the teacher in order to qualify for an appointment is most likely to lead to what has been cited as one of the prevalent ills. If, for the sake of argument, it is granted that we do not now have good engineering teachers, how can the present teachers educate better ones along the lines suggested?

On the matter of co-operation between and among the departments of physics, mathematics, and electrical engineering, I venture with apologies to cite what we are doing at the Iowa State College for the reason that I am not familiar with what is being done along the same line at other institutions, but in so far as I am informed it is with but one exception practical only at those institutions where say the departments of physics and electrical engineering are under one and the same headship. At the Iowa State College, each of the three departments under discussion is administered separately. The instructor in the mathematics department who is assigned to the instruction of electrical-engineering students in differential equations teaches one section of junior electrical-engineering students in a-c circuits in the electrical-engineering department; exchanging with one of our own staff members who teaches one section of differential equations to engineering students in the mathematics department. It goes without saying that close co-ordination thus becomes a requisite. A similar arrangement is practiced between the departments of physics and mathematics and is being developed between the departments of physics and electrical engineering, thus closing the triangle.

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*Number of sessions.

On another point, I believe that physics and mathematics are chosen almost invariably as minors for work leading to the degree of doctor of philosophy with a major in engineering.

Albert A. Nims (Newark College of Engineering, Newark, N. J.): The co-ordination about which we are talking is well illustrated by the papers themselves. Professor Bryant refers to the lack of co-ordination in his student days among the terms and methods used to explain the quantitative relationships in electric circuits, and the performance of the different varieties of electrical machines. The present situation is reflected by Professor Curry, who refers to "training the student . . . . to search for the fundamental principles upon which a statement of the problem may be formulated . . . . since subsequent engineering courses will utilize the same principles." It might well be said that the same search should be constantly in the mind of every teacher who wishes to increase his professional effectiveness.

One of the most promising ways of increasing the efficiency of our teaching methods is so to organize our subject matter that the number of new concepts to be mastered is a minimum, and the usefulness and applicability of each is a maximum. Some indication that this search for common denominators is making progress is found in Professor Teare's references to "the analogous behavior of electric circuits, mechanical systems, and heat conduction systems, expressed in exactly similar equations," and "to the broad analogy between the space fields of electrostatics, magnetostatics, gravitation, thermal and electric conduction, and fluid flow, described by similar vector equations and identical field maps."

Not too much can be expected of this ideal simplification, however. Professor Strong has referred to the practical considerations which intrude themselves into every theoretical plan, no matter how ideal, and has mentioned the economic factor. Professors Bryant and Coover have referred to the time factor which creates a continual pressure for increased efficiency in presentation of subject matter. Another practical consideration is the mind of the student himself, which sets a practical limit to the process of searching for common denominators.

Highly generalized statements are of little use as an initial approach to the phenomena of electrical engineering. The increments of knowledge by which a new subject is mastered are limited in size. The assimilable increments vary in size with each individual, but, with our mass production methods, some working average must be used. Teaching efficiency is likely to be a maximum when the number of fundamental concepts is such that each can be acquired in the minimum number of practical steps, and each will be of the widest use after it has been mastered.

Professor Morgan has pointed out that instruction is more efficient when "laboratory and classroom courses do not exist separately; each division of the work should receive investigation in the laboratory at the same time that it is being discussed in the class room." Professor Strong has referred to the effectiveness of computation exercises for driving home fundamental relationships in the minds of the students. Computation might well be made a third approach, co-ordinate with classroom discussion and laboratory exercises. "Slide-rule pushing" and "graph plotting," when properly organized and motivated, can be potent tools with which the student can reveal to himself relationships that otherwise he might not grasp. One of the comments most frequently made about the course in winding specification and performance computation offered at the Newark College of Engineering under the name of electric machine design is to the effect that "you certainly know what goes on inside a generator after you have figured out one of those designs."

Computation is a kind of laboratory work that requires very inexpensive equipment. In fact, it is about the only kind of laboratory exercise one can have in a mathematics course. At the Newark College of Engineering at least one computation exercise per week is scheduled in the second-year calculus course. The project method, based almost entirely on computation, is the procedure adopted for studies of networks, and of transient phenomena in the junior year.

The one-semester course in transients is the outgrowth of a brief theoretical discussion given in preparation for some laboratory exercises. It naturally involves considerable work with differential equations of a few varieties. The interest aroused by this course, together with that generated by the discussion of tensor analysis in the technical press, has resulted in the scheduling of two optional after-hour courses for the juniors. One deals with differential equations for one semester, and the other with vector analysis for an equal length of time. The optional and after-hour features operate automatically to select the abler students. The courses are conducted by the head of the mathematics department, who makes no claim to being an engineer. The method of presentation involves a minimum of manipulative theory and a maximum of applications, such as engineers are constantly meeting. The results of these courses seem to be twofold: an increase of mathematical interest and understanding on the part of the students who take them, and an increase of engineering interest and understanding on the part of the teacher.

Wm. A. Del Mar (Habirshaw Cable and Wire Corporation, Yonkers, N. Y.): Speaking as an engineer to an assembly composed chiefly of teachers, I can pay my compliments to the teachers of mathematics and say that they have not sinned so much as been sinned against in being forced to expend their energies on refractory material.

It is quite possible that a genius of a teacher may be able to make a mathematician of almost anyone, given ample time to work with him, but in college education we are concerned with mass production and a time must assuredly be reached when the teacher realizes that certain students are by nature mathematically inept, as far as mass education is concerned. Such men should not be forced to drag their classes and develop inferiority complexes by being either flunked or allowed to proceed with nothing but a memorized knowledge of data and standard processes.

It should not be difficult to deflect into other activities men whose minds run against a blank wall in trying to express physical problems in mathematical terms.

Is it not the failure to do this that gave rise to the need of this symposium?

W. A. Curry: Discussion has added a number of valuable suggestions and viewpoints on this matter of co-ordination and confirms the importance and interest which this problem holds for all who are concerned with electrical-engineering curricula. It would appear that we may attempt to achieve this objective in a variety of ways, and this diversity indicates that there is no single, royal road to its realization. Only by continued vigilance can an effective co-ordination be attained and maintained through the changing years.

M. G. Malti: I should like very much to endorse Sorensen's ideas regarding contact with industry. I have had the good fortune of being a consultant for a manufacturing concern over a period of several years and the pleasure and stimulus which this contact with industry has given me have been reflected both in my teaching and writings. Every trip I made to the plant and every summer I spent in it have given me more insight into the practical applications of my specialty which I have conveyed to my students. It is extremely desirable, therefore, that graduate students keep in constant touch with industry either through consultation or through regular employment. Whether such men would return to teach *as instructors* after they have been in industry, for some time, is questionable.

Strong's stress on the economic problem is misplaced. The fact that a graduate student takes an assistantship to enable him to do graduate work does not necessarily imply that he makes a poor teacher. Nor is it true that a researcher is invariably absorbed by his research to the exclusion of everything else. I agree that instances do occur where a graduate student has turned out to be a poor teacher. This, however, is a fault of the individual concerned rather than of the system of using graduate students as assistants. As to computations, I am positively in favor of them and have used them in connection with my courses. But I attempt to make my computation not "slide-rule" or "formula-substitution" problems but rather problems which stimulate thought. Computing periods should, in my opinion, be periods in which the habit of straight thinking is required under competent supervision.

Nims seems to favor "slide-rule pushing" and "graph plotting" as showing the student "relationships which he otherwise might not grasp." I am sure the "graphs" themselves do reveal such relationships but I question whether the actual plotting of these graphs does! In order to illustrate what I mean let us assume that one laboratory course is run by two different instructors as follows:

Instructor A requires his students to run the equipment, take data, and when the experiment is over gives his instructions as follows:

(*a*) Compute only one point on each curve or perhaps only one curve, thus reducing slide-rule pushing to a minimum.

(*b*) Blue prints of all curves are given to each student. You *do not have to plot any curves* but!

(*c*) Give a complete and adequate discussion of the why, how, wherefore, of all the results that the curves reveal.

(*d*) Answer the following pertinent questions (which require thinking) regarding the results of your experiment.

This is the instructor who does not care to have his student waste time on curve plotting and slide-rule pushing. He would rather have them use their time in forming correct habits of thought.

Instructor B requires his students to run the equipment, take data, and when the experiment is over gives his instructions as follows:

(*a*) Compute *all points* on each curve (maximum slide-rule pushing).

(*b*) Make neat plots of *all curves* using india ink (maximum curve plotting).

(*c*) Discuss curves (by this he generally means say that this curve runs this way and that curve runs that way, which of course is obvious from the curve itself).

This is the instructor who requires "curve plotting" and "slide-rule pushing" and is the preponderant type in our engineering colleges.

Now I ask who turns out better engineers, instructor *A* or instructor *B*?

Del Mar puts his finger on the right spot when he states that our present plight is due to mass education in engineering. Now it so happens that other professions have solved this problem of mass education by limiting enrollment. This is true of the medical, dental, and legal professions. Engineering as a profession has, so far, not seen fit to take this step which, in my opinion, is the only solution to the problem.

Coover raises the question of how the present teachers can educate better ones if they are not so good themselves. My answer is that Coover begs the question. I neither did condemn nor even had the intention of condemning *all* engineering teachers. Thank God there are high-grade teachers scattered throughout this land. It is these who would and could educate better ones along the lines suggested.

Manning and I are in full agreement. The value of co-operation between industry and the universities is unquestionable. They both have something to offer to each other and a mutual exchange is extremely desirable. I should like to see a plan instituted whereby a large number of engineering teachers throughout the country are afforded a chance to spend their summers in industrial concerns. I also commend Manning's statements that mathematics and physics are valuable to the engineer not perhaps so much because of their subject matter but because of the training they afford him in logical reasoning and in correct processes of thought.

Self-Excitation of Induction Motors

By C. F. WAGNER
MEMBER AIEE

Synopsis: It has been known for some time that an induction machine whose rotor is driven mechanically may become self-excited if capacitors are connected across its terminals. The present paper is concerned with the predetermination of the machine characteristics when operating under such conditions. The frequency of excitation is very close to the synchronous frequency corresponding to the speed of the rotor. The voltage to which the machine will excite is dependent upon its no-load excitation characteristics at that frequency, the criterion to be satisfied being that the lagging volt-amperes of excitation equal the leading volt-amperes of the capacitors.

Under load, similar criteria must be satisfied. Voltage conditions are determined by a cut-and-try solution such that the summation of reactive volt-amperes equals zero. The slip is then obtained from the relation that the summation of the real power equals zero. These relations have been applied to various types of loads, such as pure resistance and inductive resistance, single-phase and three-phase and also to induction-motor load. Excellent checks between test and calculated results have been obtained.

The general impression appears to exist that for an induction machine to operate either as a motor or as a generator a source of alternating potential is always necessary in order to supply the excitation requirements. Such is not the case, for under certain conditions, an induction machine may supply power as a generator without a source of alternating potential, the magnetizing current being supplied by static capacitors. It is the purpose of this paper to discuss the circumstances under which such operation becomes possible.

The fact that such operation is possible has been known for some time, but very little has been written upon the subject until recently. The reason for this paucity of papers and articles lies in the relatively minor practical importance of the subject. However, the increased use during the past few years, of capacitors for power-factor correction, has placed a new aspect upon this problem. If the power supply to an induction motor is disconnected, the inertia of the connected rotating load tends to continue the rotation of the armature. The extent to which this occurs is dependent upon the nature of the load and in certain cases the armature may continue to rotate for seconds or minutes. In addition, applications are known in which gas or gasoline motors are connected to the same shaft with the induction motor and the utilization device, so that, in the event of the removal of the electric-power source, the armature can actually increase in speed and remain at the increased speed until manual readjustments are made. With capacitors connected across the terminals of induction machines which have been disconnected from the electrical source and in which the armature continues to rotate, the value to which the terminal voltage will rise due to self-excitation is dependent upon the speed, value of the capacitor, and load. With the regulatory function of the power source removed the terminal voltage may rise to dangerously high voltages—dangerous with regard to human life or dangerous with regard to insulation breakdown. Parallel-connected lights might also burn out with but a nominal increase in voltage. It may be seen, therefore,

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Paper number 38-106, recommended by the AIEE committee on electrical machinery and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 16, 1938; made available for preprinting July 7, 1938.

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The author wishes to acknowledge the assistance of R. P. Shimp in making the necessary computations incident to writing this paper.

1. For all numbered references, see list at end of paper.

that this problem has been removed from one of purely academic interest to one of practical importance.

In 1935, Bassett and Potter[1] presented test results showing the performance of generators under capacitive and combinations of resistance and capacitive loading. The present article considers similar problems and further shows that the performance under such conditions, including both balanced and unbalanced operation, can be precalculated quite accurately.

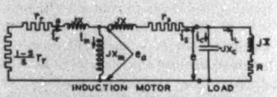


Figure 1. Equivalent circuit of induction generator with three-phase load of capacitors $-jx_c$ in parallel with induction load $R + jX$

With known conditions the characteristics of the machine can be predicted by the methods presented here.

General Considerations

The conventional equivalent diagram of the induction motor will be used with the exception that the magnetizing branch will not be regarded as constant but must vary in the manner dictated by the saturation characteristics of the particular machine. This circuit is shown in figure 1 for one phase. The symbols will have the following significance:

r_s = Stator resistance in ohms per phase to neutral
r_r = Rotor resistance in ohms per phase to neutral
x = Stator and rotor leakage reactances in ohms per phase to neutral
x_m = Reactance of branch representing magnetizing current in ohms per phase to neutral
s = Slip expressed as a fraction of synchronous speed

The slip is referred to the frequency of the stator voltages and currents and, when operating as an induction generator, is, of course, negative.

The general considerations which will be applied in determining the solutions are that the summation of the real power and the summation of the reactive volt-amperes throughout the entire circuit, including that of the load, must each equal zero. Because of the presence of saturation phenomenon, resort will be had to a graphical or a cut-and-try method of solution.

Most of the tests which are described in what follows were made on a 15-horsepower, three-phase 60-cycle 220-volt 1,770-rpm type *CS* Westinghouse motor of conventional design. The constants, obtained from test data, are

r_s = 0.151 ohm per phase to neutral
r_r = 0.145 ohm per phase to neutral
x = 0.394 ohm per phase to neutral
x_m (air-gap line) = 19.8 ohms per phase to neutral

The no-load saturation curve is given in figure 2. The lower portion of this curve, indicated by circles, was obtained by impressing a three-phase alternating voltage to the machine terminals and varying the speed by means of a connected d-c motor so that the power input from the a-c end was zero.

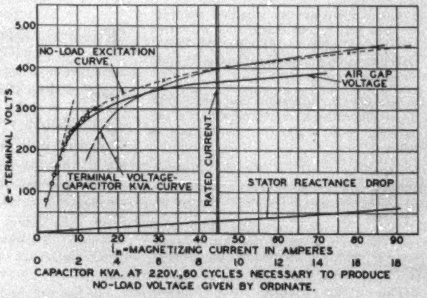
No-Load Excitation

A three-phase bank of static condensers was connected (without the a-c supply) across the terminals of the machine and the rotor driven at a speed corresponding to normal synchronous speed by means of the d-c motor. Since only the losses of the machine must be supplied, the quantity, $\frac{1-s}{s} r_r i_r^2$ (figure 1) which represents the shaft input, will be very small. This condition requires that s be very small and consequently that $\frac{1-s}{s} r_r$ be large and i_r small. The equivalent network for this operating condition for all practical purposes reduces to that shown in figure 3 in which r_s of figure 1 has also been neglected. Because s is small the generated frequency corresponds to that of the shaft which in this case is normal frequency. Upon varying the magnitude of the capacitors the test points, indicated by crosses in figure 2, were obtained, extending the no-load excitation curve to more than ten times normal excitation current. It will be observed that over the portion of the range for which the data overlap, the test data checks with that obtained by the application of an alternating voltage. Referring to the equivalent circuit of figure 3 it may be seen that the ratio $\frac{e}{i_m}$ for any point on the no-load excitation curve must equal $(x_m + x)$ or x_c. In order that the reactive kilovolt-amperes sum up to zero, x_c must equal $(x_m + x)$. Thus the slope of a straight line drawn from the origin of the curve of figure 2 to any point on the curve gives the capacitive reactance of the capacitor required to produce that voltage at no load. As the condenser capacity decreases the slope representing its capacitive reactance increases and the terminal voltage decreases. When finally the slope equals the air-gap line, an infinite number of solutions are possible. Beyond this point the machine is inoperative. Therefore, there exists a certain minimum amount of capacitors which will still produce self-excitation. In this regard the induction generator performs in a manner quite analogous to a shunt-excited d-c generator.

Figure 2. No-load saturation curve of 15-horsepower 220-volt 60-cycle induction motor

o = motor connected to a-c source

x = motor excited through capacitors



In what follows it will be necessary to know the volt-ampere characteristics of the x_m branch alone. This is obtained from the dotted line in figure 2 by subtracting the xi drop giving the curve shown by the full line and marked "air-gap voltage."

A further demonstration of the truth of the above theory is offered by the manner in which the terminal voltage changes with change in frequency. The full lines in the insert of figure 4 show the magnetizing characteristic and the capacitor characteristics for normal frequency. As the frequency is increased the terminal voltage for a given magnetizing current increases proportionately with the frequency. On the other hand, the capacitor current varies inversely proportional to the frequency. These characteristics are shown by the dotted lines for an increase in frequency of about 20 per cent. The new terminal voltage is then given by the intersection of the dotted lines. The comparison between test and calculated results using this method is shown by the circles and crosses in figure 4, which indicate a very close agreement.

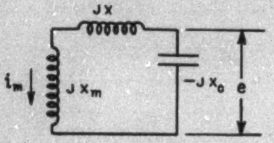


Figure 3. Equivalent single-line diagram for no-load excitation condition

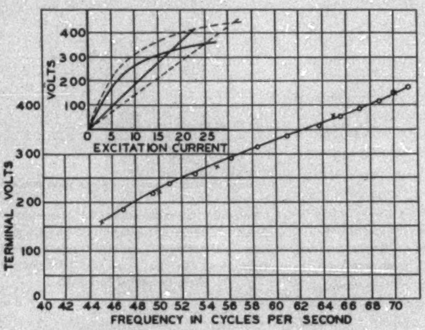


Figure 4. Effect of change in frequency

No load—constant shunt capacitor = 10.6 kva at 60 cycles

o = test points

x = calculated points

In order to provide a better quantitative perspective of the values involved, the dashed line in figure 2 was calculated. This curve gives the terminal voltage as a function of the kilovolt-amperes of the capacitors at normal frequency and voltage. It will be seen that for a capacitor whose kilovolt-amperes is equal numerically to the horsepower rating of the machine the terminal voltage reaches a value equal to twice normal. This will vary within limits for different motors, depending upon their excitation characteristics.

Three-Phase Impedance Loading

To determine the regulation characteristics under polyphase conditions, the induction generator was loaded with three resistance racks, which upon measurement were found to have 8.3-per cent reactance at 60 cycles. With a constant capacitor bank the load was changed keeping the load frequency constant with the result shown in figure 5. The circuit under consideration is shown complete in figure 1. To calculate the performance while keeping the capacitor fixed the following steps are involved. An arbitrary value of R and its corresponding value of X are chosen for which it is desired to determine the terminal voltage, e. Estimate the value of e. This fixes i_L and i_c. The current i_s is the sum of i_c and i_L. Knowing i_s the drop through $r_s + jx$ is found which determines e_a. The magnetizing current i_m is obtained from the full line of figure 2. And, finally, i_r is the sum of i_m and i_s. At this point all the currents are determined for the estimated values of e. All of those operations are, of course, vector operations. If the estimated value of e is the solution the following relation should be satisfied.

$$Xi_L^2 + xi_s^2 + e_a i_m + xi_r^2 - x_c i_c^2 = 0 \quad (1)$$

If, however, this summation is not satisfied a different value of e should be tried.

The comparisons between the test and calculated results for two values of capacitors are shown in figure 5.

The slip for this particular value of R is determined by summing up the power quantities for the solution obtained from the summation of reactive volt-amperes and equating this sum to zero. No cut-and-try method is necessary. Thus

$$\frac{1-s}{s} r_r i_r^2 + r_r i_r^2 + r_s i_s^2 + i_L^2 R = 0$$

or, combining the first two terms and solving

$$s = -\frac{r_r i_r^2}{r_s i_s^2 + i_L^2 R} \quad (2)$$

Values of s obtained by this method are shown in figure 5.

Calculated curves for pure resistance load and for the same capacitors are plotted in figure 6. It will be observed that the regulation is very much better. The series reactance in the load for the former case, although only 8.3 per cent, exerted considerable influence upon the terminal voltage.

An interesting condition exists for the range of operation indicated by the dotted lines in figure 6. Note that for this range there is no appreciable saturation so that x_m can be represented by a constant. It is possible, therefore, to determine the impedance of all that portion of the cir-

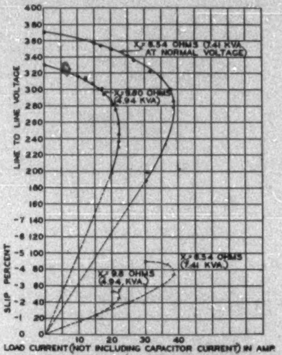


Figure 5. Characteristics of a 15-horsepower induction generator under three-phase resistance load having 8.3 per cent reactance

x = calculated points

o = experimental results

—— = terminal voltage

- - - - - = slip

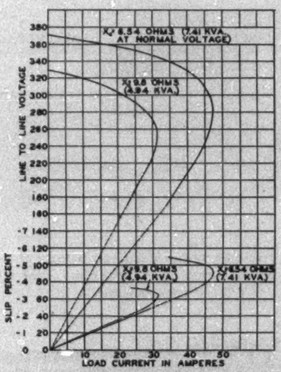


Figure 6. Calculated characteristics of a 15-horsepower induction generator under three-phase resistance load

—— = terminal voltage

— — — = slip

cuit in figure 1 which lies to the right of a. A solution exists when this impedance is purely real, there being no imaginary part. Since for pure resistance load of figure 6, X is zero, then R and $-jx_e$ in parallel become $\frac{-jx_eR}{R - jx_e}$ which will be designated by $R' + jX'$, in which

$$R' = \frac{x_e^2R}{R^2 + x_e^2} \qquad (3)$$

and

$$X' = \frac{x_eR^2}{R^2 + x_e^2} \qquad (4)$$

The impedance to the right of a then becomes

$$\frac{jx_m((R' + r_s) + j(x + X'))}{(R' + r_s) + j(x + X' + x_m)} + jx$$

The condition that the imaginary component of this expression equals zero, results in the following equation:

$$(x + X' + x_m)[x_m(x + X') + x(x + X' + x_m)] + (R' + r_s)(x_m + x) = 0 \qquad (5)$$

Upon substituting (3) and (4) into (5), a fourth degree equation in R results, which permits the determination of R. The quantity R is equal to $\frac{1}{\sqrt{3}}$ times the slope of the dotted lines in figure 6. The significance of this expression is that there are an infinitely large number of solutions for the terminal voltage when R has the value satisfied by this equation. Actual operation is impossible at any of these values as an attempt to operate on the straight part of the curve results in instability. For values of R slightly smaller than this critical value the machine loses voltage, is unstable. A self-excited induction machine cannot, therefore, supply a sustained short-circuit current. A similar expression can also be derived for the dotted lines in figure 5, the slope of which represents the limiting ratio of $\frac{\sqrt{3}\,e}{\sqrt{R^2+X^2}}$ which still permits operation as a generator.

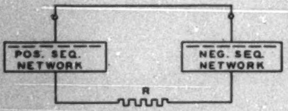


Figure 7. Method of connecting sequence networks

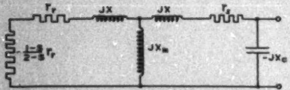


Figure 8. Negative-sequence network of induction motor with three-phase capacitor across its terminals

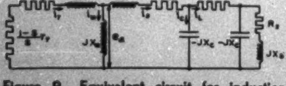


Figure 9. Equivalent circuit for induction generator and three-phase capacitor loaded single-phase by resistance R

Single-Phase Impedance Loading

For this case let it be assumed that a resistance R is placed across phases b and c of the induction generator, the capacitors still being connected across all three phases. This is analogous to the short-circuit of a three-phase system through a resistance R, a case treated frequently in the literature.[2] For this case the positive- and negative-sequence networks are connected together as shown in figure 7. The actual current through the resistor R is $\sqrt{3}$ times the positive- or negative-sequence current flowing through R in the diagram. This procedure tacitly assumes the justifiability of superposition in the presence of saturation phenomenon. It will be assumed that saturation effects influence only the positive-sequence network and not the negative-sequence network. The checks obtained by comparison between the calculated and test values justify these assumptions. The positive-sequence network of the machine is the same as has been considered previously in this paper and the negative-sequence network, with the exception of the capacitors, has been treated previously in the literature.[3] This network is reproduced in figure 8. Since s is usually small, a good approximation is to assume s equal to zero and to combine r_r and $-\frac{1-s}{2-s}\,r_r$ making the sum equal $\frac{r_r}{s}$. It will be assumed that x_m can be replaced by a constant term determined by the air-gap line. It is thus possible to replace the negative-sequence network of figure 8 by a simple impedance independent of saturation and slip. The resultant combined network for the positive- and negative-sequence is therefore that shown in figure 9; $R_2 + jX_2$ is the impedance to which the network of figure 8 has been reduced neglecting the impedance of the capacitors $-jx_e$. The current in the load is equal to $\sqrt{3}$ times the current in R and armature or load voltage is $\sqrt{3}$ times the voltage across R.

If the load is an impedance instead of a pure resistance, it is only necessary to replace R by $R + jX$ where X represents the reactance of the load. Tests and calculations upon the foregoing basis have been made on the previously used induction motor excited with capacitors across three terminals and loaded with a resistance rack across two terminals. The rack possessed 8.3 per cent reactance.

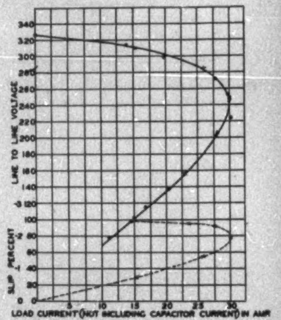


Figure 10. Characteristics of a 15-horsepower induction generator under single-phase resistance load having 8.3 per cent reactance and a three-phase capacitor equal to 10.3 ohms per phase to neutral (4.7 kva)

x = calculated
o = experimental
——— = terminal voltage
– – – – – = slip

The results of these tests and calculations are plotted in figure 10, which shows very close agreement.

If a capacitor is connected across the load only then the two capacitors $-jx_e$ of figure 9 must be removed and a single capacitor having the impedance of the actual capacitor must be connected across the load impedance.

Load Characteristics

Under certain conditions an induction generator driven by an engine or other equivalent motive power might have connected to it other induction machines which had also been connected to the source of supply. At the time of failure of the a-c source of supply the engine-connected induction motor tends to drive the other induction machines at a frequency determined by the speed characteristics of the motive power and at a

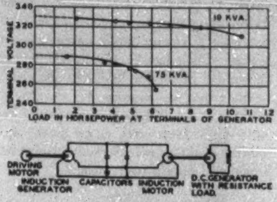


Figure 11. Self-excitation characteristics of two 15-horsepower induction motors, one of which is driven as an induction generator feeding the other as an induction motor. Figures on curves represent capacitor kilovolt-amperes at 220 volts, 60 cycles

voltage determined by the shunt-connected condensers. In figure 11 is shown a test setup to determine the characteristics of an induction motor when operating under such conditions. The setup consists of two of the above-mentioned motor-generator sets, the induction machines having the characteristics of those described previously. One of the d-c machines acted as a motor to drive one of the induction machines as an induction generator and the other induction machine drove the second d-c machine which was loaded upon a resistance rack. The two induction motors were connected in parallel and a bank of condensers connected across their terminals. Under this type of operation, theory dictates that the capacitors supply not only the excitation requirements of both induction motors but also the leakage reactance volt-amperes of the two machines. The curves of this figure show the characteristics at 60 cycles for constant values of capacitors as the load is increased on the induction generator. The no-load values of the voltage correspond to the values obtained previously for the no-load condition. It will be observed that the upper of these curves is much flatter than the lower one. The reason for this is apparent when it is considered that as the load is increased, the increase in leakage reactance volt-amperes of the machine must be supplied from the decrease in volt-amperes required by the excitation. At the higher voltages produced by the larger condenser capacity the machine is operating at a higher saturation and, therefore, more reactive volt-amperes is released for a given change in excitation voltage.

For the particular values of condenser capacity used in these tests, it was necessary to bring the driven induction motor up to speed by its d-c machine in order to make the sets excite themselves and carry load. However, at the no-load condition discussed previously, the machine came up to voltage very rapidly provided there was sufficient capacitor kilovolt-amperes to provide the excitation. In the loaded case, the capacitor kilovolt-amperes was too low to supply both the excitation requirements and the leakage reactance volt-amperes of the machine. Subsequent tests in which a five-horsepower 550-volt induction motor was used for the load both sets were excited very nicely by either bringing the two machines up to speed simultaneously by the one driving motor or by first exciting the induction generator and then connecting the five-horsepower machine. The question merely resolves itself into one of whether a given capacitor can supply both the excitation kilovolt-amperes of the generator and the starting lagging kilovolt-amperes required by the stationary motor.

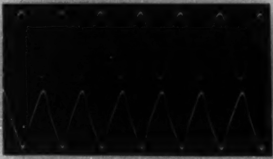


Figure 12. Wave forms for a 15-horsepower, 220-volt three-phase induction motor operating at no load and excited by a five-kilovolt-ampere static capacitor

Upper curve—terminal-to-terminal voltage = 320.4 volts

Lower curve—armature current = 18.3 amperes

Wave Shape

Oscillograms of terminal voltage under either no load (figure 12) or load (figure 13) indicate a very close approximation to sine wave. The current wave forms are distorted to some extent.

Conclusion

Calculations have been made for several conditions, both at no load and under load and the results agree closely with the test results. These checks include both balanced and unbalanced operating conditions. It is felt from this that, given the motor characteristics, the self-excitation effects can be predicted in advance to the same degree of accuracy as that of the information.

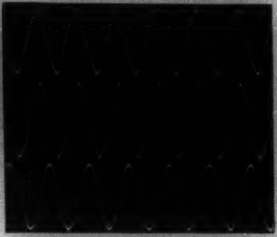


Figure 13. Wave forms for a 15-horsepower 220-volt three-phase loaded induction motor excited by static capacitors

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Use of Bismuth-Bridge Magnetic Fluxmeter for A-C Fields

By G. S. SMITH
MEMBER AIEE

THE methods most often used for the measurement of d-c, a-c, and transient magnetic fields depend upon one fundamental law: that a voltage is generated in a conductor cutting lines of magnetic flux. However, for each of these fields the apparatus and procedure are entirely different. Perhaps the greatest difficulty arises in the study of a magnetic field under transient conditions, since the resulting voltage oscillogram is a composite of several components having more or less intricate phase relations. Thus the actual magnetic-field values or changes are rather difficult to obtain. A single instrument, adapted for use in any of the three fields, would be very desirable. With this object in view, the following report describes the work done in attempting to use, for the measurement of a-c and transient magnetic fields, an instrument previously proposed for use only in the d-c field.

Analysis of Problem

In a previous paper[1] an instrument for measuring d-c magnetic fields was described which gives a continuous and steady reading when placed in the field to be measured. Its operation depends upon the change in electrical resistance bismuth exhibits when placed in a magnetic field. Two bismuth resistors are placed in the opposite arms of a wheatstone bridge and in zero magnetic field the bridge is accurately balanced by using some ordinary resistance material in the remaining arms. When placed in a magnetic field the bridge is unbalanced by an amount depending upon the effect of that field upon the bismuth resistance. Thus the current or voltage due to this unbalance can be calibrated directly in terms of the magnetic field density. To avoid changes in the zero setting of the meter, due to temperature variation, all resistors in the bridge are adjusted to have the same temperature resistance change.

During the investigation covered by the previous paper the meter was tried in an a-c magnetic field and a definite reading was obtained, but no quantitative tests were attempted at that time. By substituting an oscillograph vibrator in the fluxmeter position in figure 1, the oscillogram shown in figure 2 was obtained, indicating that the meter could also be adapted to transient-flux measurements. This report describes the work done in altering the design of the same instrument so that it can be calibrated for use in a-c magnetic fields, or used for transient-flux investigations.

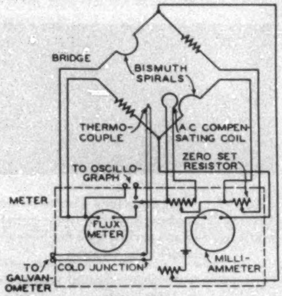

Figure 1. Wiring diagram for magnetic fluxmeter and bridge

The fluxmeter current shown by *A* in figure 2 is a composite of two current components a portion of which has been sketched in on the oscillogram. Although they have not been drawn to exact scale, it can be seen that the pulsating component which remains positive at all times represents the effect of the magnetic flux upon the bismuth resistors, and is more clearly shown in later oscillograms, while the other alternating component is merely due to an induced voltage in the coils of the bridge. This indicates that the bridge is not entirely noninductive.

The first problem was to design the bridge element so that it would be as nearly as possible noninductive. Some means must then be provided to balance out of the fluxmeter branch circuit any a-c voltage still induced. Finally the use of the bridge in a-c and transient magnetic fields must be studied to determine whether or not it does give consistent results in a-c fields, and, as far as the apparatus available will allow, determine its limits with respect to frequency.

Changes in Construction of the Instrument

To reduce the self-inductance of the bridge element, the wire for the various resistors was enameled and was wound in noninductive spirals, with the turns as closely together as possible. This winding also resulted in a smaller coil, for the same resistance, than was obtained in the instrument previously described. To eliminate any possible voltage generation between the bridge terminal and the resistor coils, all leads were made of copper foil of similar shape and size, and were stacked one upon the other, each insulated from the next by a thin film of paper. The details of this construction are clearly shown in figure 3.

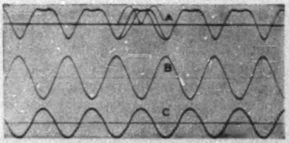

Figure 2. Oscillogram showing the distorted fluxmeter current before the bridge is provided with an a-c compensating coil

A—Current in fluxmeter
B—Induced voltage from exploring coil in air gap of magnet
C—Voltage across the exciting coils of magnet

A number of other improvements were made in the bridge element. Either lead or tin wire was found very satisfactory for use as balancing resistors, since they are nonmagnetic and have temperature coefficient of resistance about equal to or slightly higher than the coefficient for bismuth. By use of either, a slight negative reading previously experienced under certain conditions was entirely eliminated, as will be more fully discussed later. To stiffen the end of the bridge and more adequately to protect the resistor coils, the leads were tapered to a point near the coil end, in order that pieces cut from thin celluloid and shaped to extend around the outer edges of the coils might be

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Paper number 38-102, recommended by the AIEE committee on instruments and measurements and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted April 22, 1938; made available for preprinting July 11, 1938.

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[1] For all numbered references, see list at end of paper.

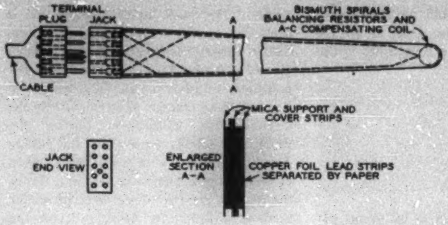
TERMINAL
PLUG
JACK
CABLE
A
A
BISMUTH SPIRALS BALANCING RESISTORS AND A-C COMPENSATING COIL
MICA SUPPORT AND COVER STRIPS
JACK END VIEW
ENLARGED SECTION A-A
COPPER FOIL LEAD STRIPS SEPARATED BY PAPER

Figure 3. Bridge and terminal block assembly

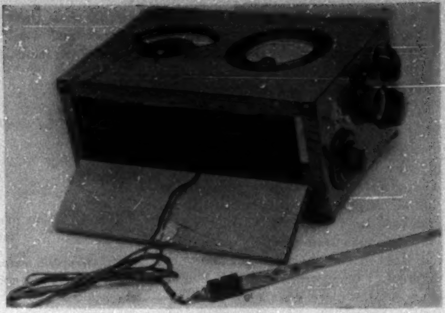

Figure 4. View of completed meter

cemented very securely to the mica support. The terminal block was divided into two parts, with the flexible leads to the instrument securely soldered into plugs in the one part, and corresponding jacks soldered to the copper-foil leads in the other. This method allows the bridge to be easily detached for checking or repairs, but is of most value in making adjustments during construction. By this means also two or more bridges may be used with the same meter. For example, the element of one bridge may be very small in diameter for measuring fields of very small area, while another may be designed with a much larger area, but sensitive to much lower field densities.

To balance out any a-c voltage still generated in the bridge coils, an a-c compensating coil of two or three turns of copper wire was wound around the outside of one set of the bridge coils. The terminals of this coil were carried to the meter and connected across a resistance, an adjustable portion of which is in series with the fluxmeter, as is shown in figure 1. By means of an oscillograph this adjustment may be set to give zero a-c current in the fluxmeter branch when the bridge is in a strong a-c magnetic field and the meter battery circuit is open. No difficulty was experienced in making this setting, and once it was made no further alteration was needed. When the instrument is used for direct current this coil has no function and is virtually outside the working circuit.

The controlling and indicating portion of the instrument was not greatly changed. For use with the oscillograph, two extra terminals were added, and a single-pole double-throw switch was provided to throw either the milliammeter or the oscillograph terminals into the flux-measuring position. The completed instrument, ready for use, is shown in figure 4.

The experimental tests may be divided into three groups: calibrations, phase relations, and examples of transient oscillograms.

Calibration Tests

For use in most of the tests, an electromagnet with a core of laminated transformer steel was designed and constructed. The pole faces of the square core were tapered and turned to a diameter of about four centimeters, and were spaced with an air gap of about 0.5 centimeters. With this magnet an a-c flux density of from eight to ten kilogausses could be obtained, though at the higher values the core would become noticeably warm.

The average value of the a-c magnetic-flux density was calculated from the area, the number of turns, and voltage induced in an exploring coil placed in the air gap. Three different exploring coils were used, each with a diameter of about 3.2 centimeters, and with 2, 30, and 100 turns, respectively, of number 38 enameled copper wire. The induced voltage was measured by means of a 15-volt rectifying type of voltmeter, with a resistance of 2,000 ohms per volt. The frequency was held at the proper value by using an oscilloscope to compare it with the power company's 60-cycle current upon which the electric clocks depend.

A check on the results obtained by the rectifying voltmeter was made by means of a high-grade thermocouple voltmeter. In this case the induced voltage was obtained by adding the computed drop in the coil to the voltmeter reading. The two methods checked very closely. For the major portion of the calibration work a good sine-wave voltage source was used as far as possible. Later distorted wave shapes were also used to check the calibration on non-sine waves.

The calibration curves for a constant temperature of 22 degrees centigrade, and for three different values of current supplied to the bridge circuit are shown in figure 5. All calibration curves made by means of direct current are shown in full lines; those for alternating current are shown in dashed lines. In all the a-c calibrations the average value of flux was used. Since the rectifying type of voltmeter indications are proportional to the average values, its reading will

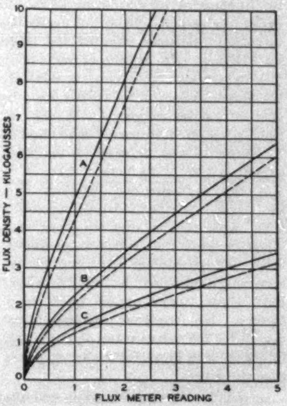

Figure 5. Calibration curves for magnetic fluxmeter

All curves taken at 22 degrees centigrade
Full lines indicate d-c calibrations
Dashed lines indicate a-c calibrations
Average values of a-c flux used throughout
A—Calibrations using three milliamperes in bridge
B—Calibrations using ten milliamperes in bridge
C—Calibrations using 25 milliamperes in bridge

indicate average values regardless of the voltage wave shape. In making checks with the thermocouple type of meter, care was used to select a good sine wave of voltage as the source.

The change in the resistance of bismuth when placed in a magnetic field varies somewhat with the temperature at which the bismuth is maintained. The change becomes less per unit of flux density as the temperature is increased. In the previous paper where only d-c fields were used this change was indicated by calibration curves at several different temperatures. The same changes take place when used for alternating current though the actual calibration curves at other temperatures are not shown. While this change is not quite linear with respect to temperature, over the usual range, the actual flux densities are roughly one per cent per degree lower for lower temperatures, or higher for higher temperatures, than the values shown for 22 degrees centigrade. Where more accuracy is desired, the values must be interpolated from a set of calibration curves covering the temperature involved, or by means of an empirical formula derived from such curves or data.

The a-c calibrations were carried out for frequencies varying from 12 to 540 cycles per second. Below 12 cycles the instrument pointers vibrated too much for reading. No apparatus was available to go above 540 cycles; in fact only the lower portions of the curves could be checked for this frequency. Where possible, portions of all curves were checked by means of two or more of the three exploring coils. All values taken over this range of frequencies, and in checking by means of the different exploring coils, were seldom over two per cent above or below the average value shown. Since the guaranteed accuracy of the rectifying voltmeter is only four per cent, these values appear quite satisfactory. No tendency whatever appeared for the values taken at the higher frequencies to be either higher or lower than the others.

Seemingly the direct current and the average values for the alternating current should fall on the same curve, but the experimental data proved this to be not so. Were this caused by the lag of the resistance change as found by Konig[2,3] more flux would probably be necessary to give the same meter reading rather than less, and the effect should increase as the frequency increased. Nor could it be accounted for by eddy-current losses, inductance, or capacity in the circuit since these would vary with frequency and would also tend to reduce rather than increase the sensitivity of the meter. The most logical explanation is suggested by the wave shape of the fluxmeter current oscillograms shown most clearly in figures 7 and 9. Because of the nonlinear relation of the flux density to the resistance change, as is evident by the direct-current calibration curves, the resulting variable current in the bridge circuit is decidedly non-sine wave when produced by a sine wave of flux. Since the torque in the direct-current meter used as a fluxmeter depends upon the average of this cyclic current, this average, due to the flat-topped waves, would be somewhat higher than for sine waves of similar amplitude and thus the reading for the average value of the alternating-current flux would be greater than would be obtained were the meter current waves sinusoidal.

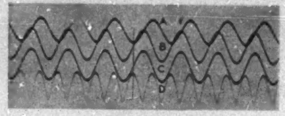

Figure 6. Phase relations at 60 cycles per second

A—Current in the coil of magnet
B—Voltage across coil of magnet
C—Induced voltage from exploring coil in air gap
D—Magnetic flux in air gap by means of fluxmeter

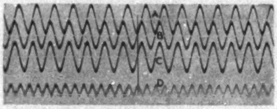

Figure 7. Phase relations at 280 cycles per second

A—Current in coil of magnet
B—Voltage across coil of magnet
C—Induced voltage from exploring coil in air gap
D—Magnetic flux in air gap by means of fluxmeter

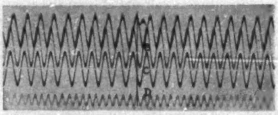

Figure 8. Phase relations at 480 cycles per second

A—Current in coil of magnet
B—Voltage across coil of magnet
C—Induced voltage from exploring coil in air gap
D—Magnetic flux in air gap by means of fluxmeter

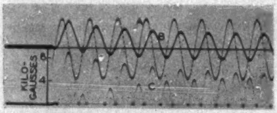

Figure 9. Starting transient when a-c voltage is switched across coil of magnet

A—Induced voltage from exploring coil in air gap
B—Current in coil of magnet
C—Magnetic flux in air gap by means of fluxmeter

No difference could be detected in the readings when the wave shape was distorted as compared to sine waves. Also since the highest frequency used is a rather high order of harmonics for the lowest and the calibration for all frequencies used fell on the same calibration curve, it seems evident that within the range of frequencies covered the accuracy of the meter is not noticeably affected by the wave shape of the flux.

The accuracy with which readings could be duplicated or checked between different meters, using reasonable care in determining the temperature, was seldom over two per cent in error when used for direct current. Only the one meter for use in alternating current has so far been constructed and calibrated; thus it would be difficult to state what accuracy could be expected, since the apparatus used for calibration could not be depended upon for an accuracy of more than four per cent, and this was the only possible means of obtaining a comparison.

Phase Relations

The second problem was to study the time-phase relations of the bismuth resistance and magnetic flux as they were varying. Investigations made in this field by Konig, Heurlinger, and others,[2–6] indicate a lag of the bismuth resistance change. Konig, who probably made the most extensive investigation, used a make-and-break device to produce changes in the magnetic field. This would probably be equivalent to using a rather high alternating frequency. The problem attempted in the present research was limited to what might be termed commercial frequencies and their harmonics.

For such study a series of some twenty or more oscillograms were taken over a range of from 60 to 480 cycles per second. Representative oscillograms for 60, 280, and 480 cycles per second are shown in figures 6, 7, and 8.

No lag in the resistance change can be detected at 60 cycles, but at 280 cycles a lag appears of about 14 electrical degrees, and at 480 cycles this has further increased to about 24 electrical degrees. However, these measured lags must not be considered more than approximate, since the oscillograph light and vibrator adjustments can often cause slight apparent phase shifts when none are present. For example, in figure 6, the magnetic-flux record appears slightly to lead the voltage values. The conclusions stated are based upon the average results of the large number of oscillograms taken. Unusual care was exercised in taking sets with the minimum of changes in adjustments, while other sets were purposely taken after adjustments had been changed.

Magnetic Transients

The third problem was to obtain some transient oscillograms of the magnetic field, together with the corresponding current and voltage values. The oscillogram in figure 9 shows the switching of a 60-cycle voltage across the coil of the magnet previously described. The unusual height of the transient-flux values gives an indication of the instantaneous mechanical forces acting during the transient period. The usual transient and steady conditions when a single-phase alternator is short circuited are shown in figure 10. The variations in the field current *C* after short circuit give some indication of the armature reaction on the field flux but the actual flux variations in *D* show exactly how the air-gap flux is varying as well as its phase relations to the field and armature current changes. The variations of the air-gap flux due to the armature slots and teeth are also very evident. Figure 11 shows the flux distribution under the pole face of a stationary direct-current motor, when the shunt field is excited. This was taken by moving the bridge element along the air gap. The oscillograph drum was rotated by a thread extending from its pulley to the bridge. Since the bridge was moved with the hand the oscillogram shows some unevenness which should not be present. Many other interesting and valuable studies could be made on stationary and running machines, as well as on other equipment.

Directional Variations

An observation may be made here, not strictly in line with the subject at hand but nevertheless relating to an interesting and important feature of the instrument.

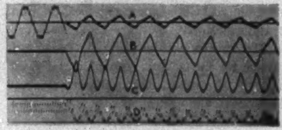

Figure 10. Transient from no load to almost short circuit in a single-phase alternator

A—Alternator terminal voltage
B—Alternator armature current
C—Alternator d-c field current
D—Air-gap flux under pole

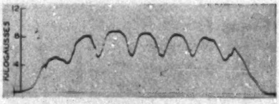

Figure 11. Distribution of magnetic flux under the excited pole of a stationary d-c motor

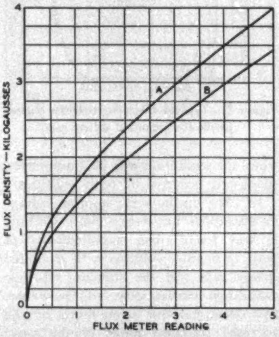

Figure 12. Directional effect in use of magnetic fluxmeter

Curves taken using 25 milliamperes in bridge
A—Magnetic flux parallel to plane of coils in bridge
B—Magnetic flux perpendicular to plane of coils in bridge

In the discussion on the previous paper[6] a set of curves was shown in which the bridge element was held with the plane of its coils parallel to the magnetic-flux lines. At very low-flux densities the readings were negative; at slightly higher values the negative reading decreased, finally changed to positive, and continued to increase. A greater density was always required to give the same reading than when the plane of the bridge coils was held perpendicular to the lines of flux.

By using lead or tin wire instead of nickel for the balancing resistors, no negative readings are obtained; the calibration curves obtained with the plane of the coil held parallel with the lines of flux, and also held perpendicular to the flux, are shown in figure 12. The directional effects of the magnetic field upon bismuth as well as upon other materials has been quite fully investigated in previous researches.[7]

At first thought this change might appear disadvantageous, but it has the one important advantage of indicating the resultant direction of the flux as perpendicular to the plane of the coils when the reading is maximum, or as being parallel to the plane of the coils when the reading is minimum. In the measurement and plotting of the distribution of the flux in irregular fields this indication would prove of considerable value.

Conclusions

While this research by no means exhausts the field for study, it has definitely proved that the instrument is of value in the measurement of alternating current and transient as well as direct-current magnetic fields. Within the range studied, the frequency has no effect upon the calibration. The tendency of the bismuth resistance to lag behind the flux changes found by previous investigators has been verified, though this lag at the usual commercial power frequencies either cannot be detected or is of very low magnitude. The instrument is thus well adapted to many studies in the alternating current and transient fields.

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Polarity Limits of the Sphere Gap

By F. O. McMILLAN
FELLOW AIEE

Synopsis: It is found that 60-cycle polarity characteristics correlate with the positive and negative impulse calibration curves for grounded sphere gaps sufficiently well to use them for determining the polarity limits for sphere gaps. The available data show that the maximum spacing at which a grounded sphere gap has equal positive and negative spark-over is a linear function of the size of the spheres. From this relation, it follows that the maximum sparking distance for equal positive and negative spark-over, expressed as a per cent of the diameter of the spheres, is not constant for all sphere diameters. Therefore, arbitrary polarity limits expressed in terms of the diameter of the sphere cannot apply to all sizes of sphere gaps.

EXTENSIVE use of sphere gaps in high-voltage testing and research makes it imperative to have complete data on their characteristics and limitations, particularly when the gaps are used for measuring impulse voltages. This paper describes a method of using the 60-cycle polarity characteristics for sphere gaps to determine their polarity limits of spark-over in measuring impulse voltages. Empirical expressions are derived for the equal positive and negative spark-over limit of the sphere gap in centimeters and also as a function of the diameter of the spheres. The large amount of data obtained from 60-cycle polarity characteristics and impulse tests made in the local laboratory over a period of eight years and by other investigators, have been carefully correlated in arriving at these empirical equations. As more data become available for large spheres at the higher voltages, similar expressions can be derived for the interesting but erratic transition region which is not used for high-voltage measurements. The laws proposed are applicable to the measurement of impulse voltages for which the time from zero to flashover voltage is one microsecond or more. This impulse time is the same as that for which the recommended AIEE sphere-gap calibration standards apply.

The sphere gap has been an accepted device for measuring high voltages since 1913;[1,2] however, the polarity characteristics of the grounded sphere gap were not observed until 1929. In October 1929, Lusignan[3] reported that 60-cycle symmetrical voltage only sparked over a 50-centimeter grounded sphere gap when the ungrounded sphere was negative if the spheres were one sphere diameter apart. He also reported that when the separation was 69 centimeters, spark-over occurred on both polarities.

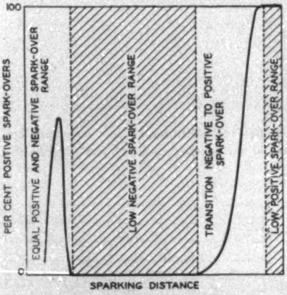

Figure 1. Typical 60-cycle polarity characteristic for grounded sphere gap

In 1929, during some impulse voltage investigations at Oregon State College, it was found that measurements of positive impulse voltages with grounded sphere gaps and the standard 60-cycle calibration curves gave voltage values that were too low over much of the accepted standard range of the gap.[4] This observation led to a study of the polarity characteristics of sphere gaps at 60 cycles to determine the extent and nature of the polarity effects at power frequencies. Complete 60-cycle polarity characteristics were obtained for several different types of gaps including sphere gaps. The latter showed four distinct polarity ranges occurring at different spacings of the spheres.[4]

The observed polarity characteristics of grounded sphere gaps when measuring impulse voltages were confirmed by a number of investigators, notably Allibone, Hawley, and Perry[5] in June 1933, Meador[6] in June 1934, and Bellaschi and McAuley[7] in June 1934.

General recognition of the difference in the positive and negative spark-over of grounded sphere gaps has led to extensive revision of sphere-gap calibration standards in the United States and abroad. Notable progress has been made by the AIEE subcommittee on sphere-gap calibrations in assembling positive, negative, and 60-cycle calibration data for the standard sizes of sphere gaps. Supplementing these data, it is important to know accurately the polarity limits of these gaps over a wide range of spacings. This information is readily obtained from the 60-cycle polarity characteristics, because sphere-gap polarity effects are essentially independent of the duration of the voltage wave for waves of one to two microseconds duration and longer.[8,9]

60-Cycle Polarity Characteristics

A typical 60-cycle polarity characteristic for a grounded sphere gap is shown in figure 1. Two methods of determining these characteristics, one utilizing visual Lichtenberg figures and the other a neon cathode-glow lamp, have been described in papers[4,6] previ-

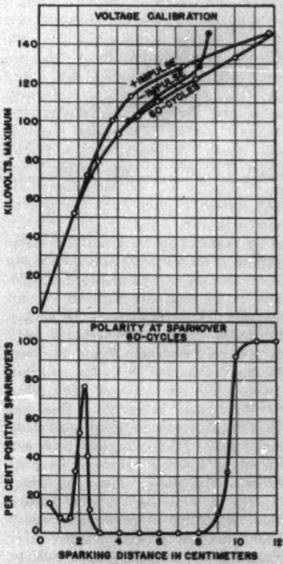

Figure 2. Characteristics of 6.25-centimeter grounded sphere gap

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Paper number 38-101, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9-12, 1938. Manuscript submitted May 23, 1938; made available for preprinting July 5, 1938.

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[1] For all numbered references, see list at end of paper.

ously cited. The 60-cycle polarity characteristics shown in figures 1, 2, and 3 cover a much wider range of sparking distances than are useful in measuring voltages; however, they serve to give a complete picture of the four distinct polarity ranges characteristic of grounded sphere gaps.

Both positive and negative spark-over occurs in the first range showing that the positive and negative spark-over voltages are essentially equal. The upper limit of this range is of great importance, because it shows the maximum sparking distance over which the sphere gap can be used without objectionable polarity effect.

Positive spark-over does not occur in the second range, showing that the negative spark-over voltage is less than the positive. This region is important, because it includes the remainder of the range of the sphere gap that is useful for measuring voltages and shows the spacings over which positive and negative calibrations must be used.

In the third range, both positive and negative spark-over occurs, and the gap is very erratic in its behavior, because corona forms on the ungrounded sphere at voltages that are lower than the spark-over value. This range covers the transition from 100 per cent negative spark-over to 100 per cent positive spark-over.

One hundred per cent positive spark-over occurs in the fourth range due to the formation of heavy corona streamers preceding spark-over. These corona streamers give the gap characteristics similar to those of a point-to-plane gap.

Sphere-gap spacings falling in ranges three and four are almost entirely of theoretical interest, because they have very little practical value for voltage measurements.

Correlation of 60-Cycle Polarity Characteristics With Impulse Calibrations

When the positive and negative impulse voltage calibration curves for a sphere gap are plotted directly above the 60-cycle polarity characteristic, using a common scale of abscissas, the correlation of the 60-cycle characteristic with the calibration curves is clearly shown. This has been done for the 6.25-centimeter and 50-centimeter sphere gaps in figures 2 and 3. These sphere gaps were selected on account of the large difference in size and because complete characteristics were available. Both the 60-cycle and impulse data for the 50-centimeter gap were taken by Meador[6] in another laboratory. The wave used for the 6.25-centimeter sphere-gap impulse voltage calibration was a 1x5 microsecond wave, while the 50-centimeter gap was calibrated with a 1.5x40-microsecond wave.

Table I. Polarity Limits of Spark-Over for Grounded Sphere Gaps

	Sphere-Gap Spacing in Centimeters and Per Cent Sphere Diameter											
	Equal Positive and Negative Spark-Over Range			Low Negative Spark-Over Range			Transition Negative to Positive Spark-Over			Low Positive Spark-Over Range		
		Maximum			Maximum			Maximum			Maximum	
Diameter of Spheres (Centimeters)	Centimeters Minimum	Centimeters	Per Cent Sphere Diameter	Centimeters Minimum	Centimeters	Per Cent Sphere Diameter	Centimeters Minimum	Centimeters	Per Cent Sphere Diameter	Centimeters Minimum	Centimeters	Per Cent Sphere Diameter
2.00	*											
2.54	0	2.2	87	2.2	3.3	130	3.3	3.8	150	3.8	∞	∞
6.25	0	3.	48	3.	8.	128	8.	11.	176	11.	∞	∞
12.5	0	5.	40	5.	15.	120	15.	*				
25.	0	9.	36	9.	30.	120	30.	*				
50.	0	13.	26	13.	50.	100	50.	70.	140	70.	∞	∞
75.	0	22.	29	22.	*							
100.	0	30.	30	30.	*							
150.	*											
200.	0	55.	28	55.	*							

* Limits of available data.

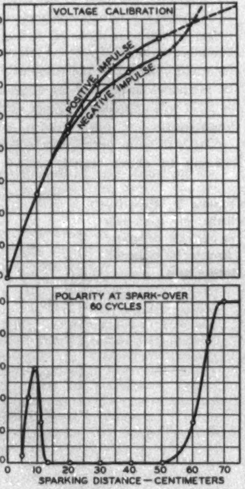

Figure 3. Characteristics of 50-centimeter grounded sphere gap

The upper limits of the equal positive and negative spark-over ranges for the two polarity characteristics coincide quite well with the points of divergence for the positive and negative impulse calibration curves. The limit for the 6.25-centimeter sphere gap is slightly higher than the branching point for its impulse calibration curves, and the limit for the 50-centimeter sphere gap is essentially coincident with the point of divergence of the calibration curves.

The range of zero positive spark-over is wider than the usual range of impulse calibration curves, and it clearly shows the gap limits over which positive and negative calibration curves must be used for impulse measurements.

The polarity characteristics shown were taken on gaps with the ungrounded sphere mounted directly above the grounded sphere and with the sparking surface of the grounded sphere four sphere diameters above the ground plane. This is the usual position of sphere gaps when making high-voltage measurements. Other 60-cycle polarity characteristics taken with the sphere gap mounted in a horizontal position with respect to the ground plane were essentially the same as those for the vertical gap except the second range over which there are no positive spark-overs was shortened, because the transition from negative to positive spark-over occurred at a shorter gap spacing.

Polarity Limits From 60-Cycle Polarity Characteristics

Close correlation between impulse voltage calibration curves and the 60-cycle polarity characteristics makes it possible to take from the latter characteristic curves the sparking distances defining each of the four distinct polarity ranges for the different sizes of sphere gaps. The polarity characteristics at 60 cycles are readily determined if an adequate source of high voltage is avail-

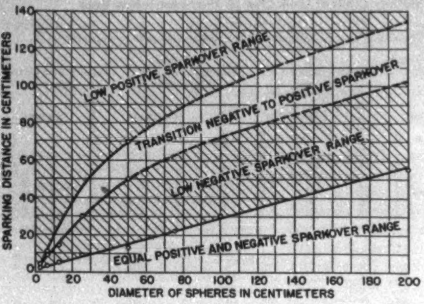

Figure 4. Polarity limits of spark-over in centimeters for grounded sphere gaps

able. The maximum voltage available for this investigation was limited, and hence it was necessary to draw upon data published by Meador[4] in 1934 to show the polarity limits for the large standard sphere gaps. Meador's results, which are the only data published since the original paper[4] in 1930, are the most extensive available, but they are not complete for the large sphere gaps at wide spacings.

The polarity limits of spark-over for grounded sphere gaps are shown in table I. These data are sufficient to determine the upper limit of the very important equal positive and negative spark-over range for all standard sphere gap sizes except the 2-centimeter spheres, but they are quite inadequate for the other three higher polarity ranges. It is hoped that investigators with high-voltage facilities available will extend and check the existing data.

The data in table I are shown graphically in figures 4 and 5. Figure 4 shows that the sparking distance in centimeters, marking the upper limit of the equal positive and negative spark-over range, is a linear function of the diameter of the sphere. The equation of the curve is:

$$S = 0.267\,D + 1.50 \qquad (1)$$

where

S = maximum sparking distances, in centimeters, for equal positive and negative spark-over.

D = sphere diameter in centimeters.

This is a very interesting and important relationship, and, if confirmed by more complete and extensive data, it shows that the more or less generally accepted idea that small sphere gaps are useful for measuring impulse voltages over a smaller per cent of sphere diameter separation than large gaps is erroneous. These data show that just the opposite is true. If the sparking distance S in equation 1 is expressed as a per cent of sphere diameter D, the following equation may be written:

$$S_D = 26.7 + \frac{150}{D} \qquad (2)$$

where

S_D = maximum sparking distance, in per cent of sphere diameter, for equal positive and negative spark-over.

D = sphere diameter in centimeters.

From equation 2 it is apparent that for small values of D the value of S_D will be relatively large, and for large values of D, it will approach 26.7 per cent as a lower limit.

The maximum permissible sphere-gap spacing to obtain equal positive and negative spark-over voltage within the useful range of the gap are given in table II. These are calculated values from equations 1 and 2 which give the maximum spacings in centimeters and in per cent of sphere diameter. A comparison of the calculated values in table II with the experimental values in table I shows that the agreement is well within the limits of experimental accuracy.

Reference to the experimental values in table I shows that the 2.54-centimeter sphere gap shows no appreciable polarity effect until a spacing of 87 per cent of sphere diameter is reached, while the 200-centimeter gap shows a polarity effect at 28 per cent of diameter separation. The maximum sparking distances for equal positive and negative spark-over, expressed in per cent of sphere diameter, are shown graphically in figure 5.

In the light of these data, it is not advisable to establish arbitrary sphere-gap limits expressed in terms of sphere diameter and apply them to all gaps regardless of size, as has been the practice in previous standards for measuring high voltage.

The boundary curves for the low negative spark-over range and the transition from low negative to low positive spark-over are only reasonably well established up to 50-centimeter diameter spheres; beyond that point the curves are located entirely by extrapolation without adequate data, and the curves may be very much in error. These portions of the sphere-gap characteristics are beyond the limits that are useful for measuring voltage, but it is believed when data are available to establish them accurately, they will assist in obtaining a better understanding of sphere-gap characteristics within the useful range. The available data show that as sphere gaps increase in size, the polarity limits do not increase to the proportions that would be expected. This may be due, at least in part, to abnormal flux distortion because of inadequate free space around the large gaps.

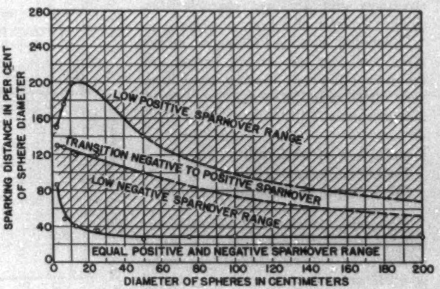

Figure 5. Polarity limits of spark-over in per cent of sphere diameter for grounded sphere gaps

When important measurements are to be made, a low-frequency polarity characteristic may be taken on the sphere gap in position to determine whether or not the polarity characteristics are normal. This precaution is important with large gaps, because it is a well-known fact

Table II. Maximum Sparking Distance for Equal Positive and Negative Spark-Over Voltage for Grounded Sphere Gaps

Values Calculated From Empirical Equations

Diameter of Spheres Centimeters	Maximum Sparking Distance: Centimeters $S = 0.267D + 1.50$	Maximum Sparking Distance: Per Cent Sphere Diameter $S_D = 26.7 + \frac{150}{D}$
2.00	2.03	101.7
2.54	2.18	85.7
6.25	3.17	50.7
12.5	4.84	38.7
25.	8.18	32.7
50.	14.9	29.7
75.	21.5	28.7
100.	28.2	28.2
150.	41.5	27.7
200.	54.9	27.5

that the flux distortion due to the ground plane causes the polarity effect, and when this distortion is abnormal, standard calibration data no longer apply.

Concluding Summary

1. The 60-cycle polarity characteristics for grounded sphere gaps show four distinct polarity ranges:

I. Equal positive and negative spark-over.

II. Low negative spark-over.

III. Transition from low negative to low positive spark-over.

IV. Low positive spark-over.

2. The correlation of the 60-cycle polarity characteristics with the positive and negative impulse voltage calibration curves for grounded sphere gaps is such that the 60-cycle polarity characteristics may be used to determine the impulse polarity limits for measuring voltage waves that increase from zero to flashover in one microsecond or more.

3. All available data show that the maximum sparking distance for equal positive and negative spark-over is a linear function of sphere diameter over the entire range of standard sphere-gap sizes. The equation for the relationship is:

$$S = 0.267\,D + 1.50$$

4. Small grounded sphere gaps may be used over a greater per cent of diameter separation than large gaps without appreciable polarity effect.

5. The maximum sparking distance for equal positive and negative spark-over voltage for grounded sphere gaps, expressed as a per cent of the diameter of the spheres, is a variable function of the sphere diameter. The equation for the relationship is:

$$S_D = 26.7 + \frac{150}{D}$$

Therefore, it is inadvisable to establish arbitrary sphere-gap polarity limits expressed in terms of sphere diameter and apply them to all gaps regardless of size.

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Discussion

J. R. Meador (General Electric Company, Pittsfield, Mass.): This is an interesting continuation of the work reported by the author and E. C. Starr in 1931.

I wish to comment on the statement:

> The available data show that as sphere gaps increase in size, the polarity limits do not increase to the proportions that would be expected. This may be due, at least in part, to abnormal flux distortion because of inadequate free space around the large gaps.

The implications of this statement are important to those who expect to use sphere gaps over a wide range of gap spacings.

In the tests reported in the writer's paper ("Calibration of the Sphere Gap," J. R. Meador, AIEE Transactions, volume 53, 1934, pages 942–8) it was found that the transition points from partial negative spark-over to all negative spark-overs (usually near one-fourth diameter spacing) and from all negative spark-overs to partial negative spark-overs (usually near diameter spacing) were not entirely consistent. This was particularly true of the $6^1/_4$-centimeter sphere gap which had no all-negative spark-over range.

In the early part of 1934 the writer made a few tests on several sizes of spheres to determine the effect of surroundings on the spark-over voltage and polarity characteristics. Some of the results obtained may be of interest.

With $6^1/_4$-centimeter spheres at $6^1/_4$-centimeter spacing and the sparking point of the grounded sphere 4.1 diameters above a flat grounded plate, 100 per cent negative spark-overs were obtained. When the spacing above ground was increased to 5.7 diameters, only 80 per cent of the spark-overs were on the negative half cycle. With four vertical line-potential wires spaced ten inches from the center line of the spheres, the voltage was approximately the same as without shielding wire but all spark-overs were positive polarity. In this case the spheres were overshielded.

Similar effects of shielding were obtained with the 12.5-centimeter and 50-centimeter spheres. In each case when the shielding wires were adjusted to give equal positive and negative spark-overs, the 60-cycle calibration curve was in close agreement with the calculated curve for isolated sphere gaps. Similarly, the positive and negative polarity impulse spark-over curves were practically the same.

These comments are made to emphasize the fact that care must be exercised when the spheres are used at spacings above $^1/_4$ to $^1/_3$ of diameter for either low frequency or impulse measurements, since the surroundings may appreciably influence the polarity characteristics and consequently the spark-over voltage on both polarities.

Abe Tilles (University of California, Berkeley): Professor McMillan's most interesting paper presents, logically assembled, a great deal of useful information. It answers numerous questions and raises still other questions.

In the so-called "equal positive and negative sparking range" of figures 1, 2, and 3 would it not be more correct to alter the nomenclature somewhat? Possibly "range of spark-over of both polarities" would do. The curve seems to have a definite structure in that range and there seem to be strictly "equal" spark-overs only where the curve is, within the limits of error, at an ordinate of 50 per cent. How well defined is the curve in that region? How many individual spark-overs is it necessary to observe to establish a point on this curve?

In comparing the 60-cycle polarity curves of figure 3 for 50-centimeter spheres and of figure 2 for 6.25-centimeter spheres an upturn is observed at the smallest spacings of figure 2. Presumably, for such a curve on the still smaller 2.5-centimeter spheres, and for measurements at still smaller spacings, this upturn might be observable in greater detail. It would be of interest if Professor McMillan could publish such a curve for the smallest spheres and spacings at which he obtained data. Small gaps may show interesting differences in behavior from large gaps.

It is clear, I believe, that any appreciable even harmonic distortion in the voltage wave shape would introduce error into the results. In what way, and to what degree of refinement, was the voltage wave shape controlled or observed?

In assembling these data Professor McMillan has doubtless had occasion to consider what varying mechanisms of spark-over account for the different "polarity ranges" at different spacings. It would be helpful if he would care to state more fully what he considers these mechanisms of spark-over to be.

D. W. VerPlanck (Yale University, New Haven, Conn.): As pointed out by Professor McMillan the negative impulse spark-over voltage for grounded sphere gaps is lower than the positive at intermediate spacings but becomes higher at large spacings. From this, one might expect that with alternating voltage in the intermediate range of spacings, all spark-overs would have negative polarity, and that at the spacing where the impulse characteristics cross, the polarity of the alternating voltage sparks would change abruptly from negative to positive. Actually, however, as shown in the paper, the transition from negative to positive polarity is a gradual one extending over a range which includes the spacing at which the impulse characteristics cross (see figures 2 and 3). That the transition is not abrupt may possibly be because of an insufficient continuous supply of initiating electrons in critical parts of the gap. In this connection it would be interesting to know whether or not McMillan's data were obtained from tests on irradiated gaps.

While the middle of the transition range may be at the spacing where the impulse characteristics cross, the transition appears to begin, as will be shown below, at the spacing for which the ratio of positive to negative impulse voltage is a maximum.

The ratio of positive to negative impulse spark-over voltage as a function of spacing to diameter ratio for the various standard sphere sizes is plotted in the figure accompanying this discussion. These curves, obtained with the aid of empirical formulas presented in a recent paper ("A New Correlation of Sphere-Gap Data," D. W. VerPlanck, AIEE TRANSACTIONS, January 1938, page 45) are in accord with the revision of the AIEE proposed sphere-gap standards dated December 1937. The dashed portions of the curves are extrapolations by means of the formulas beyond the region actually covered by the standards. The maxima of these curves occur at spacing to diameter ratios shown in table I of this discussion, which increase with decreasing sphere diameter. An examination of Professor McMillan's figure 5 shows that the lower boundary of his transition region occurs at spacing to diameter ratios, also shown in table I, which are substantially the same as those for the maximum ratio of positive to negative impulse voltage. Thus independent evidence is presented confirming Professor McMillan's results at least to the extent of showing that there must be some change in the mechanism of sparking commencing at about the spacing to diameter ratios shown in table I.

Another, though less definite check is that each of the curves showing the ratio of positive to negative impulse spark-over voltage has a point of inflection at roughly the same spacing to diameter ratio as the lower boundary of the "low negative spark-over range" in Professor McMillan's figure 5. This may indicate another change in the character of the spark discharge.

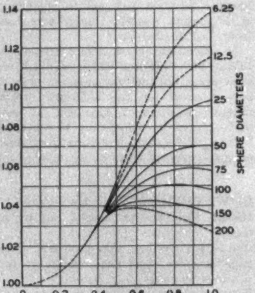


Figure 1

Table I

Sphere Diameter (Centimeters)	Spacing to Diameter Ratio	
	For Maximum Ratio of Positive to Negative Impulse Spark-over Voltage	For Beginning of Transition Negative to Positive Spark-over Polarity
200	0.55	0.55
150	0.65	0.60
100	0.80	0.75
75	0.87	0.85
50	1.0	1.0
25	Greater than 1.0	1.15

F. O. McMillan: It is suggested by Professor VerPlanck that the rather gradual transition from negative to positive spark-over may be due to an insufficient continuous supply of initiating electrons in critical parts of the gap, and he asks whether the data were obtained from irradiated gaps. The gaps were not irradiated to obtain any of the data used, hence the initiating electrons were supplied entirely by the normal ionization of the air in the interelectrode space. Some experimental evidence at hand indicates that the number of ions present does influence the polarity characteristics near the transition regions. However, it is believed that the rather long transition range between negative and positive spark-over is largely due to the fact that it occurs over the range of gap spacings where the corona discharge and spark-over curves are diverging. When corona occurs at a slightly lower voltage than complete spark-over, voltage disturbances are set up by the corona that cause spark-over to occur on both polarities. As the gap spacing is further increased and the voltage difference between corona formation and spark-over becomes greater, the corona streamers on the ungrounded sphere reach sufficient length to cause the gap to behave somewhat like a point-to-plane gap, and the spark-over always occurs when the ungrounded sphere is positive.

The graphs of the ratio of positive to negative impulse spark-over voltage as a function of spacing to diameter ratio obtained by Professor VerPlanck from his recently developed formulas for sphere-gap characteristics lead to conclusions that agree remarkably well with the data in the paper. This independent evidence aids materially in establishing the dependability of the results.

A question of nomenclature is raised by Doctor Tilles. He suggests "range of spark-over of both polarities" in place of "equal positive and negative spark-over range." Whether or not this is desirable depends upon the quantities to which "equal" is applied. If an equal number of positive and negative spark-overs is understood, the point is well taken; however, the expression is used in the paper to state that the magnitudes of the positive and negative spark-over voltages are essentially equal for the range in question. This use is believed to be justified because if both positive and negative spark-overs occur at a given spacing, even though they are unequal in number, the magnitudes of the positive and negative spark-over voltages must be essentially equal within the limits of measurement, otherwise spark-over would occur only on one polarity.

The ordinates of the polarity characteristic curves for the range of gap spacings in question are determined by the laws of probability and are established by taking a relatively large number of observations at each point. Usually 25 to 50 observations will locate a point reasonably well, but in most instances a hundred or more observations were made to determine each point on the polarity characteristics.

The 60-cycle polarity characteristics for different sizes of grounded sphere gaps all show four distinct polarity ranges as described in the paper, but the relative widths of the ranges vary with the size of the spheres as shown by the curves in figure 5. The upward inflection of the 6.25-centimeter sphere-gap polarity characteristic at close spacings is characteristic of small gaps and is exhibited to an even greater degree by the 2.54-centimeter sphere gap. The polarity characteristic curve for a 2.54-centimeter sphere gap is shown in figure 9 of the paper "The Influence of Polarity on High-Voltage Discharges," AIEE TRANSACTIONS, volume 50, page 25.

As Doctor Tilles points out, even harmonic distortion of the 60-cycle voltage wave shape would introduce errors in the results. This was guarded against by using an alternator with a good wave form, by using field control on the alternator to vary the voltage, and by the careful elimination of all voltage-distorting phenomena such as nonlinear circuit elements and corona discharges.

A gap that is physically and electrically symmetrical does not show any polarity effect. The mechanism of spark-over for each of the four polarity ranges of the grounded sphere gap can, therefore, be accounted for reasonably well by the dielectric flux distortion due to the ground plane and its resulting influence on the ionization phenomena in the gap.

Consider a grounded sphere gap mounted perpendicular to the ground plane with the spark gap four to five sphere diameters above it, and the lower sphere connected electrically to the plane. When voltage is applied to such a gap, a dielectric field is established between the insulated and the grounded sphere and also between the insulated sphere and the ground plane.

At relatively close spacings, the flux between the sparking surfaces of the spheres is not appreciably distorted by the ground plane; hence, the voltage gradients at the sparking surfaces of the two spheres are equal and the gap does not show any polarity effect.

When the sparking distance is increased beyond a certain value, which the paper shows to be a function of sphere diameter, the ground plane distorts the dielectric field between the spheres to such an extent that the gap becomes electrically unsymmetrical and a polarity effect results. A part of the flux originating on the insulated sphere terminates on the grounded sphere and a part of it on the ground plane. Because of this flux distribution, the flux density and the resulting voltage gradient is greater on the sparking-surface of the upper ungrounded sphere than on the lower grounded sphere. When the gap spacing is further increased, the flux distortion and the difference in voltage gradient at the sparking-surfaces of the two spheres becomes greater.

When the top sphere is negative, the electrons in the gap are repelled from it toward the bottom grounded sphere. If the voltage gradient is sufficiently high, these electrons will attain ionizing velocities and release more electrons and positive ions by ionizing the air. The newly formed electrons are also repelled and produce further ionization as they travel toward the positive grounded sphere. The original positive ions in the gap and those formed by the avalanche ionizing action are attracted toward the negative sphere, but due to their relatively large mass and resulting low velocity, as compared with the electrons, a positive space-charge results. The maximum ionization and, therefore, the maximum positive space-charge will occur in the region of highest voltage gradient or near the ungrounded negative sphere. This positive space-charge near the negative sphere further increases the gradient and the ionization in that region. As the positive space-charge approaches very near the negative sphere, it probably ultimately produces a sufficiently high gradient to overcome the work function of the metal and extracts electrons from the sphere. Some of the electrons extracted recombine with positive ions, and others are projected into the interelectrode space and sustain the ionization. This ionization process progresses until the entire distance between electrodes is ionized, and spark-over occurs if the spacing of the sphere gap is within the range where the corona-formation voltage and spark-over voltage are identical. Reversing the polarity of the sphere gap, the upper ungrounded sphere becomes positive, and the region of high-voltage gradient resulting from the flux distortion of the ground plane is on the upper sphere as before. The electrons and negative ions are attracted to the top sphere, and the positive ions left behind by the avalanche process of ionization by collision are near the ungrounded sphere as before, but it is now positive instead of negative. Under these conditions, the grounded negative sphere must supply the electrons for sustaining a discharge between spheres. However, the negative sphere has a low voltage gradient at its discharge surface due to the ground-plane flux distortion, and the further fact that the point of maximum positive space-charge is at the point of maximum ionization near the positive sphere. As a result of these conditions, the extraction of the necessary electrons from the negative sphere to sustain a discharge requires a higher voltage when the upper ungrounded sphere is positive than when it is negative. Therefore, at intermediate gap spacings where both corona formation and spark-over occur at the same voltage, the grounded sphere gap sparks over at a lower voltage when the upper ungrounded sphere is negative than when it is positive.

At wide gap spacings, where corona forms on the ungrounded sphere of the grounded gap at a voltage lower than the spark-over value, the characteristics of the gap are materially altered. Under these conditions the sphere gap takes on the characteristics of a gap having electrodes with a very marked dissimilarity. The corona streamers on the top sphere act as points and the bottom sphere and ground plane as a composite electrode. The condition is a compromise between a point-to-sphere and a point-to-plane gap. When the ungrounded sphere is positive, the free electrons are attracted upward toward it and accelerated to the ionizing velocity in the region where the voltage gradient is sufficiently high. These electrons and those liberated by ionization, except the ones lost by recombination, are conducted away by the positive electrode. The relatively immobile positive ions not neutralized by recombination are left along the ionization or corona-streamer paths and form space charges. These positive space charges add to the field from the positive electrode and extend the ionizing gradient at the ends of the streamers to greater distances. In this manner ionization is progressively extended downward at the ends of the corona streamers until either the gradient at the extreme limit of the ionized region falls below the critical value or complete spark-over occurs. When the ungrounded sphere is negative, the free electrons in the surrounding space are repelled downward at high velocity producing ionization by collision in the region that is above the critical gradient. A positive space charge is formed near the negative sphere, and a negative space charge is formed outside of the positive space charge. The field of the positive space charge opposes the field from the negative electrode which it surrounds and reduces the resultant electrode flux in the area outside of the positive space charge. The decrease of flux due to the positive space charge around the negative sphere reduces the distance to which the ionizing gradient extends from the electrode and hence increases the voltage necessary to cause complete spark-over. Therefore, at wide spacings on the grounded sphere gap where corona occurs at an appreciably lower voltage than complete spark-over the gap sparks over at a lower voltage when the ungrounded sphere is positive than when it is negative.

J. R. Meador in his discussion further stresses the importance of adequate clearance around sphere gaps to avoid abnormal flux distortion and the resulting erratic characteristics. That point cannot be emphasized too strongly, because there is no doubt that it is the cause of much difficulty with sphere-gap measurements.

The inconsistencies in the transition from partial negative spark-over to all negative spark-overs found by Mr. Meador in his 1934 investigations, which he points out were particularly pronounced in the case of the 6.25-centimeter sphere gap, merit special consideration. In contrast with Mr. Meador's experience, we have always obtained polarity characteristics for the 6.25-centimeter sphere gap that show consistently a definite range of gap spacings over which the spark-over is 100 per cent negative.

The change from 100-per-cent-negative spark-overs to 80 per cent, when the sparking point of the ground sphere was raised from 4.1 sphere diameters to 5.7 sphere diameters above the ground plate, reported by Mr. Meador, probably is not due to the change in position of the gap with respect to the ground plane.

During the academic year 1933–34, E. D. Harrington and K. M. Klein, students at Oregon State College, made a careful investigation of the influence of the ground-plane position on the polarity characteristics of the 6.25-centimeter sphere gap. The results of their investigations were given in a paper presented before the Portland Section of the AIEE on May 19, 1934. They used a vertical gap with the sparking surface of the grounded sphere at various distances ranging from 1 to 20 diameters above a large metal ground plate. The polarity characteristics, over this entire range of ground-plate positions, each showed a definite range of gap spacings over which the spark-overs were 100 per cent negative. The 100-per-cent-negative spark-over range was changed to some extent by the position of the ground plane, but typical polarity characteristics with four distinct polarity ranges were obtained for all ground-plane positions.

In 1935, polarity characteristics were taken for a 6.25-centimeter horizontal grounded sphere gap. The characteristics were taken with the horizontal axis of the spheres at both four and five diameters above the ground plane. This horizontal gap also had typical polarity characteristics with the usual four distinct polarity ranges.

For the past eight years, the polarity characteristic of either a 6.25-centimeter or a 12.5-centimeter vertical sphere gap has been taken each year by students as a high-voltage laboratory experiment. These polarity characteristics have always shown a 100-per-cent-negative spark-over range and have been typical in every respect.

In view of this evidence, we are forced to the conclusion that Mr. Meador's failure to find a range over which the spark-over of the 6.25-centimeter gap was 100 per cent negative was due to some extraneous cause not directly related with the polarity characteristics of the gap.

It has been found that it is necessary to have the circuit free from corona discharges of any magnitude when the polarity characteristics are taken on small sphere gaps. The elimination of corona is important because corona discharges cause transient disturbances in the voltage that alter the polarity distribution of spark-over on small gaps. It is possible that either corona or some other electrical disturbance may account for Mr. Meador's failure to secure any 100-per-cent-negative spark-over range on the 6.25-centimeter sphere gap.

The shielding effects of wires reported by Mr. Meador are very interesting and are in agreement with the results obtained by George M. Chandler in his thesis "The Control of Sphere-Gap Polarity Characteristics" submitted at Oregon State College in 1935. Mr. Chandler used one toroidal shielding ring around the upper ungrounded sphere and in various vertical positions with respect to it, and also two toroidal rings with one around each sphere of the gap. He found that one shielding ring properly adjusted around the ungrounded sphere was adequate to eliminate the polarity effect.

Devices for Controlling Amplitude Characteristics of Telephonic Signals

By A. C. NORWINE
NONMEMBER AIEE

Synopsis: This paper describes a family of devices which automatically respond to signals and control the circuit amplification in such a way as to improve transmission. Their general characteristics are outlined, their differences explained, and some of their applications are listed.

THE transmission of speech energy over electrical circuits is attended by the interesting and sometimes difficult problem of preserving the original signal in spite of limitations in the transmission medium. These limitations include load-carrying capacity, interference with other service, noise, change in attenuation with time, and many others. Because of special limitations it is sometimes desirable to alter the amplitude characteristics of the speech or other signal energy without, of course, materially lowering its intelligibility. In high-quality systems the peak voltage from some speech sounds of a given talker may be over 30 decibels (some 30 times) higher than from his weakest sounds when there is very little inflection in the speech. Loudness changes for emphasis will increase this range of intensities. Ordinary message systems do not have to contend with quite so wide a range of instantaneous voltages from a single talker, but different talkers under extreme terminal conditions produce about a 45-decibel range of average voltage, which is additive to that for a single talker. Consequently, a voltage range of about 70 decibels (over 3,000 to 1) must be considered for message circuits.

In order to accommodate such ranges of intensity to certain transmission media such as radio links a new family of automatic devices has been developed. In general all of these contain amplifiers or attenuating networks whose loss or gain is changed according to some function of the applied input and which may have a variety of time sequences in their control circuits. It is hoped that by the classification and description of some of these devices their distinguishing characteristics and fields of usefulness will be made somewhat clearer. We are to be concerned here principally with those elements allied to the telephonic art, although some applications are to be found in other fields. It is not intended to include those voice-operated functions which are essentially switching operations although the distinction in some cases becomes exceedingly fine.

Names of volume-controlled devices* which have been used in published papers include vogad,[†][2–4] compandor,[**][5,6] and volume limiter.[7] Without direct comparison it may not be obvious how these and similar devices differ. First the apparent similarity of several of these devices will be shown in simple diagrams. Next the more important characteristics of a number of devices will be presented in tabular form, followed by descriptions of the different types. These will then be discussed with particular emphasis on their distinctive qualities, with notes on their variants which have some apparent value.

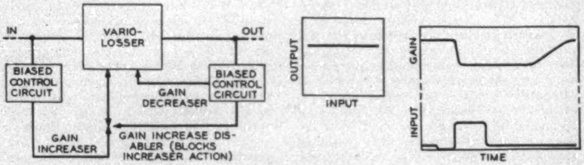
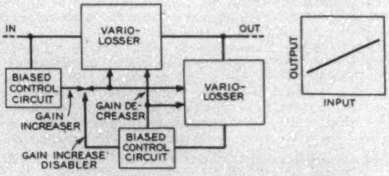


Figure 1. Vogad



TIME FUNCTIONS SAME AS VOGAD

GAIN CHANGES HALF AS GREAT

Figure 2. Half vogad

General Characteristics of Volume-Controlled Devices

In figures 1 to 10 are shown simplified diagrams of some of these devices. While detailed descriptions of them will be deferred till later it may be pointed out that all those shown contain "vario-lossers," and all have paths from the main transmission path to control circuits which affect the vario-losser. A vario-losser usually consists of a balanced pair of vacuum tubes whose gain is changed by varying the grid bias or a network of nonlinear elements such as copper oxide or silicon carbide whose loss is changed by varying a current through them. In some special cases it may be a mechanically adjusted variable network. The word "vario-losser" is thus a generic term relating to a circuit whose loss or gain is controllable. A control circuit ordinarily consists of an amplifier and rectifier whose d-c or a-c output bears a chosen relation to its input. Thus some control circuits are marginal; they produce no control voltage till the input exceeds some critical value, then produce large control voltages for small additional increments of input. These are used, for example, when it is desired to limit the output of a vario-losser to a definite amount. Another type of control circuit produces a current or voltage which is linear with input expressed in decibels. In combination with a vario-losser whose gain is a linear function of control current or

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Paper number 38-115, recommended by the AIEE committee on communication and scheduled for presentation at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 31, 1938; made available for preprinting July 8, 1938.

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[2] For all numbered references, see list at end of paper.

* See the footnote on page 63.

† "*Volume-operated gain-adjusting device.*"

** A combination of the names *compressor* and *expandor*.

Table I—Characteristics of Volume-Controlled Devices

Device	Gain Controlled by	Frequency of Adjustment	Time Required for Gain Changes		Ratio of Output Range to Input Range**	Position of Controls	Volume Range Controlled*	Frequency Range Controlled	Frequency Range Causing Control	Part of Input Range Causing Operation
			Gain Increase	Gain Reduction						
1. VOGAD	Average volume	Infrequent. Gain fixed between transmissions	After a few words—sometimes eight to ten words	After one or more words	Approximately 0	At input and output	Large	Full band transmitted	Full band	All
2. VOLUME LIMITER	Average volume over part of input range	Relatively infrequent. Approaches maximum gain in silent periods.	Slow and continuous	After one or more words	1 up to operate point. 0 above	At output	Moderate	Full band	Full band	High amplitude only
3. COMPANDOR										
a. Compressor b. Expandor	Syllabic variations	Continuous at syllabic rate	On each syllable	On each syllable	1/2 to 1/5 5 to 2	Compressor output Expandor input	Large	Full band	Full band	All
c. Special compandor	Syllabic variations	Continuous at syllabic rate	On each syllable	On each syllable	1/2 to 1/5 5 to 2	Compressor output expandor input or expandor control over separate channel	Large	High frequency only or multi-band	High frequency only or multi-band	All or high amplitude only
4. RADIO-NOISE REDUCER (Limited-range expandor)	Syllabic variations over part of input range	Each syllable	On each syllable	On each syllable	2 for low amplitudes. 1 for high.	At input	Moderate	Full band	Full band	Variable at low amplitudes only. Fixed gain to high amplitudes
5. LIMITED-RANGE COMPRESSOR	Syllabic variations over part of input range	Each syllable within the operating range	Fast	Fast	1 up to operate point, then 0 to 1/2, then 1	At input or output	Small	Full band	Full band	High or intermediate amplitudes
6. PEAK LIMITER	Syllabic peaks	Infrequent	After about one word	On single syllable	1 up to operate point, then approaches 0	At output	Small	Full band	Full band	High amplitude only
7. PEAK CHOPPER	Instantaneous peak voltage	Relatively infrequent	Instantaneous	Instantaneous	1 up to operate point then 0	At input	Very small	Full band	Full band	High amplitude
8. CROSS-TALK SUPPRESSOR	Voltage exceeding a specified value low in input range	Relatively frequent	Fast	After one or two words	1 except at point of discontinuity	At input	Very small	Full band	Full band	All above specified low value
9. ROOTER AND INVERSE ROOTER	Instantaneous voltage	Continuous	Instantaneous	Instantaneous	Ordinarily 1/2 and 2	Integral with vario-losser	Moderate	Full band	Full band	All

* Outside these ranges most of the devices tend to be linear transducers except for higher volumes applied to "limiters."

** It is important to note that these ranges are measured in the same units as the respective control circuits measure: *viz.*, "volume," "syllabic power," "voltage," etc.

voltage one can produce a device whose gain is a linear function in decibels of the input to the control circuit.

It will be recognized that if the application or removal of the control energy is retarded, the action of the control circuit may be made quite different on transient inputs than on steady-state inputs. It will appear later that this is the important distinction between some of the devices to be discussed and that fundamental differences in their functioning are thus brought about.

Referring to the figures once more it will be noted that some control devices are connected to the transmission path at the input to the vario-losser. These are known as "forward acting" control circuits. Other controls, connected at the vario-losser outputs, are known as "backward acting" control circuits. This is simply convenient terminology to indicate whether the control energy is progressing in the same direction as the main transmission or is progressing in a backward direction after traversing the main path, usually through a vario-losser. Some backward-acting controls function to measure the output of the devices containing them and to make whatever adjustments are required. Others are placed in that position to take advantage of the vario-lossers in the transmission paths, i.e., such controls could be replaced by combinations of forward-acting controls and extra vario-lossers.

In table I, nine of the volume-controlled devices* which have been developed for various commercial and experimental uses are listed with the functions of voltage, time, and frequency which are employed to obtain their respective performances. There is, of course, some latitude in the choice of these functions for any one device. Pending more complete description of the different types in the following paragraphs this table should be viewed as illustrating the general character of the different circuits and also the range of the variables which already have been employed. For example, it will be seen that instantaneous voltage of the signal wave, its short-time average value, peak power, syllabic variations, and long-time average power have all been used as criteria of gain settings in different circuits. Some devices change their adjustments only when critical values or ranges are exceeded, while others vary

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*** The names employed do not follow an entirely logical classification, but they are given here because they have had considerable usage. For the same reason the term "volume-controlled devices" is used, although to be strictly correct it might better be "sound-energy-controlled devices," for example, for not all the devices operate in accordance with "volume" as measured by the well-known class of visual-reading meters called "volume indicators."**

somewhat with every syllable if speech, for example, is being transmitted. Some are linear transducers to all but low or high amplitudes while others reduce or increase the output range from that at the input. It will be seen that proper choices of times for gain increase and gain decrease in combination with certain gain-control criteria make possible a wide variety of signal-altering means to meet different requirements.

Description of Devices in Table I

With this introduction to the combinations of characteristics which are possible it should be less difficult to distinguish between the specific devices discussed in the following paragraphs, which in addition to describing the devices, contain some comments which should assist in visualizing their forms and their operation.

1. The *vogad* (figure 1) is a device which will maintain at its output speech volume[1] which, over a certain range of input, is relatively independent of the speech volume applied to its input and which, in the ideal case, will not change its gain during periods of no speech input. It makes little or no alteration in the ratios of maximum and minimum instantaneous-to-average voltages of the speech.

2. The *volume limiter* (figure 3) is a device which is a linear transducer for all speech volumes up to a critical value, beyond which all input volumes produce essentially the same output volume. It is essentially different from the vogad in that its gain approaches the maximum value when input is removed.

3. The *compandor* (figures 4 and 5) is composed of a *compressor* and an *expandor*. A *compressor* is a device whose input-output characteristic on a decibel scale has a slope less than unity[*] and whose gain or loss is variable under control of the input energy at a time rate which will permit it to follow the syllabic rate of change of speech energy. Similarly, an *expandor* is a device whose input-output curve has a slope greater than unity and whose gain is variable at a syllabic rate under control of the input energy. Thus very shortly after all input is removed the gain of a compressor is maximum and the loss of an expandor is maximum. The reciprocal of the compressor characteristic slope is spoken of as the compression ratio, and the slope of the expandor characteristic is spoken of as the expansion ratio.

4. The *radio noise reducer*[8,9] (figure 6) combines the functions of an expandor which operates in the range of amplitudes where noise and weaker speech sounds lie and a linear transducer which comes into play for all amplitudes exceeding a critical value, which can be set to best suit the atmospheric noise conditions. In other words, the radio-noise reducer is a limited-range expandor. Inputs which are below the expandor range are subject to transmission at the minimum gain.

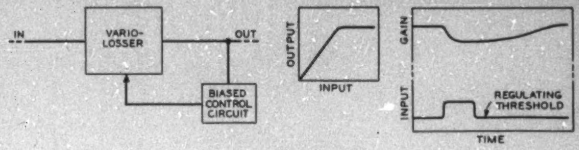

Figure 3. Volume limiter

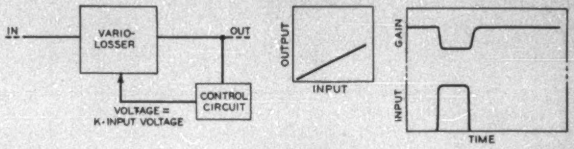

Figure 4. Compressor

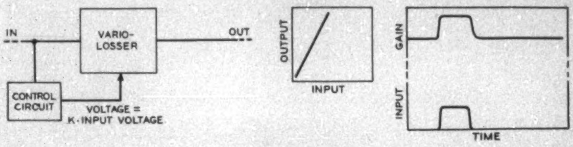

Figure 5. Expandor

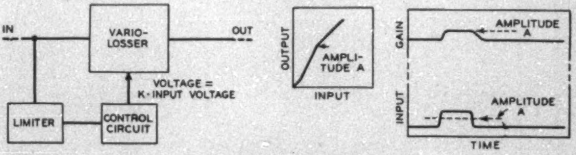

Figure 6. Limited-range expandor

Radio-noise reducer

5. The *limited range compressor* (figure 7) is a device whose operating range includes a region within which compression at a syllabic rate can take place; at other inputs the device is a linear transducer. Its connecting diagram and time functions are the same as those shown in figure 5 except that the control circuit contains a limiting device, so that compression takes place in only a portion of its input range, analogous to the action of the limited-range expandor of figure 6. As a special case the *limited range compressor* may have no linear range above its compression range, thus becoming one type of peak limiter.

6. The *peak limiter* (figure 8) is a device whose gain will be quickly reduced and slowly restored when the instantaneous peak power of the input exceeds a predetermined value. The amount of gain reduction is a function of the peak amplitude, and in practice is usually intended to be small to prevent material reduction of the range of intensity of the signal.

7. The *peak chopper* (figure 9) is a device which prevents transmission of peak amplitudes exceeding a critical amount, an essential characteristic being that the loss it inserts is completely determined by the instantaneous voltage of the signal; that is, its operating and releasing times are substantially equal to zero.

8. The *cross-talk suppressor* (figure 10) is a device which normally presents a prescribed loss to transmission, which loss is removed rapidly when the input amplitude exceeds a certain threshold and is reinserted at a definite time after the input is removed. It reduces low-amplitude unwanted currents such as cross talk but does not affect amplitudes in the useful signal-voltage range. This device differs from the limited-range expandor in that the time during which the low-loss condition is maintained is considerably greater, so the transition from one gain to the other occurs less frequently.

9. A *rooter* is an instantaneous compressor. Such a circuit can be made to produce an output whose instantaneous voltage is, for example, the square root or some similar function of the instantaneous voltage applied to the input. An *inverse rooter* is an instantaneous expandor whose characteristic is complementary to that of the rooter. A combination of *rooter* and *inverse rooter* will reduce the load requirements on a trans-

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[*] That is, if the input increases by x decibels the output increases by less than x decibels.

mission system between the two units but requires that it transmit a wider band of frequencies than that for the original signal, and that it be essentially free from phase distortion. This does not seem to be an attractive arrangement from a commercial viewpoint and is included here simply as an illustration of one of the possible modifications of signal energy. It is not shown in the group of diagrams.

Variants to the Devices Described

In addition to these there are various devices which are essentially modifications of those described. For example, a half vogad, figure 2, may have the same time functions as a vogad, figure 1, but the gain changes in the transmission circuit are half as great for the same range of input volumes. Thus in a vogad the range of gain changes in the transmission circuit is equal to the range of input volumes, so that the output volume is the same for all input volumes. In the case of the half vogad the range of gain changes in the transmission path is one-half the range of input volumes, so the output volume range is one-half that of the input. It is also possible to construct a vogad whose output volume range is any desired fraction of the input range. As another example of modification of the devices described, for special applications it may be desirable to incorporate a certain amount of syllabic compression in a vogad.

Communication circuits which have separate paths for oppositely directed transmission between the two terminals are usually operated at such an over-all loss that with ordinary terminations there will be little tendency for circulating currents to build up to a "singing" condition. Sometimes there may not be a great deal of margin, however, so that volume-controlled devices added to such circuits must add loss at some point to counterbalance whatever gain is put in at some other point. Thus a vogad inserted at the transmitting side of one terminal of such a circuit to amplify speech energy from weak talkers must be supplemented by a "reverse vogad" in the receiving side of the circuit. The reverse vogad is simply another vario-losser which is operated upon by the vogad control circuit in such a way that it always has a loss numerically equal to the gain of the vogad. Any vogad gain will be compensated by the reverse-vogad loss, so no greater tendency to sing will be effected by the addition of the combination to the circuit. In like manner half vogads must be used with compensating reverse half vogads.

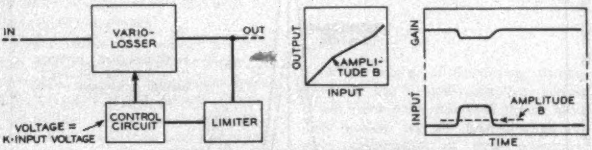

Figure 7. Limited-range compressor

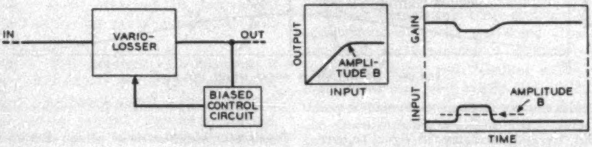

Figure 8. Peak limiter

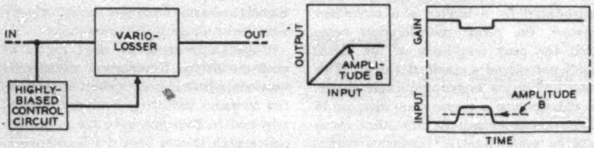

Figure 9. Peak chopper

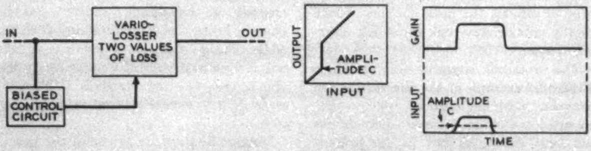

Figure 10. Cross-talk suppressor

Combinations of some of the devices also have interesting characteristics. For example, a combined radio-noise reducer and peak limiter at the receiving end of a circuit would suppress noise and would also reduce the amplitude of excessively high amplitude signals. Likewise, a vogad, compressor, and peak chopper in tandem in the order named could be made to reduce the range of input signals by a very large amount for transmission over a medium having only a small range between noise and maximum permissible signal. In this case it would be practically impossible to recover the original signal range at the receiving terminal of the medium, but the intelligibility of speech over such a system has been shown in the laboratory to be good.

Special compandors for high-quality service may require compression and expansion which varies with frequency. The exact characteristics will depend upon band width, program material, and transmission medium. For transmission media in which the noise reproduced at the receiving end is principally at the higher frequencies an unusual effect is obtained if the usual variety of compandor is used. Low frequencies unaccompanied by high frequencies will cause a gain change in compressor and expandor, thus changing the background of high-frequency noise which is not masked by the low-frequency signal energy. The resulting swishing noise has been given the onomatopoeic name of "hush-hush effect." To avoid this, recourse may be had to split-band compandors in which the compression and expansion is done only at high frequencies or separately for low and high frequencies. The successful application of the latter method is, however, more difficult than it appears from its simple description.

Distinguishing Characteristics

It is important to distinguish between the half vogad, figure 2 and the compressor, figure 4. As shown in table I the latter operates on syllabic variations and the former on the average volume of the input. Thus the half vogad reduces the range of output volumes to one-half that at the input while the compressor reduces the range of syllabic power at its output to one-half that at the input. In other words, the compressor reduces the ratio of peak to average power on constant-volume speech, while the half vogad simply adjusts for that volume and does not alter the peak ratio. There is, of course, the additional important difference that the half vogad retains its gain setting during silent periods while the compressor, by virtue of having followed the syllabic power, has its maximum gain during silent periods.

Volume limiters, figure 3, may be mistaken for vogads, figure 1, because during speech input above a certain value the two may produce the same output volume. They both employ something like a measurement of average power over periods longer than a syllable to determine their gain settings. The important difference is that a vogad retains its gain setting when speech currents are not present, while a volume limiter approaches its maximum gain during such periods. In terms of the output resulting from a range of input volumes there is another important difference if the volume limiter operates over only part of the input range: the vogad reduces the width of the distribution curve of volumes to a very small value, while the volume limiter moves all the area under the distribution curve above a certain point to the region near that point, which is its limiting volume. This is illustrated in figure 11, in which the calculated modifications of a volume distribution by a vogad and by a volume limiter are shown.

In the cases "without volume control" and "with a vogad" the distributions are normal, and the standard deviation, σ, has its usual statistical significance. With a volume limiter, only volumes above the limiting volume are affected, and these higher volumes are redistributed according to a normal law whose standard deviation is one decibel, as stated in the figure.

It is also important to distinguish between a peak limiter, and a peak chopper, figures 8 and 9. Naturally they resemble one another since they are intended to permit transmission of signals at higher average amplitudes without excessive loading of transmission circuits. However, they are intended for different classes of service and hence are not interchangeable except in some borderline cases. For the highest grade of transmission harmonic production must be negligible and the reduction in amplitude range of signals small and infrequent. Gain changes must be smooth, though rapid enough to compensate for practically any input wave to be expected. These characteristics are found in the peak limiter now being furnished for use on program networks and radio transmitters.[10,11] For services in which it is desirable to maintain the signal energy at a high value to override noise and in which harmonic distortion must be kept low a peak limiter with somewhat smaller time constants may be used. A high-ratio limited-range compressor might be suitable in this instance. This device would lower its gain a little more quickly on excessive inputs, and it would also reinsert its gain much more quickly; it would affect the naturalness of the sound of the signal more than the slower peak limiter but it would also cause the signal to override noise somewhat better. In a third variety of service the harmonic distortion introduced by a limiter is a secondary matter, the prime consideration being that the peak amplitude of the signal shall not exceed a specified value. This may be because higher-amplitude signals would produce a tremendous increase in distortion or cross talk into other channels or would damage expensive equipment farther along in the circuit. For these cases we may use the fastest possible type of limiter, the peak chopper, which simply cuts off any peak exceeding a certain value.

The cross-talk suppressor, figure 10, is a splendid example of the fine distinction between volume-controlled and voice-operated switching devices. This device has been described, but in the present state of the art its time functions have not been definitely fixed. If the characteristic of loss versus input is made steep enough and the speed of operation fast enough it will sound like a switching circuit and may in fact be replaced by a relay-switched attenuating network. If made somewhat slower and given a smaller slope of loss versus input it approaches the limited-range expandor or noise reducer.

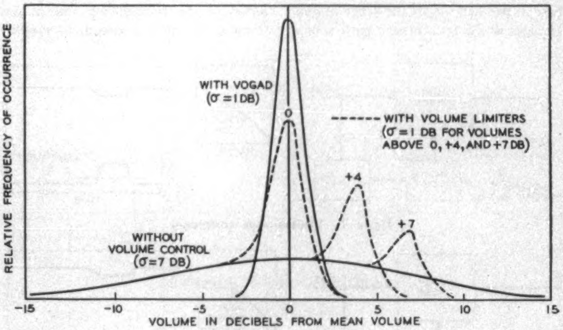

Figure 11. Modification of volume distribution by use of a vogad or a volume limiter

Applications and Expected Advantages

It may be of interest to give some approximate figures on the magnitudes of the advantages to be obtained by the use of some of these devices. It will be understood that the values to be given are simply illustrative, some having been obtained from field service on particular models and some from tests on laboratory equipment under special conditions.

Vogads appear to be most useful in such circuits as transoceanic radio connections, where it is important to operate the terminal switching equipment properly and to transmit over the radio circuit speech energy from loud and weak talkers equally well. It is essential in such cases that noise should not be increased in amplitude during speech pauses, hence the gain retaining feature of the vogad. On such a circuit a vogad will reduce a 45-decibel volume range to about two to four decibels. This is equivalent to expert manual volume control.

Volume limiters are in use at the present time to prevent peaks of speech energy in carrier circuits from "splashing" into telegraph channels.[7] Some five- to ten-decibel limiting is allowed on loudest talkers, which causes little degradation of the speech channels but makes possible the use of telegraph on the same carrier system. There is no widespread use of volume limiters in point-to-point radio service so far, but in cases in which there is no disadvantage in raising noise in silent periods of speech, such as in push-to-talk installations, proper transmitter loading can be obtained with volume limiters fairly cheaply.

One commercial model peak limiter, used as part of a program amplifier[10,11] is capable of introducing a considerable amount of compression without overloading on peaks, but for the preservation of adequate program volume range it is being recommended that only three-decibel peak limiting be allowed. This, of course, reduces the range of intensity of the program, but from the standpoint of the listeners it is equivalent to doubling the transmitted power or obtaining the same signal-to-noise ratio with half the transmitted power.

Limited-range compressors might be used either on land lines to insure full loading or on radio links whose fading is too severe to permit the use of normal compandors. There is no commercial application of either sort at the present time. Peak choppers are, however, used on some high-power radio transmitters which might otherwise be temporarily disabled by high peaks in the signal being transmitted.

The chief usefulness of compandors is on radio links in which the transmission of a compressed signal with subsequent expansion permits operation through higher noise or with lower transmitter power. On a long-wave transatlantic radiotelephone circuit a compandor with 40-decibel range has been shown to allow an increase in noise of some five decibels before reaching the commercial limit.[5] With smaller amounts of noise the noise advantage of the compandor approaches half its range in decibels. This benefit is sometimes applied to a reduction of transmitter power.

Radio noise reducers have been used to advantage in connection with short-wave ship-to-shore and transoceanic radiotelephone service. In the former, routine transmission rating is given on a judgment basis using a merit scale from one to five, five being practically perfect transmission and one so poor that intelligibility is very close to zero. It will then be seen that the observed improvement of one-half to one point in transmission rating due to the noise reducer is of considerable importance. Perhaps more graphic figures are those for transoceanic service, where the reduction of noise in the receiving path not only reduces the noise heard by the listener but also improves the voice-operated switching with the indirect result that at times receiving volume increases of 5 to 15 decibels are realized.[9]

As has been noted, the radio-noise reducer is a special use of an expandor alone. There are also two interesting applications for a compressor alone. The first, which uses a fairly high ratio of compression, has been mentioned as one type of peak-limiting device. The second, using a moderate ratio of compression, is in connection with announcing systems for use in very noisy locations. Its effect is to amplify weak sounds more than strong sounds, which considerably improves the intelligibility through high noise. For quiet locations it is of less value, since the speech sounds lose some of their naturalness in this process.

Conclusion

In the course of developing various types of the volume-controlled devices which have been described means have been worked out for providing almost any combination of time constants, range of control, and other characteristics which may be required. Some devices for which there were specific commercial applications or useful functional characteristics for experimental work have been constructed, with resulting advantages which have been briefly mentioned. There remain many possible ways to alter the characteristics of signal energy such as speech to which these methods are applicable and which await the special needs of new transmission problems.

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Testing and Application of Lightning Arresters

FOR some time, information dealing with the testing and application of lightning arresters has been needed. The two memoranda presented herein entitled "General Guide for Utilities on Methods of Testing Lightning Arresters" and "Factors Affecting Application of Arresters" have been prepared by the lightning arrester subcommittee* of the AIEE protective devices committee and provide such information.

†General Guide for Utilities on Methods of Testing Lightning Arresters

At the meeting of the AIEE lightning arrester subcommittee in New York, January 1937, it was decided that general information regarding methods of testing lightning arresters should be prepared. It was proposed that utilities make tests on new arresters, arresters removed from lines and accumulated in stock in a routine manner, and arresters in service, in order to determine the ability of such arresters to perform satisfactorily in service, as well as to augment, as much as possible, the somewhat limited amount of information now available on the subject of test limits.

The scope of this guide is mainly limited for the present to line-type arresters rated at 15 kv and less, such devices being constructed in accordance with the usual ideas of arresters, that is, with characteristic element having valve action against follow current in series with a gap or set of gaps.

It is desirable to obtain information on test methods, results, and characteristic limits for line and station type arresters rated above 15 kv, in order that this guide may later be expanded to include detailed information on preferred test methods and limiting values for all types and ratings of arresters. In the meantime for arresters rated above 15 kv, it will usually be preferable to obtain the assistance of the manufacturers in connection with the test methods to use and the interpretation of test results.

It is expected that, after more experience is obtained, the outline of test methods and the data on test limits will be expanded and improved as information becomes available.

Impulse Tests

Impulse tests are used to determine the surge protective characteristics of arresters, while 60-cycle tests indicate the internal condition of arresters and the relation of breakdown voltage to rated voltage. Impulse tests on new and used arresters should follow, in general, the test method prescribed in AIEE Standard No. 28. To determine the condition of the arrester when new or when removed from service, an impulse should be applied to the arrester and breakdown and impedance drop voltages obtained by means of a cathode-ray oscillogram. These values should be compared with corresponding values for new arresters as given for 3- to 15-kv arresters in the paper "Distribution Lightning Arrester Performance Data," ELECTRICAL ENGINEERING, May 1937, page 576, or as furnished by the arrester manufacturer for all ratings.

To determine the ability of an arrester to operate satisfactorily in service without change in its characteristics, the AIEE standard operating-duty test (AIEE 28-234) should be applied. As this test requires that the arrester be subjected to 60-cycle voltage during the application of impulses, it may be feasible to make such tests while the arrester is connected to the lines.

60-Cycle Tests

TEST CIRCUIT

Three suitable circuits for conducting tests at 60 cycles on arresters are illustrated in the attached figure 1. These circuits are essentially similar, comprising means for applying a variable voltage up to the breakdown voltage of the arrester, and meters for indicating the voltage applied to, and leakage current through, the arrester. An essential part of the test set is a current-limiting resistor R_1, located in either the low-voltage or high-voltage side of the test circuit, of such value as to limit the current during breakdown to a value which will not damage the arrester. Limiting values of from 3 to 15 milliamperes through the arrester

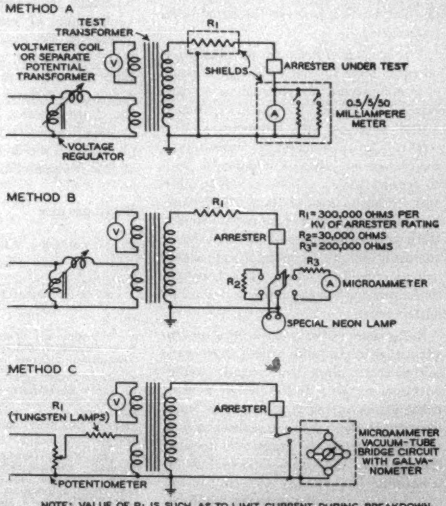
METHOD A
TEST TRANSFORMER
R1
VOLTMETER COIL OR SEPARATE POTENTIAL TRANSFORMER
V
SHIELDS
ARRESTER UNDER TEST
A
0.5/5/50 MILLIAMPERE METER
VOLTAGE REGULATOR
METHOD B
R1
V
R1 = 300,000 OHMS PER KV OF ARRESTER RATING
R2 = 30,000 OHMS
R3 = 200,000 OHMS
ARRESTER
R3
R2
A
MICROAMMETER
SPECIAL NEON LAMP
METHOD C
V
R1 (TUNGSTEN LAMPS)
ARRESTER
MICROAMMETER VACUUM-TUBE BRIDGE CIRCUIT WITH GALVANOMETER
POTENTIOMETER
NOTE: VALUE OF R1 IS SUCH AS TO LIMIT CURRENT DURING BREAKDOWN TO MANUFACTURER'S RECOMMENDED SAFE VALUE FOR ARRESTER (ABOUT 3 TO 15 MILLIAMPERES FOR VARIOUS TYPES)

Figure 1. Circuits for 60-cycle tests of lightning arresters

Paper number 38-43, recommended by the AIEE committee on protective devices and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9-12, 1938. Manuscript submitted December 27, 1937; made available for preprinting April 12, 1938.

† Prepared under the sponsorship of Herman Halperin.

* Personnel of the AIEE lightning arresting subcommittee: J. R. North, *chairman;* H. W. Collins, R. H. Earle, I. W. Gross, Herman Halperin, C. F. Harding, K. B. McEachron, J. R. McFarlin, A. M. Opsahl, A. H. Schirmer, H. K. Sels, L. G. Smith, A. H. Sweetnam, and J. J. Torok.

have been used or suggested. For the test circuit shown as method *B*, the limiting leakage current during breakdown may be as low as three milliamperes. For testing arresters with different voltage ratings, the resistance should be variable or provision made for the insertion of resistors of proper values.

For methods *A* and *B*, the drop of voltage in the resistor R_1 in the circuit directly including the arrester should be taken into account in determining the voltage across the arrester.

Control of the test voltage may be accomplished by either a regulator or a potentiometer. If a voltmeter coil in the test transformer or a separate potential transformer is used to measure the applied voltage, the voltmeter should preferably be calibrated against the crest voltage applied to the arrester, but should give readings of root-mean-square values of the equivalent sine wave.

In one of the circuits shown, a neon tube is used to detect breakdown; the others use the voltmeter or microammeter indication. For the latter case the meter should be able to withstand the current during breakdown without being damaged. This meter, to accomplish its primary purpose of measuring leakage current through the arrester, should have a range of from about 10 microamperes to 3 or 5 or 15 milliamperes.

Care should be taken that stray electrostatic fields or capacitive currents do not affect the leakage current measurements. This may be prevented by suitable shielding, as indicated in two of the sketches, or by physically isolating the microammeter and arrester from each other and from the remaining testing equipment. If desired, the arrester under test may itself be shielded by placing it in a grounded metal box.

TEST PROCEDURE

1. In 60-cycle tests, there must be no other voltage on the arrester than the test voltage. This means that, for arresters installed on the system, the arrester must be disconnected from the lines before being tested for breakdown voltage.

Sixty-cycle leakage current measurements at operating voltage, however, can possibly be made on the arrester as installed. Due to the necessity for careful shielding against stray fields, such testing of arresters in service should be considered as developmental work and investigated by each utility planning tests on installed equipment.

2. With the microammeter in the test circuit, the leakage current through the arrester is usually measured at the rated voltage of the arrester.

3. The microammeter is either cut out of the circuit or shunted by resistance. The test voltage is then gradually increased to the breakdown point of the arrester. The breakdown voltage and breakdown characteristic (whether sharp or gradual) are indicated by the neon lamp, if used, or by the voltmeter or shunted microammeter.

SUGGESTED TEST LIMITS

Insufficient data are as yet available to indicate sharply the test limits differentiating between satisfactory and unsatisfactory arresters removed from service. For satisfactory three-kv line type arresters, one utility has found from its experience and examinations of used arresters that the upper limit of leakage current should be about 150 microamperes at the normal operating voltage, that is, 2,300 volts. For other voltage ratings, this value might be subject to modification because of varying ratios of test (or operating) voltage rating. Further work seems necessary to establish proper values for all voltages.

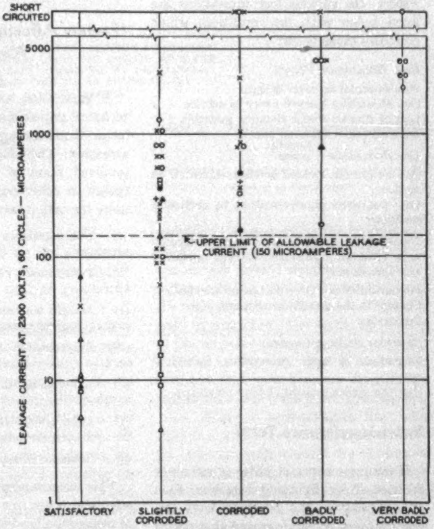

Figure 2. Comparison of leakage currents of used three-kilovolt arresters with condition of gap found on examination

Each symbol represents a different type of arrester

Arresters of modern manufacture generally have 60-cycle breakdown values between 1.5 and 3.0 times the rated arrester voltage. These ratios result in the limits shown in table I.

Table I

Rated Voltage (Kv)	Usual Limits of 60-Cycle Breakdown Voltages (Kv-RMS)	
	Minimum	Maximum
3	4.5	9
6	9	18
9	13.5	27
12	18	36
15	22.5	45

Due to the differences in designs, it will probably be desirable to determine the ratios for each manufacture, type, and vintage of arrester.

It has been found occasionally for modern arresters, especially for some removed from service, that the 60-cycle breakdown voltage for satisfactory arresters has been outside the limits given in the above table. In such cases the variation is usually on the low side; that is, the 60-cycle breakdown was found to be 1.2 to 1.4 times the rated voltage.

For arresters of older designs, the limits of 60-cycle breakdown voltages have been found for apparently satisfactory arresters to vary between 1.1 and 3.5 times the rated voltage. Usually, however, the test limits have been between 1.5 and 3 times the rated voltage. The outstanding exception, however, has been reported for some old three-kv arresters of two types where the breakdown voltage for apparently satisfactory arresters has been found to be as high as about five times the rated voltage.

In order to set up adequate test limits, both for leakage current and for breakdown value, which may be used to differentiate between satisfactory and defective arresters, it is suggested that each utility, in the preliminary stages of its 60-cycle arrester testing, conduct an investigation on the various classes of arresters removed from service. The recommended procedure is to test each arrester, then dissect particularly those having suspicious test values, and inspect for internal defects. The attached figure 2 indicates the procedure followed by one utility, for example, in determining the proper maximum limit of leakage current for three-kv line-type arresters. The values given above for breakdown voltage limits should also be checked in a similar way and modified, if necessary.

As a guide in analyzing the data obtained in examinations of dissected arresters, the various test indications are listed below with the conditions which generally cause them:

High Breakdown Voltage

Fundamental arrester design.
Gap electrodes burned away in service.
Loss of characteristic element material.
Destruction of internal parts.

Low Breakdown Voltage

Gap electrodes welded or short-circuited in service.
Gap electrodes short-circuited by corrosion products.

Unsatisfactory Discharge Action on Breakdown (gradual breakdown, or high resistance after breakdown)

Fundamental arrester design or assembly.
Change in characteristic element.
Corrosion.

Excessive Leakage Current

Corrosion of gap assemblies (principal cause).
Gap electrodes welded or short-circuited.

Radio-Interference Tests

If frequent cases of radio interference are caused by lightning arresters, then in addition to the 60-cycle tests described above, it is suggested that radio-interference tests be made on arresters with rated voltage applied. Tests should be made in accordance with Edison Electric Institute Publication No. C-9 "Methods of Measuring Radio Noise."

Correlation of Surge and 60-Cycle Data

In order to determine whether any correlation exists between surge data, 60-cycle data, and arrester condition, it is suggested that, where facilities are available, arresters be tested by both methods. The greatest amount of information is to be obtained by testing arresters which are to be dissected for examination.

Co-ordination of Data

Data covering the testing experience of each utility should be forwarded for co-ordination to the subject committee sponsor, Herman Halperin, Commonwealth Edison Company, Chicago, Ill. Such data should include the following: testing circuits and methods, limits of and detailed data used in arriving at limits of acceptable test characteristics, correlation of surge and 60-cycle data, and general test results and conclusions. It is also desirable to follow and report on the protective performance obtained in service with used arresters which were tested before reuse.

*Factors Affecting Application of Arresters

A great deal has been published in technical papers and manufacturers' bulletins about the application of lightning arresters. There are a number of factors involved, many of which have been discussed in other papers. Some of these more important factors are:

(*a*). Characteristics of equipment to be protected.

(*b*). Performance characteristics of arresters.

(*c*). Margin between arrester performance and equipment characteristics.

(*d*). Deterioration of arresters and equipment.

(*e*). When to use line-type or station-type arresters.

(*f*). Influence of circuit feet between arrester and equipment to be protected.

(*g*). Influence of multiple arresters.

This memorandum deals only with the selection of arrester ratings and methods of connection for distribution transformer protection. Certain factors such as when to use line-type versus station-type arresters, influence of distance between arresters and protected equipment, and influence of multiple arresters, are largely matters of individual application.

Selection of Arrester Rating

The principal factors that should be considered in the selection of the rating of a surge protective device, such as a lightning arrester or a gap, are:

(*a*). The maximum dynamic voltage which may occur between sound phase and ground under any fault conditions. The device should withstand this voltage without failure.

(*b*). The efficacy of the protective device in maintaining a low protected level and at the same time meeting satisfactorily the system operating requirements. The impulse volt-time characteristics of the device must be below that of the equipment to be protected.

(*c*). The surge current which must be satisfactorily discharged. The thermal ability of the device must be adequate for the currents expected, and the voltage drop across the arrester during discharge must be sufficiently low to provide satisfactory protection.

The voltage rating of an arrester represents the maximum dynamic voltage which the arrester can withstand continuously and which it can interrupt after functioning. The arrester rating must be not less than the maximum dynamic voltage which may occur between the arrester terminals during disturbances, allowing for the power system neutral being isolated, or grounded, as the case may be, and considering the actual maximum operating voltage. Allowance must also be made for the voltage recovery rate and any overvoltage conditions which may exist due to the overspeeding of hydroelectric generators, etc. The protection afforded by a lightning arrester is approximately proportional to its voltage rating and, therefore, it is important to use an arrester of as low a voltage rating as possible.

The normal "full voltage" rated arrester is designed to operate on an ungrounded or isolated neutral system where the voltage existing between line and ground may at times equal the normal line-to-line voltage. The arrester rating selected is generally five per cent or more above the maximum operating line-to-line voltage. Under favorable conditions where the system neutral is effectively grounded, better protection can be obtained by the use of an arrester having a reduced voltage rating, provided the upper limit of dynamic voltage of the arrester is not exceeded under any operating or fault condition. In the past, "grounded neutral" arresters have had voltage ratings approximately 80 per cent of the "full rated" arresters. This gave a wide margin above the normal system line-to-neutral voltage but a margin which is required to allow for fault voltages, etc.

For the purpose of determining the allowable arrester voltage rating, the maximum dynamic voltage which may occur during fault conditions between the sound phases and ground may be calculated by the method of symmetrical components ("Symmetrical Components" by Wagner and Evans, McGraw-Hill Book Company). Under certain conditions, a two line-to-ground fault may be slightly more severe than a single line-to-ground fault.

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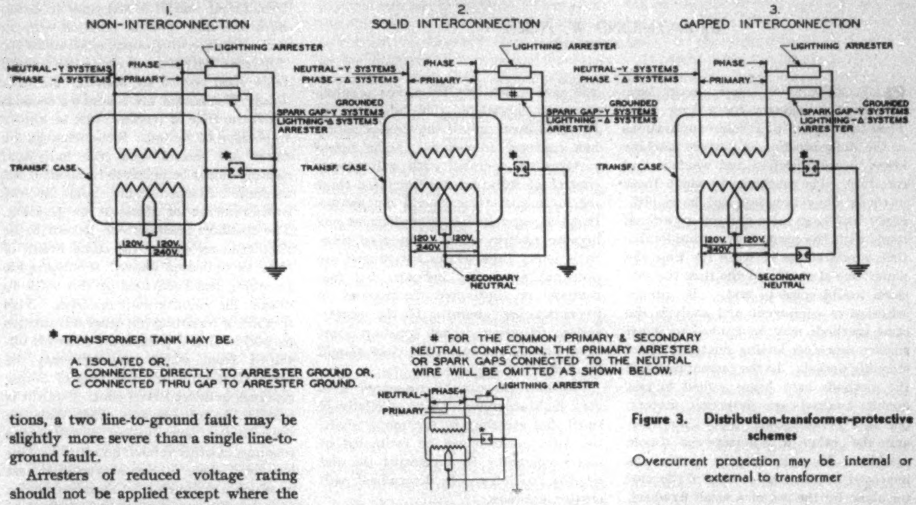
*Prepared under the sponsorship of J. R. North.



* TRANSFORMER TANK MAY BE:

A. ISOLATED OR,
B. CONNECTED DIRECTLY TO ARRESTER GROUND OR,
C. CONNECTED THRU GAP TO ARRESTER GROUND.

# FOR THE COMMON PRIMARY & SECONDARY NEUTRAL CONNECTION, THE PRIMARY ARRESTER OR SPARK GAPS CONNECTED TO THE NEUTRAL WIRE WILL BE OMITTED AS SHOWN BELOW.

Figure 3. Distribution-transformer-protective schemes

Overcurrent protection may be internal or external to transformer

Arresters of reduced voltage rating should not be applied except where the neutral is "effectively grounded" and the following conditions are met:

(*a*). The neutral of that portion of the system must be grounded at all times and under all operating conditions.

(*b*). The ratio of zero sequence reactance (including neutral reactance) to positive sequence reactance or negative sequence reactance X_0/X_1 or X_0/X_2 must be equal to two or less under the most severe fault conditions. Due consideration must be given to the circuit impedance as affected by sequential operation of circuit breakers.

It is recommended that the arrester voltage rating be at least ten per cent above the calculated maximum sound phase voltage to ground.

As a rough rule of thumb, in many cases, it will be found that:

1. If $X_0/X_1 = 2$ or less, an 80 per cent arrester may be used.
2. If $X_0/X_1 = 1$ or less, a 70 per cent arrester may be used.

These latter percentages should be applied to the *maximum* root-mean-square line-to-line voltage, which may exist under the most unfavorable operating condition, in determining the allowable arrester rating.

Connections of Lightning Protective Equipment on Distribution Circuits

The problem of protecting transformers against lightning is essentially a question of definitely limiting the surge voltage stress on the transformer insulation to values that it can withstand by the proper use of arresters or gaps, while coincidently providing a suitable path to ground for the surge.

Conventional practice provides a more or less suitable ground path for the surge by the use of lightning arresters or gaps connected between line and ground, but does not provide for the coincident necessity of definitely limiting the voltages that can appear across the transformer insulation during the surge discharge, except in the limiting case of negligible arrester ground resistance. As a consequence, primary to secondary flashovers frequently occur, resulting at least in blown transformer fuses with attendant service interruptions, if not in equipment failure, etc.

By providing suitable "interconnection" between arresters, secondaries, and tanks, the surge potential across the transformer insulation may be limited to the voltage drop across the terminals of the protective devices themselves which, in the event the latter are suitably chosen, will eliminate the great majority of the transformer service outages otherwise experienced. The benefits to be derived from interconnection are not limited to single-phase installations, but may be extended to include the customary polyphase installations as well.

It is not essential to permanently ground the transformer tank, from the standpoint of protecting the transformer itself. While interconnection eliminates the arrester ground resistance as a factor in the protection of the transformer itself, the necessity of providing a low resistance path to ground in order to finally dispose of the surge is just as important as ever.

There are various ways of connecting arresters and gaps between distribution transformer leads, the tank and ground. Also, different nomenclature has been used to describe these various connections. In order to avoid lengthy descriptions or drawings every time a certain connection scheme is mentioned, it is very desirable to adopt a uniform system of titles.

Figure 3 shows the more common connections used for protecting distribution transformers. The numerous schemes of connections have been grouped into three general classes and each class subdivided to cover the more detailed connections. This method of identification makes it possible to describe the scheme of connections used by simply stating whether a "non," "solid," or "gapped" interconnection is used and whether the transformer tank is isolated, grounded, or connected to ground through a gap.

An Application of Deceleration Test Methods to the Determination of Induction-Motor Performance

By RAYMOND W. AGER
ASSOCIATE AIEE

RETARDATION tests have been used by industry for a long time. They have found their main applications in the determination of certain machine losses, usually friction and windage and core loss. The machines to which these methods have been applied have ordinarily had large mass and low rotational losses with the result that a considerable time would elapse between the time the power was shut off and the time the machine would come to rest. By proper selection of equipment and analysis the same methods may be applied to much smaller machines having relatively short stopping periods. In the present instance the methods have been applied to two double squirrel-cage induction motors, one a $7^{1}/_{2}$-horsepower 6-pole machine, and the other a 5-horsepower 4-pole machine. In each case the inertia was increased to about twice that of the motor alone by the use of a small flywheel. A similar analysis has been applied to acceleration tests on these two motors in which the motors were started at reduced voltage. By combining the acceleration tests with the decleration tests the speed-torque curves of the motors have been obtained. It is hoped that the discussion of the difficulties and the results of these tests will help to indicate the value and limitations of the methods as applied to similar determinations in this and other fields.

Quantities Involved and Their Measurement

There are five quantities involved in deceleration tests. These are inertia, time, position, velocity, and acceleration.

Inertia may be determined by calculation or test with the required accuracy. Its determination is not considered a part of this paper.

Time may be measured with extremely high accuracy. In the measurement of very short periods of time, however, a high-grade stop watch is almost useless since the registration does not go below one-fifth second. The synchronous clock will give indications to as fine a subdivision as desired but this indication can be relied upon only if the driving source has constant frequency. Some power systems are operated with a frequency control of sufficient accuracy for these measurements. One should not assume that the power frequency is constant just because electric clocks keep good time. Satisfactory time service for general use does not necessarily indicate that momentary or continuous fluctuations in frequency are absent. If the power-system frequency is not constant some other driving source for the clock should be used.

Position also can be measured with very high accuracy. Using a relatively small dial attached to the motor shaft, the rotor position can be estimated to 0.01 revolution. By increasing the dial size the position can be determined with greater accuracy.

Velocity is a derived quantity and cannot be measured directly with the accuracy with which one can measure fundamental quantities. Tachometers, whether electrical or mechanical, are subject to errors several times those which may exist in the measurement of time or position. The velocity may be determined more accurately from a properly taken distance-time curve than by direct measurement using tachometers. Another difficulty encountered in the use of tachometers is the load which the instrument places upon the machine being tested. If the machine being tested is small, the instrument may represent a burden which will mask the effects it is desired to measure.

Acceleration is a derived quantity of the second order. Satisfactory instruments for the measurement of accelerations as small as those found in deceleration tests are not available. The determination of acceleration must therefore be based upon calculations involving the velocity, which for best results must in turn be based upon the distance-time curve.

Since so much depends upon the distance-time curve, very high accuracy is required in its determination. It has been found that it is necessary to determine the time and the position with an error not exceeding about 0.002 times the difference between successive readings if truly good results are desired. In other words, if readings are taken ten seconds apart the time of reading must be known to about 0.02 second. If the readings are taken one second apart the individual readings must be accurate to about 0.002 seconds. Similar limits hold for the measurements of distance or position. The readings must also be known to be simultaneous within the same limits of time as indicated above. Photographic recording has been used in this work to obtain the simultaneous readings. This method of recording has other advantages as well in that a permanent record is obtained from which readings may be checked at any time, the rate of taking readings is under better control, and it is possible to photograph other dials on these same films giving simultaneous readings of other related quantities. This has been done in one acceleration test in which a voltmeter, an ammeter, and a polyphase wattmeter were photographed along with the time and displacement dials and their readings used to get the electrical characteristics of the motor.

Apparatus

The timing device was a synchronous clock, made for checking relay settings, etc. It was of the continuously rotating type and did not have the intermittent motion of the cycle counter. The clock had two hands, one of which made one revolution per second, and the other made one revolution in ten seconds, when supplied with 110-volt 60-cycle alternating current. The dial was calibrated with 100 divisions and time could be estimated to 0.001 second.

The position indicator was a watthour meter register which had the units dial directly coupled to the motor shaft. The coupling was by means of a tightly wound piano-wire flexible shaft slipped over a pin set in the center of the motor shaft on one end, and over the shaft of the units dial of the register on the other end. With the two shafts in line the hand appeared to follow the shaft movement accurately.

Paper number 38-107, recommended by the AIEE committee on electrical machinery and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted January 10, 1938; made available for preprinting July 7, 1938.

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The rotor position was estimated to 0.01 revolution.

The photographic equipment consisted of a candid camera clamped into position and accurately focused on the dials. The camera had a focal-plane shutter which was wound and tripped by hand. The camera had a maximum capacity of about 40 pictures at one loading. This number of pictures was adequate for most tests, although a slightly larger number could have been used to advantage at times. The marked shutter speeds ranged up to $^1/_{500}$ second but the actual exposure was found to be about twice this. Shorter times of exposure would have resulted in better images of the faster moving pointers, but the values obtained by estimating the position of the centers of the blurs caused by the moving pointers seem to have all the accuracy required. A distance of 30 inches between the camera and the dials was found to be satisfactory using the two-inch-focal-length lens.

The dials were illuminated by "photoflood" lamps. The lens was stopped down so that the resulting negatives would be much thinner than would be used for the more normal photographic work since it was found to be easier to read these thinner negatives. The readings were taken from the films using a pocket magnifying glass of about six power.

Standard portable-type voltmeters, ammeters, and wattmeters were used in the acceleration test in order to get the complete electrical characteristics. Figure 1 shows these instruments, the synchronous clock, and the position-indicator dial as they were grouped around the motor in the acceleration test.

Calculations

Because of the high accuracy of the data taken from the films, the velocities may be calculated from the differences between successive readings. That is, if the difference in distance (revolutions) as read from two successive films is divided by the difference in time as read from the same films, the average velocity during the interval is obtained. This calculation is indicated in table I, which lists the readings for the acceleration test of the $7^1/_2$-horsepower motor. The first and second columns contain the distance and time readings as they were taken from the films. The third and fourth columns show the differences referred to above, while the fifth column lists the quotient of these differences or the average velocities during the intervals. If each interval is of short enough duration so that the change in velocity is small during that interval, this average velocity may be considered as the true velocity at the time corresponding to the middle of the interval. How close this will be to the truth will depend upon the curve to which the calculations are applied. No approximations are involved if the velocity is a straight-line function of the time. The times corresponding to the velocities listed in the fifth column of table I are shown in the sixth column. However, if one is interested in the velocity-distance curve, the average velocity found above may be considered as the true value at the mid-distance of the interval. The accuracy of this approximation again depends upon the shape of the curve. In this case no approximation is involved if the velocity is a straight-line function of the distance. It is to be noted that the mid-distance and the mid-time points do not fall on the true curve of time and distance, but on straight lines joining successive points on the original curve.

It might seem that the velocity could be obtained by graphical means from the distance-time curve. It was not found to be feasible to do it this way, since the plotting of the curve could hardly be done with an accuracy comparable with that of the original data, unless the plot were made to an enormous scale. The process of calculating differences as outlined above results in accurate velocity data with a minimum amount of manipulation.

Now if a sufficient number of readings of distance and time have been obtained in the original data, a velocity-time curve or a velocity-distance curve may be derived having high enough accuracy for use in determining the acceleration. The slope of the velocity-time curve is equal to the acceleration. If one wishes to use the velocity-distance curve, he may find the acceleration by multiplying the velocity by the slope of this curve. This relationship may be shown as follows:

Since

$$v = ds/dt$$

and

$$a = dv/dt$$

$$\frac{a}{v} = \frac{dv}{dt} \times \frac{dt}{ds} = \frac{dv}{ds}$$

or

$$a = v\frac{dv}{ds}$$

It is to be noted that in either case the slope of a curve must be found. The accuracy with which one can determine the slope of an experimental curve depends upon the shape of the curve and the accuracy with which the location of the curve is known. The supposedly slight variations which would be expected in the judgment of curve location may produce relatively large changes in the slope of adjacent parts of the curve. The easiest and most accurate curve to draw, and the easiest one from which to determine slopes is that curve which approximates a straight line. Also, as inferred in the preceding paragraphs, this would be the one which would have the better approxi-

Table I

Revolutions	Time	Revolution Difference	Time Difference	Velocity	Time	Velocity Difference	Time Difference	Acceleration	Velocity
143.24	0.002	0.37	1.516	0.24	0.760				
143.61	1.518	2.85	2.058	1.39	2.547	1.15	1.787	0.644	0.84
146.46	3.576	3.84	1.519	2.53	4.336	1.14	1.789	0.638	1.96
150.30	5.095	4.92	1.436	3.42	5.813	0.89	1.477	0.602	2.97
155.22	6.531	6.45	1.504	4.29	7.283	0.87	1.470	0.592	3.85
161.67	8.035	7.79	1.522	5.11	8.796	0.82	1.513	0.542	4.70
169.46	9.557	9.28	1.554	5.97	10.334	0.86	1.538	0.560	5.54
178.74	11.111	11.75	1.719	6.84	11.971	0.87	1.637	0.531	6.41
190.49	12.830	12.69	1.636	7.75	13.648	0.91	1.677	0.542	7.30
203.18	14.466	14.54	1.694	8.58	15.313	0.83	1.665	0.498	8.17
217.72	16.160	15.85	1.685	9.41	17.002	0.83	1.689	0.491	9.00
233.57	17.845	17.74	1.730	10.25	18.710	0.84	1.708	0.492	9.83
251.31	19.575	18.99	1.715	11.07	20.433	0.82	1.723	0.476	10.66
270.30	21.290	19.90	1.679	11.86	22.130	0.79	1.697	0.466	11.47
290.20	22.969	19.91	1.583	12.58	23.761	0.72	1.631	0.441	12.22
310.11	24.552	22.83	1.718	13.29	25.411	0.71	1.650	0.430	12.94
332.94	26.270	26.01	1.850	14.06	27.195	0.77	1.784	0.431	13.67
358.95	28.120	22.55	1.534	14.70	28.887	0.64	1.692	0.378	14.38
381.50	29.654	24.71	1.615	15.30	30.462	0.60	1.575	0.381	15.00
406.21	31.269	25.14	1.576	15.95	32.057	0.65	1.595	0.407	15.62
431.35	32.845	28.77	1.741	16.52	33.715	0.57	1.658	0.344	16.23
460.12	34.586	29.78	1.740	17.12	35.456	0.60	1.741	0.344	16.82
489.90	36.326	28.73	1.626	17.68	37.139	0.56	1.683	0.333	17.40
518.63	37.952	29.23	1.613	18.12	38.758	0.44	1.619	0.272	17.90
547.86	39.565	31.35	1.690	18.55	40.410	0.43	1.652	0.260	18.34
579.21	41.255	32.62	1.721	18.96	42.115	0.41	1.705	0.240	18.75
611.83	42.976	34.06	1.782	19.12	43.867	0.16	1.752	0.091	19.04
645.89	44.758								

mations. One should therefore use the curve which more closely approximates a straight line.

These graphical methods for determining the acceleration from the velocity curves were used in the preparation of this article. As an extension of the method of taking differences to the determination of acceleration the last four columns of table I have been calculated, and further indicate the surprising accuracy of the data. The figures shown in the seventh and eighth columns are the velocity and time differences calculated from the values shown in the fifth and sixth columns. The quotients obtained by dividing these velocity differences by the corresponding time differences are the accelerations and are listed in the ninth column. The last column contains the velocities corresponding to the accelerations.

Accuracy of the Data

Although the foregoing shows very well the high accuracy which may be obtained, further improvement is possible so that some consideration of the sources of errors may be of value.

First consider the ability of the dials to follow time and position. The time dial should record time with the same accuracy as that with which the frequency is held constant at rated value. The tests which are reported were made on two large power systems. In one case the frequency fluctuations were of no consequence and the results are entirely reliable on this point. In the other case the frequency fluctuations were such that the results are somewhat in question. In this case the results of duplicate tests were similar but not identical. The distance or position dial should follow the motion of the rotor if the coupling is rigid. At first the coupling used was thought to be satisfactory as long as the shafts coupled by it were in line and the coupling was kept from vibrating. It is now believed that the greatest inaccuracies in this work originate here.

Figure 1. General view of the instruments grouped for photographing in the acceleration test

Since the camera had a focal-plane shutter the time of recording was not simultaneous over the entire picture. The progression of the exposure across the film should be the same in each picture and so should cause no errors due to the placing of the dials. However, an error will exist due to this sweeping action, since different portions of a single dial are photographed at slightly different times. Some irregularities in the curves have been traced to this source.

Procedure

From the above the general procedure should be obvious. Some of the details of procedure which must be watched will bear discussion. Bearing friction is a determining factor in these tests. Its value will vary with the bearing temperature, the load on the bearings, the axial position of the rotor, and the thickness of the oil film, as well as with the motor speed, the kind of oil used, and the general design.

Bearing temperature is very important. Changes in bearing temperature may affect the results to such an extent that any conclusions drawn from the tests may be entirely erroneous. This statement applies to ball-bearing machines as well as to those equipped with sleeve bearings. On successive tests on a new ball-bearing machine the bearing friction varied in the ratio of four to three although the time of running before the second test was only slightly greater than that before the first. The preferred procedure would be to run the motors to constant bearing temperatures before taking readings.

The addition of a flywheel to a motor may change the bearing friction and also the windage. In these tests all runs were made with identical flywheel loading and hence the effects of the flywheel should cancel out of the final result.

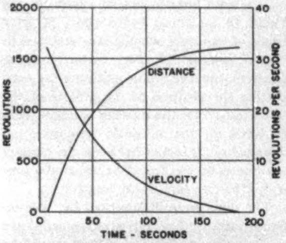

Figure 2. Distance-time and velocity-time curves for the deceleration test on the $7^1/_2$-horsepower 6-pole motor

The axial position of the rotor may affect the bearing friction, or, by changing fan clearances, change the windage loss. This difficulty is not experienced in ball-bearing machines where end-play is restricted, but in sleeve-bearing machines there may be a tendency for the rotor to shift from its normal position during the test unless the shaft is carefully leveled and perhaps lightly restrained.

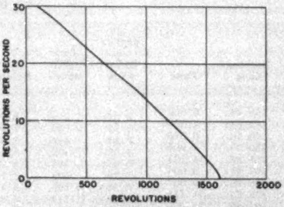

Figure 3. Velocity-distance curve from the test of figure 2

In deceleration tests the oil film should be properly maintained by the motion of the rotor. In acceleration tests, starting from rest, it may take several revolutions before the oil film is built up to its proper value. It is therefore important to start acceleration tests after a period of running without allowing time for the rotor to settle and squeeze out the oil film.

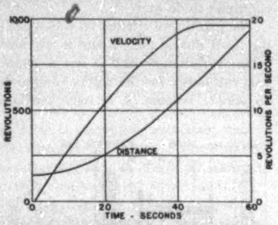

Figure 4. Distance-time and velocity-time curves from the acceleration test on the $7^{1}/_{2}$-horsepower 6-pole motor

In trying to avoid the change in the oil film in the acceleration tests two methods were employed. The first was to run the machine and then allow it to coast to rest. The test was started within about one second of the time the motor stopped. The second method was to run the machine in the reverse direction and then to plug it on the line. Recording was started as the motor reversed. In analyzing the results of the tests made using the first method it was found that there was an apparent delay in the starting of the motor. In figure 4 this delay is indicated. In this test the power was applied at the time $t = 0.002$. This apparent delay is believed to be due to the inductance of the rotor circuit, which does not permit the field to build up instantaneously, and to transients which may exist at the closing of the line switch. The net result is that full torque is not available until some time after the closing of the switch and, therefore, the speed increases more slowly than it should at first. By plugging the motor on the line the time required for the building up of the field and the decay of the transients was provided before recording was started.

On deceleration tests the motor field tends to collapse on the removal of the applied voltage but is held trapped by the short-circuited rotor winding. Therefore, during the first few seconds the losses of the machine which must be supplied by the rotational energy include a stator core loss. Only after the rotor resistance has dissipated the energy of the field does this loss become zero and the friction and windage losses remain alone. A simple means of avoiding this core-loss error is to supply the motor with a higher frequency, running it at overspeeds so that the field will have become negligible by the time the speed has decreased to normal.

Since the camera used had a limited number of exposures, it was necessary to space these properly throughout the time of the test. In the deceleration tests the velocity-distance curves approximated straight lines and so were used in preference to the velocity-time curves. Since distance was used for the abscissa in these curves, a fairly uniform spacing of points on the curves was obtained by taking readings at approximately equal intervals of distance. In the acceleration tests, however, the velocity-time curves were more nearly straight lines and were therefore used in determining acceleration. For these tests proper spacing of points was obtained by taking readings at approximately equal intervals of time.

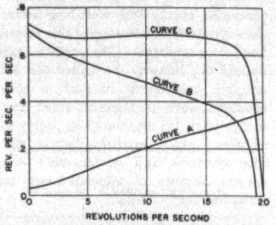

Figure 5. Acceleration and deceleration curves derived from the curves of figures 3 and 4

Curve *A*—Deceleration versus speed in the deceleration test

Curve *B*—Acceleration versus speed in the acceleration test

Curve *C*—Electrical torque versus speed

Curves

Figure 2 shows the distance-time and the velocity-time curves of the $7^{1}/_{2}$-horsepower 1,200-rpm 60-cycle motor in the deceleration test. The motor was run at overspeed by supplying power to its stator at about 100 cycles. In figure 3 the same data are used to plot the velocity-distance curve. It is believed that some of the waviness of the curve is due to fluctuations in the frequency of the supply used to drive the synchronous clock. Figure 4 shows the distance-time and the velocity-time curves of the same motor when accelerated from rest by applying three-phase 60-cycle power to the stator. The voltage was about 16 per cent of rated value. The power was applied at the time $t = 0.002$. The apparent delay in starting in this case amounts to about 0.5 seconds. It is probable that the curve should not plunge into the axis at this point but should contain a reverse bend which would bring it over through the origin. It is also probable that the transients causing this apparent delay have decreased the slope of the curve near the origin so that the torques calculated for the lower speeds are too low.

Curve *A* of figure 5 shows the deceleration as calculated from figure 3, curve *B* shows the acceleration as calculated from figure 4, and curve *C* shows the sum of the ordinates of *A* and *B* and gives the speed-torque curve of the motor. This speed-torque curve of the motor is redrawn in figure 6 in combination with curves showing current and power input. The inertia, spring constants, and damping of the instruments used to measure the current and power have undoubtedly affected the readings near zero speed since power was applied at that time. A slight effect may also be present at the higher speeds but except near zero speed the pointer movements were so slow that errors due to this should be negligible.

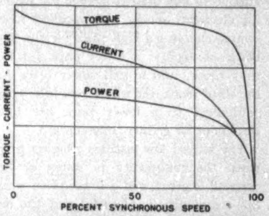

Figure 6. Complete starting characteristics of the $7^{1}/_{2}$-horsepower 6-pole motor

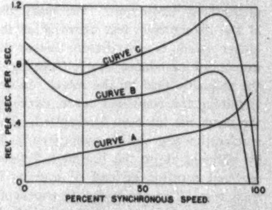

Figure 7. Deceleration, acceleration, and torque curves from the tests on the five-horsepower 4-pole motor

In figure 7 are shown the curves obtained in tests on a five-horsepower 1,800-

rpm 60-cycle motor. In this case only 60-cycle power was available and the deceleration test was started from approximately 1,800 rpm. The sharp rise in the deceleration test curve *A* at the higher speeds is undoubtedly due to the iron losses introduced by the trapping of the magnetic field by the rotor. In determining the combined curve, curve *C*, this sharp rise has been disregarded. The acceleration test on this motor was made by plugging it on the line at full-speed reversed rotation instead of starting it from rest. The errors due to transients at the start have thus been eliminated. For this motor dynamometer tests were available in which starting torque, pull-out torque, and the minimum torque between these two had been determined. The ratios of these torques as determined in these tests and as determined in the dynamometer tests checked within about two per cent. An agreement as close as this was somewhat unexpected considering some of the known errors and the widely different voltages used.

Conclusions

These tests would indicate that the method is quite usable and that the results should be quite satisfactory if the proper precautions are taken. The apparatus required and its costs are relatively small if the phenomena investigated are not of too high speed. With further refinements and the use of stroboscopic cameras, giving finer subdivisions of time and distance, higher-speed phenomena may be investigated, with, of course, an increase in the cost and the complexity of the apparatus.

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A Variable-Register-Ratio Watt-Hour Meter

By G. R. SHUCK
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As is generally recognized, electrical energy is a commodity, the sale of which should be subject to the same economic laws that apply to other commodities. The two most important economic laws which regulate the sale price are (1) the law of diminishing costs, and (2) the law of supply and demand.

The first of these laws is partially applied in our present system for metering electrical energy, the so-called block system charging a high price for the first block of energy, a lower price for the next block, and a still lower price for the third block, the whole schedule being equivalent to a lower price per kilowatt-hour for greater consumption.

The second law justifies a higher price when the commodity is scarce or the demand is great, and a lower price when the commodity is plentiful and the demand the least. This law has an application in the 24-hour load cycle of an electric-utility company. The ever increasing peak load of the period between 4 p.m. and 9 p.m. indicates a greater demand and a limited supply, justifying an increased price. The period from 12 midnight to 5 a.m. indicates little demand and an unlimited supply, justifying a decreased price per kilowatt-hour.

Unfortunately, our present method of measuring energy with watt-hour meters does not and cannot supply this second law of economics. The utility rate makers do, however, recognize this law and are applying it in part to some schedules, such as domestic water heating, through the medium of off-peak time switches, carrier-current control, flat-rate schedules, and an allowance of a specific number of kilowatt-hours for water heating in billing.

Although water heating is especially applicable to off-peak control, owing to the heat-storage capacity of water, other loads, both domestic and commercial, could in part be kept off-peak, particularly if there was some incentive for the consumer to do so.

The practice of cutting off water heating loads during peak hours is not altogether satisfactory and is not a logical long-run policy for the utility companies to adopt, because it is only partially effective and does not apply to all shiftable loads.

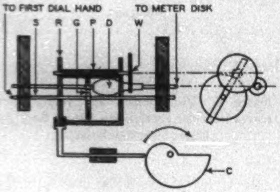

Figure 1

Rather than go to the expense of utilizing schemes for cutting loads off the peak, requiring additional wiring, meters, and timing devices, would it not be more logical to develop some metering system which will automatically take into account this second law of economics? These loads may be left on the system at all times, subject to a higher rate during the daily load cycle when energy is scarce and the demand great, and subject to a lesser rate when the energy is plentiful and the demand least. The consumer may himself apply automatic time switches on his load, or control them manually or not at all, as he chooses.

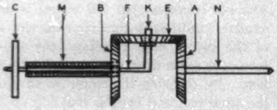

Figure 2

In order to investigate the possibility of applying the law of supply and demand in the metering of electrical energy, the writer, in the University of Washington laboratories, constructed a metering unit from a standard watt-hour meter, which will be called in this paper a variable-register-ratio watt-hour meter.

The variable register ratio is obtained by introducing in the registering train of gears, a drum, and wheel. The position

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Paper number 38-103, recommended by the AIEE committee on instruments and measurements and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted April 20, 1938; made available for preprinting July 15, 1938.

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of the wheel with respect to the drum is changed during the day by means of a timing unit.

The shape of the variable-diameter drum should be such that the meter registers more slowly during off-peak hours. In general the shape of the drum should correspond to the daily load curve.

Figure 1 is a schematic drawing of that part of the registering train which has been altered. The drum *D* is mounted on one of the shafts of the registering train of a standard watt-hour meter. The wheel *W* and pinion *P* are mounted on a carriage rack *R*, the latter arranged to slide longitudinally as well as turn on shaft *S*. The gear *G* solid on shaft *S* meshes with the long pinion *P* with the carriage in any position. The motion is transmitted from the meter disk through drum *D* to wheel *W*, pinion *P*, gear *G*, to the first dial hand.

The rack *R* is moved to the left along shaft *S* by means of the cam *C* and a push rod, and returned by means of a spring when released by the cam. The forward motion takes place in a little less than 24 hours, the return motion in about five minutes, the complete cycle in exactly 24 hours.

The cam *C* is rotated at one revolution per day by means of a timing unit shown in figure 2. Gear *B*, mounted solid on the hollow shaft *M*, is driven by a synchronous clock at two revolutions per day. Gear *A*, mounted on shaft *N*, is driven in the same direction by an ordinary hand-wound clock at two revolutions per day. The gear *E*, meshing with both gears *A* and *B*, rotates on shaft *K* and revolves with shaft *F*, the latter passing through the hollow shaft *M*. The cam *C*, also shown as *C* in figure 1, is driven by shaft *F*. If either gear *A* or *B* rotates while the other is stationary, the shaft *F* is caused to rotate one revolution per day.

In operation, the synchronous-motor clock drives the shaft *F* and cam *C*, while the hand-wound clock and gear *A* are stationary. In the event of a power interruption, the synchronous-motor clock stops, and the hand-wound clock starts up and drives shaft *F* until the power again comes on and the synchronous motor resumes its duties. The hand-wound clock is designed to start or stop automatically by means of a very small electromagnet which controls the motion of the balance wheel of the clock.

In order to discuss the application of this metering system to a schedule, the 24-hour day may be divided into periods such as normal, off-peak, and peak periods. The normal periods may, for example, be assumed to be from 7:00 a.m. to 4:30 p.m., and from 6:30 p.m. to 11 p.m.; the peak period from 4:30 p.m. to 6:30 p.m.; and the off-peak period from 11 p.m. to 7 a.m.

The diameter of that part of the drum corresponding to the normal periods will be such as to cause the watt-hour meter to register true kilowatt-hours. The diameter of that part of the drum corresponding to the peak period will be greater, causing the meter to register more than the true kilowatt-hours. The diameter of that part of the drum corresponding to the off-peak period will be such as to cause the meter to register less than the true kilowatt-hours.

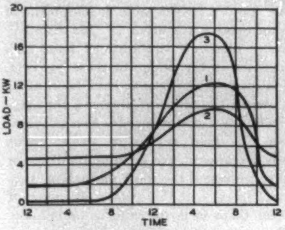

Figure 3. Customer-load curves

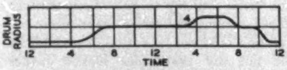

Figure 4. Variable-register-ratio drum radii

Obviously any consumer cannot be expected to have a 100 per cent load factor. He is entitled to use a certain amount of energy during peak hours, and is also expected to use some energy during off-peak hours. The average consumer may be assumed to have a normal load curve, just as the power consumer is expected to maintain a normal power factor within reasonable limits. For purposes of discussion load curve 1 (figure 3) may be considered to be a normal load curve.

The peak and off-peak diameters of the drum may be so calculated that, during a 24-hour period, the variable-register-ratio watt-hour meter, equipped with this drum, will register the true kilowatt-hours of a normal load. That is, a consumer having a normal load curve would have the same kilowatt-hour registration during the month as he would have if his load were metered by a standard watt-hour meter. Curve 4 (figure 4) shows the shape of such a drum. Curve 5 (figure 5) shows the registration of the variable-register-ratio meter for load curve 1 (figure 3) indicating a total registration of 192 kilowatt-hours.

Assume that the consumer has a load curve 2 (figure 3) equal in kilowatt-hours to the normal load curve 1, but showing less peak and more off-peak load. The actual registration curve of the variable-register-ratio meter is shown in curve 6 (figure 5) giving a total daily registration of 172 kilowatt-hours.

Assume the consumer has a load curve 3 (figure 3) equal in kilowatt-hours to the normal load curve 1, but showing more peak load but less off-peak load. The actual registration curve of the variable-register-ratio watt-hour meter is shown in curve 7 (figure 5) giving a total registration of 218.

These curves show that a given number of kilowatt-hours will give different registration, depending on *when* the energy was used.

In some two-rate schedules involving large blocks of power the maximum demand is determined by a graphic recording wattmeter, and the charge per kilowatt of maximum demand varies, depending on when the maximum demand occurred, whether off-peak or on-peak. In two-rate schedules involving small blocks of power the indicating maximum-demand register is used. This method gives no indication of the time of day the maximum demand occurred. If a maximum-demand attachment were installed on the variable-register-ratio watt-hour meter, it would automatically register a higher or lower indication, depending on the time of day the maximum demand occurred.

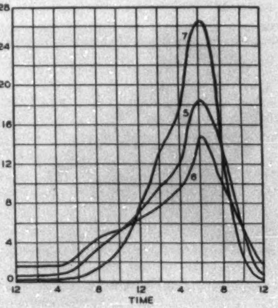

Figure 5. Rate of meter registration

The variable-register-ratio watt-hour meter may be used in a different way by having two registers, one a standard dial, the other a variable-register-ratio dial. The standard dial registers the true kilowatt-hours as any other standard meter. The variable-register-ratio dial gives the modified registration as already explained. The ratio between these two readings gives a modifying factor K which may be used in billing the true kilowatt-hours.

Assume the consumer has the normal load curve 1 (figure 3). Both dials will indicate the same, namely 192, and the factor K will be $^{192}/_{192} = 1$. Assume the consumer has load curve 2. The standard dial will again register 192, but the variable-register-ratio dial will register 172. The factor K will be $^{172}/_{192} = 0.895$. Assume the consumer has load curve 3. The standard dial will register 192. The variable-register-ratio dial will register 218. The factor K will be $^{218}/_{192} = 1.135$. The factor K obtained each month is a measure of *when* the energy was used and may be used as a modifying factor in billing the true kilowatt-hours.

The meter constructed in our laboratories (figure 6) consisted of parts of other meters assembled together into a working unit to test the general method, and is essentially a crude instrument compared to one which would be designed and constructed by a manufacturing company. However, some information was obtained which leads to the following conclusions.

1. A variable-register-ratio watt-hour meter can be constructed of sufficient accuracy and low cost to compare favorably with the present methods of controlling off-peak loads, and at the same time to have more universal application.

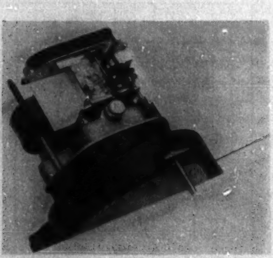

Figure 6

2. Tests were conducted to show the accuracy of transmission through a drum and wheel. The ratio between the frictional torque at the wheel and drum, and the torque required to drive the registering train was found to be three to one, showing a sufficient factor of safety in regard to the wheel slipping on the drum. A proper design and choice of material could, no doubt, increase this factor many fold.

3. The timing device should consist of a synchronous-clock motor with an auxiliary hand-wound or electric-wound clock designed to run not more than 24 hours, since power interruptions sum up to be far less than 24 hours during one month. It was found by test that the smallest type of synchronous motor produced 100 times as much torque as was necessary to rotate the cam.

4. The design of the unit should be such that the drum of the variable-register-ratio dial can be removed and another inserted without dissembling the registering train, to conform to any desired rate schedule. If it is desirable to convert the variable-register-ratio meter into a standard watt-hour meter it is only necessary to slip in a cylinder of proper diameter, or to replace the entire register with a standard registering dial.

5. In view of the probable objection on the part of the rate commissions that the variable-register-ratio meter does not actually register kilowatt-hours at all times, the second method of the two outlined in this paper would be more acceptable, the meter being used to obtain a monthly modifying billing factor.

Trends in the Design and Arrangement of Electrical Equipment in Hydraulic Power Plants

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DURING the last ten years there has been a considerable development of water power resources in this country. Many different organizations have participated in the design of these projects, and many contributions have been made to reduce the first cost, improve the over-all efficiency, and reduce the cost of operation. A brief review of those trends which represent the greatest changes, or have provoked the most discussion, may be of interest at this time. On many of them there is no unanimity of opinion. The reader may disagree with some, he may even question whether others represent general trends at all; nevertheless, the writer will try to give them without either commendation or condemnation, and he would not be entirely candid if he did not say that some looked rather foolish to him at first. It should be emphasized that this paper will discuss only those features which have been changing, or have provoked discussion; many things which are of much greater importance to the station may therefore be mentioned only briefly, if at all.

Powerhouse

A number of semioutdoor type of generating stations have been built in which the conventional powerhouse superstructure was omitted. In this design the generator-room floor becomes the station roof, the generators being protected by a concrete or metal housing. An outdoor gantry crane is usually provided for removing the waterwheel and generator parts to one end of the station, where they may be lowered through a hatch to the inside repair bay below. The layout of the generating units and their auxiliaries is arranged so that practically all operation in connection with the equipment will be below the generator deck, where complete protection is obtained. There is a considerable difference of opinion as to the true over-all saving with this type of plant, particularly where the extra cost of installation and maintenance is taken into account. If experience demonstrates the absence of operating and maintenance difficulties, they may be more generally adopted in the future.

In the past, it has been the general practice to locate the generators in the powerhouse above the maximum expected flood waters, and in many cases this has resulted in an expensive structural design, because of the distance between the waterwheel and generators. In several

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Paper number 38-100, recommended by the AIEE committee on power generation and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 7, 1938; made available for preprinting June 20, 1938

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recent projects, it was found that the most economical arrangement was to place the generators as close to the waterwheels as ordinary considerations would allow, and protect them from flood waters by building a waterproof powerhouse without windows. This type of station naturally results in an indoor design, since the walls are essential for flood protection, and only a roof is necessary to enclose it.

Generators

Further economy in power-plant design has been achieved by an increase in the size of the generators and waterwheels over those which would have been considered practicable a few years ago. Operating efficiency has been improved by the increased use of the adjustable-blade turbine for low-head developments. Although these trends principally represent an advancement in the design of hydraulic machinery, they, of course, affect directly the generating equipment. The largest unit in physical size at a particular speed which it is possible to build economically at the present time has not been reached, but it has been closely approached. The limiting factor in waterwheel-generator design is usually the mechanical stresses at runaway speed.

There has been very little change in the structural design of waterwheel generators in the last few years. Fabricated construction continues to be widely used. Partly because of the accuracy with which the strength of fabricated rotors can be predetermined, and partly because of the expense of building up and tearing down again to meet shipping limitations, particularly on the larger size units, overspeed tests are seldom made at the factory.

Overhung or umbrella type of generators with one guide bearing have been installed in a number of stations. Such generators show a slight saving in first cost over the conventional two-guide bearing type. There is, however, an upper limit in speed and physical size beyond which it is not practical to go. Owing to the lack of an upper-guide bearing on the generator, these machines are tested with the waterwheel attached. The design therefore is more difficult to test and does not lend itself readily to the investigation of vibration and other possible troubles.

The highest voltage waterwheel generator built so far in this country is 16.5 kv. Studies have been made on the larger units for voltages of 23 and 34.5 kv with the result that a more conservative and economical design was obtained with a generator voltage of 16.5 kv or less, and a step-up autotransformer. This combination is as efficient as a generator wound for 23 or 34.5 kv.

The advancement in the knowledge and appreciation of system stability has generally resulted in a more careful study and selection of those generator characteristics which affect stability.

A fuller understanding of the general operating advantages of amortisseur windings in waterwheel generators has resulted in their incorporation in a number of units. The principal advantages are: the reduction of overvoltages in the stator winding resulting from unbalanced faults on the generator, particularly on unloaded machines coupled to capacitances such as transmission lines; the effective reduction in oscillations of generator-output kilowatts occasionally experienced on machines which are connected to their loads through high resistance circuits; and a minor aid in system stability by reducing the magnitude of the rotor oscillations.

Experience has shown that even though water-power developments are usually located in rural districts, the air is not free from impurities which lodge in the generator and interfere with the effective cooling of the machine. For this and other reasons the closed ventilating system, using surface air coolers, has been adopted for most of the recent generators above 5,000 kva. In addition to the longer expected insulation life, the closed system reduces station noise and provides increased fire protection. In some installations it permitted a more economical powerhouse, since provisions for large air intake and discharge openings in the building were not required.

On very large generators, where the stacked height is sufficient, the cooler sections have generally been arranged vertically around the periphery of the generators. On the comparatively small machines, where the stacked height is so short that a vertical arrangement around the periphery would be expensive, the sections have been located parallel to the floor, and on the side of the unit for vertical machines, and underneath for horizontal units.

Of the hydraulic generators which have been provided with surface air coolers, less than half are arranged to discharge air to heat the power station. The trend is in the direction of permanently closing the generators, and deriving station heat from some other source, because obviously any departure from the closed system reduces the effectiveness of that system.

Although generator fires are rarer now than ever before, carbon-dioxide fire protection has been provided on many of the larger generators, because of its effectiveness on the closed ventilating system as compared to the open system.

In the larger-size generators which require extensive disassembly for shipment, it has become the general practice to make the electrical tests as well as overspeed tests after installation.

Excitation Schemes

The excitation scheme, used almost exclusively in the last few years on generators rated 3,750 kva and above, employs direct-connected main and pilot exciters, and a high-speed individual rheostatic voltage regulator operating in the main exciter field. A main a-c generator-field rheostat is not required, and in a number of instances the armature of the main exciter is permanently connected to the generator field without a field switch. No provision is made for paralleling exciters.

The operating simplicity of the direct-connected exciters has a very strong appeal. This system generally has the advantage of lower cost, considering in one case a shaft alternator and a motor generator set, and in the other case a direct-connected exciter plus a small step-down power transformer to serve the machine auxiliaries otherwise served by the shaft alternator. Beyond this difference in price is the additional switching equipment, such as circuit breakers and switchgear panels, not to mention increased wiring and cable connections.

Because of the low outage record of direct-connected exciters, the complications introduced by trying to provide spare excitation in what is otherwise a unit system, and a station design which contemplates the outage of a complete generating unit without adversely affecting operation, the spare motor-generator exciter set has been omitted in a number of stations.

In many stations, advantage has been taken of the possibility of allowing considerable variation in generator voltage in order to obtain better system-voltage regulation. In such stations line-drop compensation has usually been incorporated in the generator voltage regulators. In a properly co-ordinated generator voltage regulating system there is no conflict between line-drop and cross-current compensation.

Governor Flyball Drive

One of the early steps taken to free the governor flyball drive from the physical limitations of a belt was to provide a flyball motor and drive it from the main generator terminals through small transformers. This type of drive was found to be satisfactory in most instances, although it has the disadvantage of tending to cause the governor to follow system frequency rather than generator frequency during those rare cases when the main unit twists out of step with the system due to instability.

Although this condition has occurred very rarely in practice, the desirability of eliminating it was early recognized. The next source of power used was obtained from slip rings on the pilot exciter. Where steps were taken to obtain proper co-ordination in design between the governor motor and pilot exciter, this scheme has operated satisfactorily. Where this was not done, trouble has been experienced in some installations due to a very sensitive governor head along with slight phase-shifting of the pilot exciter flux with respect to the shaft of the main unit, with the result that the governor did not see true shaft speed at all times.

A permanent magnet generator directly mounted on each unit is the latest source of power supply for governor flyball motors, and is the system which has generally been used in the last few years.

Circuit Arrangements

Hydroelectric-generating-station switchgear shows a decided tendency toward simpler circuit layouts including the elimination of generator oil circuit breakers and low-voltage busses, resulting in a considerable saving in cost, as well as a greatly simplified station design.

The so-called unit system has been widely adopted. The simplest case with this scheme is to have one generator connected to its own individual transformer bank. In the earlier installations an oil circuit breaker was installed between the generator and transformer primarily for synchronizing purposes. The present oil circuit breakers of all voltages have closing speeds sufficient for synchronizing on the high-voltage bus, so that in the more recent installations the breaker between the generator and the transformer has been omitted.

Some stations have used a modification of the unit scheme in which two generators are connected to one transformer bank, each generator having its own low-voltage breaker in order to be able to synchronize or remove one generator from service without affecting the other.

Many operating engineers have concluded that the unit scheme with its omission of the low-voltage bus represents practically no sacrifice in over-all station reliability or flexibility. Although this scheme is open to the criticism that, if a generator fails and at the same time a transformer normally connected to another generator also fails, the output of the two machines is lost, operating records show that such a contingency is very unlikely to occur. Furthermore, few plants are designed on the basis of two simultaneous failures, since their cost would be prohibitive.

Switchgear Equipment

Although the simpler circuit layouts now so prevalent in hydro stations have appreciably reduced the number of low-voltage oil circuit breakers, nevertheless many stations still require large-interrupting-capacity breakers in addition to the smaller ones for controlling station-service power.

In many of the more recent projects, these breakers, their disconnecting switches, busses, etc., are enclosed in complete factory-built metal structures. Metal-enclosed busses from the generator terminals to the power transformers are also being used.

The increasing use of metal-enclosed gear is attributed to the more general appreciation of its economy and safety features. By using this equipment, station planning has been simplified by eliminating the time and expense in detail designing and co-ordination which is required where cell-mounted equipment is used. Savings in building cost have been realized in many instances, because this type of gear saved floor-space and headroom. As a rule, metal-enclosed gear has been found to cost less installed than other types in which equal precautions have been taken to preserve service continuity.

Many refinements in oil-circuit-breaker design have been made in the last few years. More effective means for controlling the interruption of the arc has permitted smaller oil circuit breakers for the same voltage, current, and interrupting rating. Faster operating mechanisms are now being used to speed up the tripping and closing times, thereby reducing system disturbances. In the high-voltage field, oil circuit breakers rated 287 kv and, having a clearing time of three cycles, have been installed. Inasmuch as these breakers were of very low oil content, they evidenced the trend toward low-oil-content breakers.

Metal-enclosed air circuit breakers or contactors are extensively used for controlling station service power at 550 volts or less. This equipment permits a still further reduction in fire hazard and its compact construction results in a saving in space and installation expense.

Control Boards

The tendency is toward the so-called duplex board with control, indicating instruments, etc., on the front panels, and relays, meters, test blocks, and similar equipment on the back panels. In some of the larger power plants, supervisory type of control has been mounted on duplex panels. Such control eliminates some of the wiring complexities, permits a smaller switchboard control room, and makes it possible for the operator to follow station conditions more readily since the essential information is concentrated in a relatively small space.

The advantages of supervisory control are secured by locating cubicles adjacent to the generators with which they are associated. The duplex switchboard located in the main control room has supervisory control over these cubicles. Only those indications and controls which are required for station operation are provided on the duplex board, thereby reducing the number of instruments and devices, and the amount of interconnecting control cable. A signal system is used to indicate abnormal operating conditions, which in some instances may require the attention and assistance of a generator floor man. In an emergency the generator floor man can take over the operation of the unit at the generator cubicle.

Automatic synchronizing equipment has been provided to a greater extent than in the past, because the operation is accomplished smoothly and without the switchboard operator's neglecting other important duties at that time.

Station-Service Power

The supply of station-service power, like excitation schemes, has run a varied and interesting course. Like excitation with which it has at one time or another been closely associated, the trend has been toward greater simplicity, influenced perhaps as much as anything by a careful reexamination of the basic service requirements of the individual devices that make up the station-service load. After doing the same thing the same way for a few years, additional experience gained in that time frequently demonstrates that the requirements are not so severe as they had been assumed, or else the improvement in apparatus permits a simpler solution. Such has been the case with station-service power.

With the use of direct-connected exciters instead of motor-generator exciter sets, the principal reason for the shaft auxiliary generator was eliminated. The simplest and most economical system for obtaining station-service power, which at the same time possesses a high degree of flexibility, has been obtained by connecting a small step-down transformer to the generator leads, either directly or through a disconnecting switch. This system has been widely adopted in the last few years, particularly in stations without a low-voltage bus.

With this scheme the auxiliaries intimately related with each generating unit are fed from the transformer connected to the terminals of that generator. This transformer usually supplies a low-voltage bus from which emanate the leads to the various unit auxiliaries, such as governor oil-pump motor. To give duplicate service to this bus for emergency or starting up the station, power is fed from an adjacent bus, a house generator, or a high-voltage step-down station-service transformer bank.

Present indications are that the unit station-service-power scheme may be used in the future, even in stations with a low-voltage bus, since it permits the elimination of an expensive high-interrupting-capacity switch position.

Grounding and Protective Equipment for Generators and Transformers

It is difficult to discern any particular trend in generator-neutral grounding and relay protection on units connected to low-voltage busses from which feeders emanate. Such generators are either solidly grounded or else grounded through a neutral impedor. The limits are, on the one hand, a sufficiently low value to stabilize the system neutral, and on the other, high enough to minimize damage resulting from faults.

In stations employing the unit system, where each generator is directly connected to the delta side of its own step-up transformer, the relaying and grounding trends have been interesting. In many stations, the generator neutral is solidly grounded. This permits the generator differential relay to protect the maximum amount of the generator winding on phase-to-ground faults, but on the other hand grounding the neutral solidly has the disadvantage of permitting the maximum amount of fault current.

Damage resulting from a fault, however, depends on duration as well as magnitude. In following the approach of reducing fault duration, high-speed balanced-beam differential relays operating in one or two cycles have been used in a number of stations, instead of induction-type relays having six- to eight-cycle operating times. This reduces the overall fault-clearing time from approximately 14 cycles to 9 cycles where the generator-neutral breakers are used to unground the machine.

Because of the increased cost of the very special current transformers now required to provide high-speed generator differential relaying and the comparatively small gain in time in extinguishing the arc, there is a trend back to induction-type relays. This trend is perhaps further influenced by the feeling that if damage from line to ground faults is the principal reason why generator differential relays have been speeded up, then the more direct attack on the problem is to insert an appreciable amount of resistance in the generator neutral, or unground it entirely, as has been done in some stations. In the latter case, a ground detector which signals the station operator, but does not trip the unit, is usually employed. This permits temporary operation until a shutdown can be conveniently arranged.

There has been some interest in high-speed transformer differential relaying, but in the case of power-transformer protection, the problem is more difficult than in the case of generator-differential protection, for not only must instrument transformers be matched with bushing transformers, but even more important, provision must be made to prevent false tripping during magnetizing inrush periods. Despite these difficulties installations of high-speed transformer differential relays have been operating quite satisfactorily. The choice between high-speed and induction-type percentage differential relays is largely dictated by stability considerations.

There has been a trend toward the increased use of bus differential protection where there were large concentrations of power because the preservation of service continuity requires the speedy clearing of the large resulting fault currents. From the protective standpoint the presence of large fault currents causes current-transformer saturation that tends to operate the differential relays during faults external to the bus. The relay current in the case of an external fault differs from that arising from a bus fault, in that it has a large harmonic content. The recognition of this difference has led to the development of a harmonic-restrained high-speed bus differential relay, which will undoubtedly find increasing use in the future.

In the past, generators feeding through step-up transformers were generally believed to be immune to lightning due to the fact that the transformer bank would act as a buffer against incoming waves. It is now known that this is not entirely true, and consequently generator lightning-protective equipment is commonly installed. Experience has indicated that a relatively small investment in properly selected equipment will provide a degree of protection that will result in a material reduction of the possible lightning stresses imposed on rotating machines and contribute to an appreciably increased security against faults due to lightning-impulse voltages.

Outdoor Substations

The arrangement of the high-voltage equipment at a hydro station is largely influenced by the topography of the site and the location of the powerhouse. In those projects where the outdoor switchyard is separated more than a few hundred feet, the general practice has been to locate the step-up transformers adjacent to the powerhouse, and conduct the power to the outdoor switchyard by means of overhead high-voltage lines.

Outdoor substations at 66 kv and above are commonly shielded by overhead ground wires which extend out over each line for at least half a mile. The use of overhead ground wires has increased the height of the outdoor structure somewhat and requires slightly heavier steel to carry the additional load, not only in dead weight, but also to carry the take-offs on the outgoing lines. These items represent only a small addition to the total cost of the station.

Shielding of outdoor substations practically eliminates the possibility of direct strokes nearer than half a mile from the substation, thereby limiting the incoming surges to a value that a lightning arrester can handle. In spite of a considerable discussion during the past few years of the real merits of plain gaps versus lightning arresters for apparatus protection, arresters continue to be in very general use. This is for the reason that gap settings which give a degree of protection comparable to that of lightning arresters result in more dynamic flashovers than can ordinarily be tolerated.

Power Transformers

The trend in the design of power transformers continues to be in the direction of improvement in the dielectric strength and refinements in mechanical construction. Much attention has been given in the last few years to the study of insulation co-ordination, resulting in improved insulation strength to resist lightning, and the proper distribution of impulse stresses throughout the transformer winding obtained, for example, by the use of shielding.

There is a trend in the larger size transformers toward the use of the gas-sealed unit, wherein the space between the oil and the cover is filled with dry nitrogen gas. In addition to providing a seal against the entrance of oxygen, moisture, dust, etc., some operators feel that the fire hazard is less, because the oil level is below the transformer cover. The outstanding contribution from the design standpoint toward minimizing the fire hazard in power transformers, however, results from the use of a noninflammable liquid instead of transil oil. Indications point toward an increased use of such transformers in the future.

The general tendency in this country has been to install single-phase transformers to form three-phase banks, even where three-phase transformers were practicable from a physical standpoint. A re-examination of this problem from the standpoint of outage, handling, and reliability, indicates an increased interest in three-phase units.

Strange as it may at first seem, several recent large hydro stations have installed self- or forced-air-cooled transformers. The elimination of the cooling water system has influenced their selection to some extent. Another consideration was the ability to obtain outputs above the self-cooled rating with forced air. The latter is directly attributable to the generator rating and temperature rise. Most generators rated 11 kv and above use class *B* insulation, with class *A* temperature rise. Operating experience with such a design indicates a longer life and one capable of carrying some emergency overload. This requirement may arise under the most favorable hydraulic conditions, since at that time the capacity of the waterwheel may be in excess of the normal generator rating. The air-pressure cooling equipment on the power transformer provides an economical means of obtaining transformer capacity to match the maximum generator output.

Step-up transformer high-voltage-neutral grounding shows a trend away from solidly grounding in the direction of grounding through an impedance, and in a recent installation at 220 kv this impedance was a Petersen coil. The experiences with these coils in this country in the last few years indicates a more general acceptance of this device for improving system performance.

Because of the desire to obtain noninflammability of the liquid, as well as to eliminate explosions of the arc-formed gases, there is an increased use of noninflammable synthetic insulating oil in station-service transformers, particularly those installed in the powerhouse.

Summary

From the foregoing discussion, it has been seen that the outstanding trends in the design and arrangement of electrical equipment in hydraulic power plants, with but a few exceptions, are toward greater simplification, reliability, and lower first cost. Inasmuch as fixed charges constitute the major portion of the cost of hydro power, a reduction in first cost affords the principal means for securing a reduction in the cost of water power.

Discussion

J. H. Foote (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper covers the subject in a comprehensive manner and gives a good general picture of some of the developments in the design of hydraulic plants and the equipment which goes into them. Many of the types of design and the features outlined in this paper have been used successfully by our associated companies for a number of years.

POWERHOUSE

In 1925, we designed and built a hydraulic power plant, utilizing the semioutdoor type of construction, with two 1,060-kva units. This plant is of the automatic variety and this type of construction has proved satisfactory from an operating standpoint.

GENERATORS

We have also utilized the enclosed type of air-cooling system for the generators of one of our newer large hydraulic power plants with satisfactory results.

It might be pointed out that the testing of umbrella-type generators in the field, with the waterwheel attached, may be of small consequence if both units are of the same manufacture, but if different manufactures are involved, it may be very important to be able to test the units individually.

EXCITATION SCHEMES

Direct-connected main and pilot exciters have been used on our hydrogenerators since 1924 with satisfactory results. In fact, direct-connected exciters have been employed without spare exciters or excitation busses since 1905 with success. No exciter failures have been experienced, and the practice of not providing spare sources of excitation has not caused us any difficulty. It is felt that the chief advantage of using the pilot exciter is that greater stability of the excitation system is obtained.

It has been noted with interest that in certain cases field switches have been omitted. We have successfully operated four generators so connected since 1922. In many of our generator-overvoltage protective schemes, the generator voltage regulators are depended upon for overvoltage protection. However, as a matter of backup protection, or where voltage regulators are not installed, facilities have been provided for tripping the field switch in case the generator voltage reaches an excessive value under runaway conditions. Where no main field switch is used, we have had success in using a small air circuit breaker to cut in resistance in the exciter field or to open the exciter field as a last resort. It also seems desirable to remove the excitation from the machine in case the differential protective equipment functions. A field switch in the exciter field seems to function satisfactorily in the absence of a main field switch.

The necessity of providing special current transformers for high-speed differential relaying is a problem requiring further detailed investigation so that a better understanding may be had of the performance of current transformers during the early transient stages of a fault.

GOVERNOR FLYBALL DRIVE

The use of an electrical drive for the governor has been a very desirable development, but our experience indicates that serious trouble may be experienced if the drive receives its energy from slip rings on the pilot exciter, especially if the generator is equipped with a voltage regulator. Permanent-magnet generators provide a very satisfactory source of supply for governor motors.

CONTROL BOARDS

Our experience with automatic synchronizing equipment indicates that while satisfactory performance can be obtained under steady-state conditions, certain types of equipment may fail to function during transient conditions or emergencies, and it is most needed at these latter times.

Phase-Angle Control of System Interconnections

By **R. E. PIERCE**
MEMBER AIEE

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NONMEMBER AIEE

Synopsis: This paper describes an application of a new method of automatic control between two interconnected power systems, whereby greater utilization of the interconnection capacity is secured. As the stable limit of the interchange between systems is affected by variations in the magnitude and location of intermediate loads on the respective systems, direct control of the phase angle between systems recognizes such variations and permits a flexible and more stable type of operation including, when desired, maximum transfers of energy. An indication of the 60-cycle voltage from a desired point on one system is continuously transmitted by means of carrier current, to a point on the second system where its phase position is compared with that of the 60-cycle voltage at the receiving point. The output of generating units at the control station is automatically regulated to maintain the phase angle between systems at desired values or within predetermined limits.

THE power systems in the Pacific Northwest have been successfully operated interconnected for many years. In addition to the normal or routine functions, on several occasions these interconnections have been called upon to transfer large blocks of power and energy to alleviate abnormal conditions on one system or another, averting serious power shortages.

Heretofore, the control of the power flow between these systems has been accomplished principally by manual operation. The amount of power which has been interchanged, under some conditions, and between some of the systems, has not approached the stable limit of the interconnections, and manual control has not been difficult. On some of these interconnections, however, experience has demonstrated, as in similar situations in other parts of the country, that because of the time required for communication and manual control of generating units, the interchange had to be regulated to values well within the stable limits, in order to prevent, or at least limit the number of severe disturbances otherwise occasioned by the systems separating due to relay action as stable limits were approached, or actually pulling out of step.

In considering the application of automatic-control equipment for the interconnection between the systems of The Montana Power Company and The Washington Water Power Company, so that a flow of power closer to the stable limit could be safely carried, a new method of control, based upon the phase-angle difference between systems, was conceived and developed. This paper describes the application and the experience so far obtained with this type of control.

Description of Systems and Interconnection

Figure 1 is a map of the Northwest showing the principal transmission lines of the several interconnected systems.

On The Montana Power Company there is approximately 300,000 kw of hydroelectric generating capacity with 250,000 kw usually in service. The total capacity will be increased to 350,000 kw in 1938. A large part of this generating capacity, and a substantial part of the load served, are both located in the Great Falls area. Many of the larger individual loads served from this system, including hoists, railway, and dredge loads, are of such magnitude and fluctuating character that sudden load changes of from 5,000 to 30,000 kw are not uncommon.

The point of interconnection between The Montana Power Company and The Washington Water Power Company systems is at Burke, Idaho. For a number of years previous, and at the time this application of automatic control was planned, the ability to interchange power between these two systems was limited by the single 110-kv, 170-mile transmission line of the Chicago, Milwaukee, St. Paul and Pacific Railroad Company between East Portal and Gold Creek. The capacity for power flow eastward was variable, depending upon seasonal variations in the Thompson Falls hydroelectric plant output. The second line from Thompson Falls, via Flathead to Anaconda, was added in March 1937, practically coinciding with the installation of the phase-angle control equipment. The purpose of this latter transmission line was to transmit power into Montana during an unprecedented low-water period and to furnish a second channel for Thompson Falls power into the Montana system as well as that from Flathead. At later periods it is contemplated these lines may transmit power to the west.

The Washington Water Power Company system is also served by hydroelectric generation, with approximately 200,000 kw of capacity, most of which is usually in service. A large concentration of capacity and load on this system is in the Spokane area.

The Washington Water Power Company system is in turn interconnected with the system of the Puget Sound Power and Light Company on the west, having approximately 300,000 kw of capacity. To the southwest, Washington is interconnected with the Pacific Power and Light and Northwestern Electric Companies, having an aggregate capacity of 120,000 kw. The Portland General Electric Company, 175,000-kw capacity, and Washington Gas and Electric Company, 30,000-kw capacity, are in turn interconnected with the Northwestern Electric Company system in the Portland area. The Portland General Electric Company, however, is not normally

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Paper number **38-118**, recommended by the AIEE committee on power transmission and distribution and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 17, 1938; made available for preprinting July 21, 1938.

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The authors gratefully acknowledge the co-operation of their associates in the engineering and operating organizations of The Montana Power Company and The Washington Water Power Company, and particularly of H. L. Melvin of Ebasco Services, Inc., G. E. Heberlein and R. E. Powers of Westinghouse Electric and Manufacturing Company, and S. B. Morehouse of Leeds and Northrup Company, in the successful development and application of this method of control.

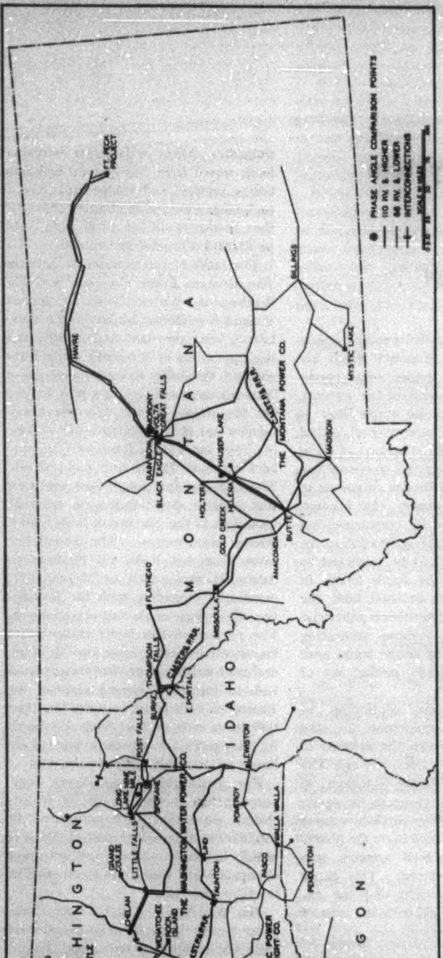

WASHINGTON
IDAHO
MONTANA
OREGON
PHASE ANGLE COMPARISON POINTS
110 KV. & HIGHER
66 KV. & LOWER
INTERCONNECTIONS
FT. PECK PROJECT
HAVRE
BILLINGS
MYSTIC LAKE
MADISON
THE MONTANA POWER CO.
GREAT FALLS
MORONY
RAINBOW
BLACK EAGLE
HAUSER LAKE
HOLTER
HELENA
BUTTE
ANACONDA
GOLD CREEK
FLATHEAD
MISSOULA
THOMPSON FALLS
BURKE
E. PORTAL
POST FALLS
SPOKANE
LONG LAKE
LITTLE FALLS
GRAND COULEE
THE WASHINGTON WATER POWER CO.
LEWISTON
POMEROY
WALLA WALLA
PENDLETON
PASCO
YAKIMA
PACIFIC POWER & LIGHT CO.
THE DALLES
CHELAN
WENATCHEE
ROCK ISLAND
DIABLO
GORGE
BELLINGHAM
BAKER RIVER
EVERETT
CITY OF SEATTLE
PUGET SOUND PWR. & LT. CO.
LAKE UNION
CITY OF TACOMA
ELECTRON
WHITE RIVER
SNOQUALMIE
CONDIT
ARIEL
NORTHWESTERN ELECTRIC CO.
ASTORIA
LONGVIEW
PORTLAND
STATION "L"
BULL RUN
OAK GROVE
SALEM
PORTLAND GENERAL ELECTRIC CO.

Figure 1. Major electric power systems—Pacific Northwest

operated in parallel with the Northwestern Electric Company when Northwestern is in parallel with other systems in Washington and Montana.

For several years the Long Lake hydroelectric station of The Washington Water Power Company has served as the primary frequency-controlling station for the entire group of systems. Additional frequency-control equipments have been available at the Morony station in Montana and the Ariel station in the Portland area, but have not generally been used when operating interconnected with Washington.

Operating Limitations With Manual Control of Interchange

From previous operating experience, when transmitting power from Washington to Montana over the original connection, it was known that the systems were likely to pull out of step whenever the power input into the line at East Portal (interchange at Burke, plus Thompson Falls generation, less local load at Burke and line loss) reached values in excess of 50,000 kw. The normal practical operating limit, under manual control procedure, was considerably less, in the order of 40,000-kw maximum, or 35,000-kw average, because whenever attempting to operate at any higher value, the rapid "drifting" of the power flow would reach the pull-out point too often. The usual "swings" in power over this interconnection were approximately 5,000 kw, with occasional swings of up to 15,000 kw.

With the Thompson Falls plant output (40,000-kw maximum during high-water months and 20,000-kw average during low-water months) flowing eastward, the maximum permissible average power flow through Burke, Idaho, ranged from approximately 0 to 15,000 kw. To obtain these average flows, with manual control, it was necessary to place observers at Burke and Gold Creek; the observer at Burke giving very close attention to the meters and promptly reporting excessive loading conditions to the observer at Gold Creek, who would in turn report conditions to the controlling station. Even with these precautions, from one to eight system separations would occur practically each day.

Because of a combination of circumstances, including several successive years of lower-than-normal precipitation and run-off in Montana, it was decided in the fall of 1935 that the capacity of the interconnection would have to be increased and at the same time maximum possible use might have to be made of this interconnection in transferring energy into Montana during the succeeding one or two years. It was decided to build the second transmission line, referred to above, and install automatic control equipment, in order to increase the practical operating limit and the load factor of the interconnection.

Experimental Tie-Line Load Control

As a preliminary step, an experimental installation was made of automatic tie-line load control, based upon a measurement of the power flow in the 110-kv line at Gold Creek. A carrier-current-telemetering circuit, operating at 20 kilocycles over the private open-wire telephone line, with variable frequency modulation, gave an indication at the Morony hydroelectric plant of The Montana Power Company on the Missouri River near Great Falls, Montana, of the power flow in the line at Gold Creek. The automatic-frequency controller installed at the Morony plant was temporarily modified and used for automatic tie-line load control. This type of automatic control gave a definite improvement in the interchange capacity; the permissible average flow through Burke being increased to approximately 21,000 kw, with separations of the systems occurring on the average of only once every two days.

Conception and Development of Phase-Angle Control

In considering the alternate locations for a permanent installation of a tie-line load measurement and telemetering transmitter, it was recognized that no one location could be chosen where the tie-line load as such could be controlled to any definite quantity over a period of time, and at the same time secure the maximum possible transfer of energy between systems. With only the Chicago, Milwaukee, St. Paul and Pacific Railroad Company line constituting the tie between systems, the power transfer at any point varied considerably due to the nature of the railway load. Not only did the railroad's electric trains have a widely variable demand, but the location of the load along the transmission line varied as the trains progressed from one section to another. Furthermore, these trains employed regenerative braking so that at times, either a part or all of the load actually became generation with a correspondingly variable point of application. This was particularly true along the 110-kv line from East Portal to Gold Creek.

For example, if the tie-line load at Gold Creek were to be controlled, the permissible power flow at this point would be greater when the intermediate-substation loads were light, especially at the Gold Creek end of the line, than when the intermediate loads were heavy. Consequently the control would have to be set for the smaller value of power transfer, to be safely within the stable limit under all conditions, and no advantage could be taken of the greater permissible flow during a large proportion of the time.

Likewise, if controlling the tie-line load at East Portal, for example, the control would have to limit the flow at this point to the amount that could safely be carried when the intermediate loads between East Portal and Gold Creek were light, without taking advantage of the greater permissible flow whenever a large part of the power flowing past this point was being consumed at the East Portal, or other substations near the western end of the line.

It was also recognized that with the planned addition of the Thompson Falls-Flathead-Anaconda circuit, stability limits, division of power flow between circuits, and permissible angular displacements between the systems would change.

Inasmuch as stability is basically a function of angular displacement between systems, it was conceived by the authors that the phase angle itself should be utilized for the control of the interchange, rather than a measurement of the power flow at any one point. It was proposed to transmit, by a continuously modulated carrier-current signal over the transmission circuits, an indication of the 60-cycle bus voltage at a point on the Washington system to a point on the Montana system. The received signal, being an indication of the angular position of the voltage in Washington, would be compared with the phase position of the local Montana 60-cycle bus voltage. Based on a measurement of the phase-angle difference between these two voltages, the output of generating units at the control station selected would be automatically regulated to maintain the phase angle at the desired point or within predetermined limits.

It was thought that phase-angle control might provide several alternate methods of operation, including those shown in figure 2.

Development and application of this method of control involved consideration of the following questions of fundamental importance:

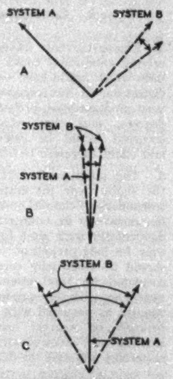
What location should be chosen on each system, for voltage measurement in the determination of the proper phase angle for control?

What phase-angle distortion or attenuation would occur in the carrier-current equipment and transmission channels, from the transmitter input to the receiver output, and what variations would there be in this transmission under different operating conditions?

What would be the effects of outages of lines at any point on the network between the two phase-angle determination points?

Following co-operative studies with engineers of Leeds and Northrup Company and Westinghouse Electric and Manufacturing Company, it was concluded that:

1. The phase angle should be measured between the East Side substation, near Spokane, on the Washington system, and the Morony hydroelectric station, near Great Falls, on the Montana system. For practically all operating conditions these locations would be most representative of the hearts of the systems, and this phase-angle comparison, therefore, a better measure of stability between the two systems. It appeared to have the further advantages of (*a*) immediately recognizing an outage of any line having an appreciable effect on the interconnection capacity, such as on the parallel lines between Spokane and Burke, or between Butte and Morony; (*b*) the receiver would be located directly at the station best suited for control purposes, due to the storage capacity, large and efficient units, and short penstocks, obviating the necessity for an additional telemetering circuit or a long-distance control circuit to the control station; and (*c*) the carrier-current equipment would be located at points well suited for adequate maintenance.



A. With the predetermined limits of phase angle within a narrow range of large positive (or negative) values, resulting in operation close to the stable limit with maximum permissible power flow in one direction (or the opposite)

B. With the predetermined limits of phase angle within a narrow range between a small positive value and a small negative value, or wherever required, to result in a minimum average power flow between the systems, with maximum stability, and maximum ability of each system to help the other under emergencies

C. With the predetermined limits over a wide range between a large positive value and a large negative value, resulting in the least amount of control or definite scheduling of the amount of power flowing between systems, except as the stable limit was approached in either direction

Figure 2

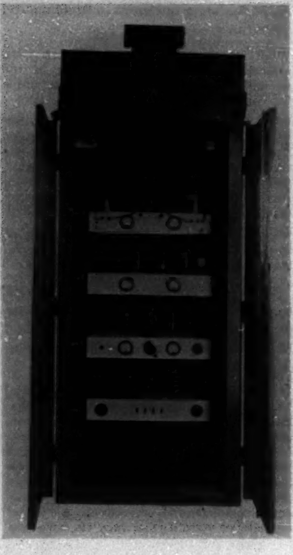


Figure 3. Carrier-current transmitter

2. Carrier-current transmitting and receiving equipment could be designed and built which would have an over-all shift or distortion, in the equipment itself, of a very small amount, probably less than five degrees, and that whatever distortion occurred would be essentially constant, and could therefore be corrected for.

3. The phase-angle shift or distortion of the signal over the entire carrier-current transmission channel might be affected by variations in temperature or weather, particularly with sleet for example; however, for long periods of time the distortion should be essentially constant and easily corrected for, and after some operating experience with the equipment, such gradual changes as occurred with changes in temperature or weather could probably be anticipated or followed fairly closely without materially affecting the degree of control.

4. One effect of outages of lines at different points in the network between the two phase-angle determination points would be to increase the net or effective impedance of the tie, and decrease the permissible power transfer between systems. In this respect automatic control based on phase angle should inherently recognize such line outages and compensate for them in correcting the interchange to values of the same relative degree of safety, with respect to stable limits.

Another effect of an outage of an intermediate line might be to change the phase-angle distortion or attentuation in the carrier-current measuring channel. As it was not planned to confine the carrier transmission to a single channel, it was believed that the loss of one of several parallel paths in the carrier channel would not seriously affect the practical operation of the control.

5. Phase-angle control equipment could readily be developed, and installed at the Morony hydroelectric station in Montana, having a generating capacity of 45,000 kw in two units; using as a basis the already-developed phase-difference recorder,[*] which, together with the frequency-control equipment already installed, would provide the following alternate methods of control:

Flat Phase-Angle Control. For controlling the phase angle to a predetermined value, or within predetermined limits, independent of frequency.

Phase-Angle Bias Control. For control primarily to a definite predetermined value of phase angle, but with assistance given for short periods in the control of system frequency.

Selective-Frequency Control. For controlling to a constant value of system frequency, but only when in so doing, the correction would also assist in controlling the phase angle to a predetermined value.

Flat-Frequency Control. For use as in the past, with isolated operation of the system.

It was decided to proceed with the development and installation of the equipment, and the complete phase-angle control system was placed in service in December 1936.

The control equipment as installed provides for any of the four types of control described under conclusion 5, and the locations of the phase-angle determination points and control equipment were in accordance with the other conclusions outlined.

The predetermined basic phase-angle difference for control is fully adjustable to any angle (0 to 360 degrees), but the width of band around this base reference control point is adjustable up to plus or minus 30 degrees.

With reference to the three alternate methods of interconnected operation previously described in figure 2, and designated as *A*, *B*, or *C*, which were conceived as possible with this type of control, the equipment as installed, therefore, has sufficient flexibility for method *A* (maximum interchange in either direction); and method *B* (maximum stability or minimum interchange), but method *C* (minimum control or scheduling) is applicable only within the range of plus 30 degrees to minus 30 degrees.

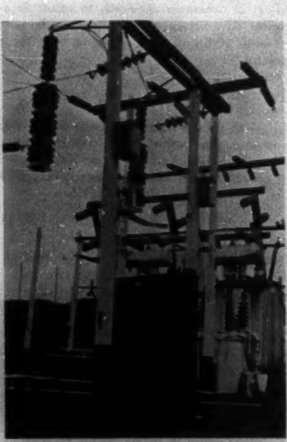


Figure 4. Transmitter installation at East Side substation, Spokane, Wash.

Description of Carrier-Current Equipment

The carrier-current equipment was manufactured by Westinghouse Electric and Manufacturing Company and consists of:

1. A transmitter, surge protective equipment, and coupling capacitors, located at the East Side substation in Spokane, on The Washington Water Power Company system.
2. A receiver, surge protective equipment, and coupling capacitors, located at the Morony hydroelectric station, near Great Falls, on The Montana Power Company system.

The distance between the transmitter and the receiver, by transmission circuits, is approximately 460 miles. Coupling is between one phase wire and ground. No wave traps were installed at any point, so that the carrier-current signal is permitted to flow over the entire 110-kv network of the group of interconnected systems.

The carrier-current transmitter, shown in figures 3 and 4, has a frequency range of 50 to 150 kilocycles and is rated 150-watts modulated output. The oscillator is of the Colpitts type, followed by one stage of class *A* and one stage of push-pull class *B* amplification. The circuit is plate-modulated, using a single transformer, with excitation from a potential transformer connected to the East Side substation 110-kv 60-cycle bus. The outdoor steel cabinet houses the tuning inductance as well as the transmitter proper.

The carrier-current receiver, shown in figure 5 is of simple design, and consists of a detector and two stages of resistance-coupled amplification. The receiver delivers one-watt output. Two tuning controls are used, one for input and another for grid tuning. Meters are provided for indications of filament voltage, and final output plate current. The receiver is housed in an outdoor steel cabinet together with its tuning inductance. All the receiver components are mounted on one panel in this cabinet and can be readily removed.

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[*] "Voltage Regulation and Load Control," H. C. Forbes and H. R. Searing, AIEE TRANSACTIONS, 1934, pages 903–09.

The surge protective equipment at each location consists of a 7,500-volt Deion fuse, two enclosed gaps, and one drain coil, all equipped for outdoor mounting.

Figure 5. Carrier-current receiver

The line coupling capacitors at each location consist of three suspension-type units connected in series with an over-all capacity of 0.00133 microfarad.

Since the accuracy of this equipment depends upon the constancy of the phase-angle difference between the transmitter modulating voltage and the receiver-output voltage, careful consideration was given in the design to limit this phase difference and to minimize variations thereof. The completed equipment was checked to determine the magnitude and variations in this angle, by means of a cathode-ray oscillograph, but the angle was found to be so small that it could not be accurately measured in this manner. For all practical purposes in this application, the shift in phase angle in the carrier equipment itself was considered to be of negligible magnitude.

Another feature of this equipment, while not a new engineering development nor confined exclusively to this set, is the use of iron-cored transformers for carrier frequencies of 50 to 150 kilocycles. By giving special consideration to leakage reactance and distributed winding capacities, efficiencies have been secured at 150 kilocycles that are comparable with commercial 60-cycle transformers.

Description of Automatic-Control Equipment

The fundamental control equipment for phase-angle, frequency, and load control was manufactured by Leeds and Northrup Company; auxiliary switchboard equipment by Westinghouse Electric and Manufacturing Company. The complete control equipment as installed at Morony hydroelectric station includes the following major items:

1. Manually operated phase shifter
2. Phase-angle recorder controller with automatic-droop corrector
3. Frequency controller
4. Proportionate load controller
5. Frequency recorder

These various items perform the following functions in the complete control system.

The manually operated phase shifter is connected in the potential circuit from the Morony bus, and is used to shift this reference voltage by the number of degrees desired to be maintained between the two systems. The controller will maintain this difference by controlling to zero degrees on the phase-angle recorder-controller. Theoretically the phase shifter also may be considered available to correct or compensate for any phase-angle shift occurring in the carrier-current transmission of the remote reference voltage, but in practice this step is unnecessary and the over-all apparent phase-angle difference is used.

As its name implies, the phase-angle recorder-controller produces a graphic record of the relative phase angle of the local Morony bus voltage (as corrected by the phase shifter), with respect to the reference voltage (the received indication of the Spokane bus voltage). Deflection to the left of the center of the chart indicates Morony voltage (as corrected) lagging with respect to Spokane. In effect, therefore, this instrument gives a micrometer reading of the algebraic difference between the phase-shifter setting and the apparent phase-angle difference between the bus voltages. The recorder has a range of plus and minus 30 degrees which is more than ample to cover the normal variations from the base setting. This record on the ten-inch chart gives a very open scale and permits direct readings to one degree of phase-angle difference.

The measuring circuit of the phase-angle recorder is shown schematically in figure 6, and is basically a form of a-c potentiometer which employs the null method of measuring the angle between the two sources, shown connected to *E*1 and *E*2. If these voltages are exactly in phase, the recorder galvanometer will balance when the pen and indicating pointer are exactly in the center of the scale. If the voltage of the Morony bus, as corrected by the phase shifter, lags or leads the reference bus voltage (the received indication of the Spokane voltage), in time phase, a current will flow through the galvanometer, causing the "micromax" balancing mechanism to change the relative values of capacity and resistance in the measuring circuit until zero deflection of the galvanometer is obtained. The recording pen and the indicating pointer move as a unit with the balancing mechanism to show the new phase-angle difference.

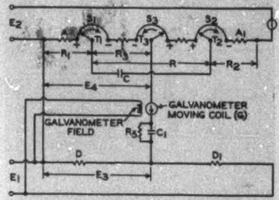


Figure 6. Measuring circuit on the phase-angle recorder-controller

Besides measuring and producing a graphic record of the phase-angle difference, this instrument contains the necessary contacts and control slidewires for originating correcting impulses which are sent to the governor motors to vary the Morony generation as required to maintain the desired phase angle. A unique feature of this control is an automatic-droop corrector which varies the rate at which the controller restores the phase-angle departures back to schedule. If the phase-angle trend is away from the control point, a proportionately larger correction is made than when the trend is toward the control point. In other words, the control action is a function of both magnitude of deviation and trend of the deviation from schedule. This prevents the tendency for the control to "hunt," or "overshoot" and was found to be an essential feature for this application.

The frequency controller is of the standard proportional-step type. When the Montana system is operating alone, this instrument is used to maintain the frequency. It is also used in connection with the phase-angle recorder to provide a bias of phase angle on the frequency for certain operating conditions.

Figure 7. Switchboard control panels at Morony hydroelectric station

The proportionate-load controller is of a similar type and is used to divide the load swing with the second unit, regardless of the type of control on the station.

Contacts were added to the frequency recorder to trip the control when the system frequency departs a predetermined amount from normal, such as under fault or surge conditions.

With this equipment the following four alternate types of control, which were previously described, are available:

1. Flat phase-angle control
2. Phase-angle bias control
3. Selective-frequency control
4. Flat-frequency control

The change from one type of automatic operation to another is accomplished by a single station operation selector switch on the automatic-control panel. The appearance of this panel is shown in figure 7. Figure 8 shows the appearance in greater detail of the phase-angle recorder-controller itself.

Operating Experience

The complete phase-angle control equipment was first placed in service on December 10, 1936. The phase-angle recorder-controller instrument initially installed was found to have an inadequate speed of response, as the changes in phase angle occurred much more rapidly than had been anticipated. The original instrument was replaced in July 1937, with one having approximately six times the rate of control response. The operation of the control equipment has been very satisfactory and it has not required any maintenance other than oiling and cleaning.

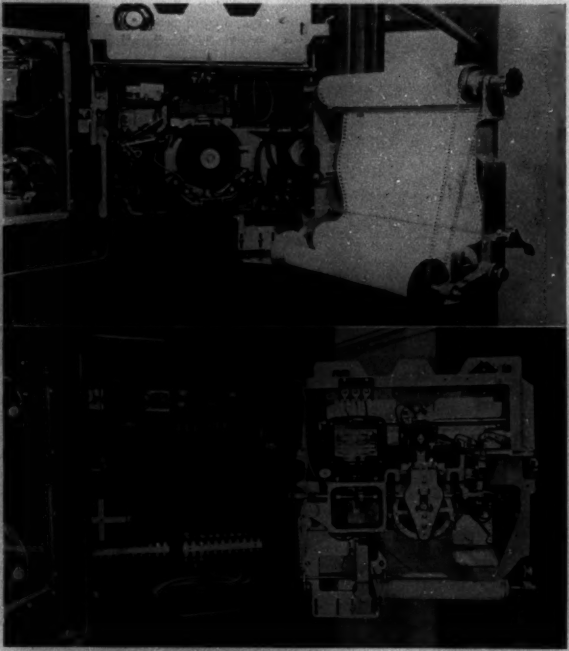


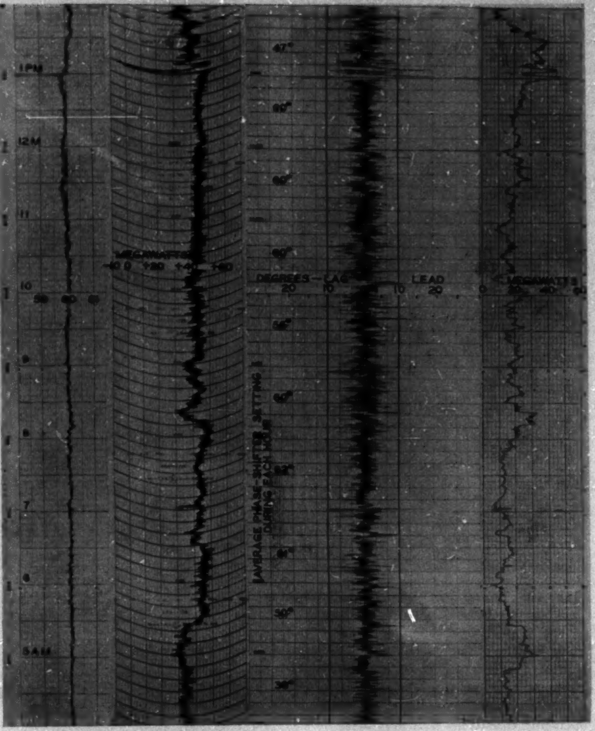
Figure 8. Views of the phase-angle recorder-controller

Operation of the carrier-current equipment has also been very satisfactory. The equipment was originally installed with coupling to two phases of the 110-kv system, but transmission tests indicated superior performance with coupling between one phase and ground within the frequency band desired, and operation has been on this basis. It is very seldom that a signal failure occurs due to storm conditions. While not provided in the original installation, an automatic alarm has since been added for signaling the attendant in the event of failure of the carrier-current signal. This consists of an electronic relay with the grid of the tube controlled positive by the incoming signal. On the failure of this signal, the tube stops drawing current and releases a relay, tripping off the automatic controller and operating an alarm. Maintenance of the carrier-current equipment has consisted principally of tube replacements. Both the transmitter and receiver have been modified in order to use standard tubes.

Previous a-c calculating board studies indicated that when the new line was completed, a phase angle of approximately 72 degrees between Spokane and Morony would be the "practical steady-state limit," with a corresponding total power flow eastward, consisting of the interchange at Burke plus the Thompson Falls generation, of 75,000 kw, for the particular distribution of the intermediate loads assumed. This "practical steady-state limit" was the maximum power flow or phase angle possible, consistent with maintaining the minimum allowable operating voltages at substations along the line. The theoretical steady-state power limit would be slightly higher, but was not determined in those studies. It was thought that in actual operation, the average amount of power delivered, and the phase angle to be maintained, would have to be limited to values less than the above, to allow for power swings between systems, or to approximately 65,000 kw average and 60 degrees.

Experience with the new line and the phase-angle control equipment in service has shown that when operating with an average indicated phase angle between systems of 55 degrees, the only separations of the systems are those resulting from line troubles. At 60 degrees, separations are likely to occur about once every three days (from operation of an instantaneous undervoltage relay at Burke indicating instability). At 70 degrees, operation is only possible with continuous vigilance on the part of the operators. The maximum indicated phase angle at which the systems have been operated under automatic phase-angle control, was 74 degrees, and under the system loading conditions existing at the time the total power flow eastward was 61,000 kw.

The total hourly average power flow eastward, however, has often reached 80,000 kw, the maximum recorded being 82,700 kw. For this large hourly average, the flow during the hour varied from 80,000 kw to 86,000 kw. The corresponding average phase-angle indication was approximately 55 degrees. Usually, however, an indicated phase angle of 60 degrees results in a power flow of from 50,000 kw to 60,000 kw.



System frequency Burke interchange Phase-angle difference Morony output

Figure 9. Typical charts showing operation of interconnection under phase-angle control

Figure 9 illustrates the typical performance of the interconnection under automatic phase-angle control. In this figure, the first chart at the left is the system frequency, the second chart is a record of the power interchange at Burke (from 40,000 to 60,000 kw toward Montana), the third chart shows the variations in the phase-angle difference between systems, and the chart at the right gives the record of the Morony plant output. It may be clearly noticed that over considerable periods of time, while the average phase angle remained essentially constant, the average kilowatt interchange between systems was continually varying several thousand kilowatts with occasional changes of as much as 6,000 to 10,000 kw. This illustrates the fundamental idea of phase-angle control as contrasted to tie-line load control. Such changes in interchange indicate the normal variations in the location and magnitude of the intermediate loads on the two systems, and are to be expected and desired under this type of control. Also illustrated in figure 9, at about 1 p.m., is an instance of the systems separating unexpectedly, which is never desired. In this instance the apparent phase angle between systems reached a value of 76 degrees and the interchange at Burke 55,000 kw when the interconnection was opened by operation of overload relays.

Figure 10 illustrates in greater detail the typical comparative phase-angle variations (*a*) when the automatic controller is out of service and with the Morony output regulated manually to maintain constant phase angle, and (*b*) with the automatic phase-angle control equipment in service. Whenever the Morony output gets outside of the regulating range of the generators in service, the phase angle, of course, cannot be held at the desired value. Such an occurrence is illustrated on this chart between 8 a.m. and 9 a.m.

Usual practice so far has been to operate with "phase-angle bias control," with frequency as the major influence. It is expected that further operating experience, not only with "phase-angle bias control," with varying relative degrees of influence between frequency and phase angle, but with "flat phase-

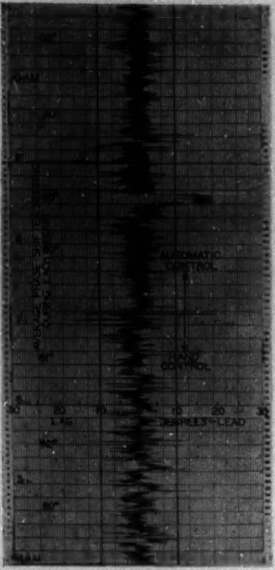
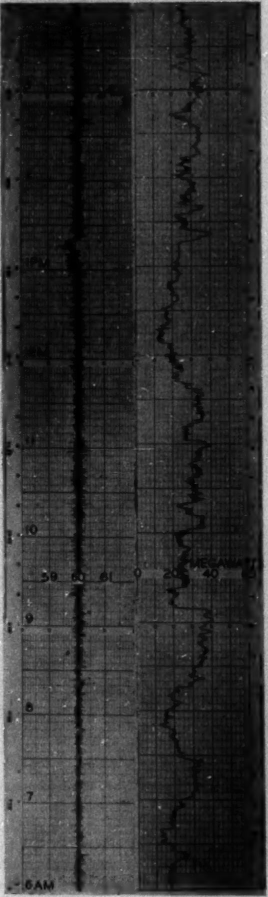


Figure 10. Typical chart—phase-angle difference—manual and automatic control



Montana system frequency

Morony output

Figure 11. Typical charts illustrating isolated operation of Montana system with automatic frequency control

angle control" and "selective-frequency control," will permit more definite conclusions regarding the relative merits of different types of control for the different operating conditions that may arise.

Figure 11 shows typical charts of system frequency and Morony plant output, when the Montana system is operating isolated from the other systems, with automatic-frequency control in service at Morony. A comparison of the frequency record with that shown in figure 9 is of interest and of significance. The wider band of the frequency record in figure 11 is evidence of the relatively larger load changes with respect to the generating capacity, on the Montana system, as compared to the entire interconnected group of systems.

One of the difficult features in the control problem of this interconnection is the rapid power fluctuation as shown by the typical high-speed chart of power flow through Burke, figure 12. The governors and water wheels that are regulated by the phase-angle controller are not fast enough to eliminate these fluctuations, as tests have shown that the output of the generators does not respond to the control impulses for $4^1/_2$ to 5 seconds. While the controller helps to lessen the magnitude of the surges, it is obvious that any slow response of the governor, gate mechanism, and water column tends to lead away from the desired results. Further attempts are being made to secure faster action of the governor, thereby reducing the delay between control impulses and water-wheel response.

A co-operative study was made to determine the cause of these rapid and persistent surges in power flow through Burke by a committee made up of representatives from each of the interconnected companies. Material for the study was obtained in a unique manner which has many possibilities for further use. It being desired to secure simultaneous readings of power flow through interconnecting ties as well as readings of power output at several stations, each company planned to install high-speed

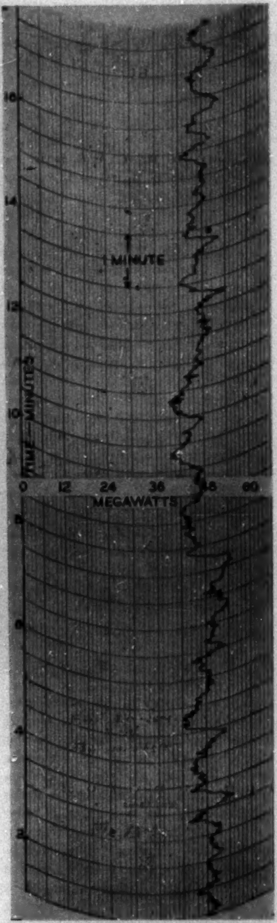


Figure 12. High-speed chart—megawatt interchange at Burke—power into Montana

wattmeters at key locations and one company would secure a high-speed frequency record.

In order to take numerous readings simultaneously, small homemade carrier-current receivers were installed at the location of each recording meter, with these receivers tuned to the phase-angle carrier frequency. In accordance with prearranged schedules, an observer at Spokane interrupted the carrier signal for a fraction of a second at regular intervals of 30 seconds. At each interruption the various observers marked their respective recording charts. In this manner simultaneous readings of power flow were taken on the systems of the Northwestern Electric Company, Puget Sound Power and Light Company, Pacific Power and Light Company, The Washington Water Power Company, and The Montana Power Company. As these charts indicated that five out of six times when the flow of power into Montana increased, the frequency was lower, indicating apparent power increases in Montana, and as it was not reasonable to believe that this proportion of the load changes occurred in Montana, further studies were made. It was concluded that many of the swings were not so much the direct result of actual load changes, as evidence of disproportionate and untimely increases in power input from certain systems and stations in endeavoring to restore system speed to normal—definitely indicating a lack of co-ordination of governors, both steam and hydraulic. If all governors had the same speed of response, the situation should be considerably better. This problem is still being studied co-operatively by the several companies.

Conclusions

Installation of automatic control based on phase angle has accomplished the following:

1. Made it possible more completely to utilize the capacity of the interconnection transmission facilities, practically up to the stable limit at all times, and thereby transfer greater amounts of energy. During one month, over 30,000,000 kilowatt-hours, an average of about 45,000 kw, or 70 per cent of the stable limit, were transferred.

2. Improved the operating performance of the interconnection by permitting large transfers of power without numerous separations of the systems. The average power flow possible with satisfactory performance is in the order of 20 to 25 per cent greater than that possible with manual control.

3. Relieved the operating personnel of much of the continuous and tedious supervision of the power interchange, decreased the number of telephone calls between operators and dispatchers, and minimized the manual operation of the controlling plant.

Discussion

V. M. Marquis (American Gas and Electric Service Corporation, New York, N. Y.): The paper by Messrs. Pierce and Hamilton describes another method of approach on the problem of regulating power flow between interconnected systems and is especially applicable where there are many intervening loads, the magnitude of which are of a highly fluctuating character. As interconnected systems have grown, it has become more and more apparent that merely tying together systems does not mean interconnected system operation unless the technique of operating these interconnections is properly developed. Great strides have been made over the past few years in developing this technique and there still remains work to be done.

On most large interconnected systems it has become apparent that to regulate properly the power flow between systems and thus maintain schedules manual control alone is not in general satisfactory. Some form of automatic control is usually necessary. For the most part it has been possible to work out a combination of frequency and tie-line control by simply using an indication of the power flow on tie lines either at the point at which the regulating is done or by telemetering from the proper point on the tie line.

The problem which the authors of this paper had to meet is somewhat more difficult due to the limitations in capacity of the lines and due to the nature of the loads tapped off between the systems. The result obtained in this case is ample proof of the effectiveness of the control system described by the authors. Although this development may have rather limited application, it is gratifying to know that another tool has been developed for improving the operating technique of interconnected systems.

A. N. Geyer (Woodward Governor Company, Seattle, Wash.): It has been a pleasure to hear Mr. Pierce talk about this unique control arrangement, about which we have been hearing good reports at several other meetings here in the Northwest during the past year or so. It has been my privilege to study even more accurate charts representing its performance, these being high-speed charts taken like the one of figure 12 at about one inch per minute. When such charts are assembled as in figure 9, with time markings all coinciding, the record of the phase angle is seen to follow very closely the changes of power in the tie line. Likewise, a further comparison of the Morony output curve shows very definitely the extent to which these are in agreement or conflict.

For example, take a conspicuous increase in power flow toward Montana, and straight below we look for an upswing of Morony output, with whatever delay actually occurs. Since this is given in the paper as being often four or five seconds, it seems unfortunate that the telemetered indication of upswing, after being transmitted immediately for several hundred miles to the generating station, is there delayed like this in going the remaining few feet to get the needed response on the prime mover.

Regarding the later reference to the unco-ordinated action of various governors on the interconnected systems, this would seem very likely when remembering that a good many of these are early-day models, thus not representing a high-grade performance on a par with this other part of the equipment concerned in this new method of control. A direct showing of how such old governors often perform may be seen in those particular plants where the several units are in a line enabling an observer at one end and looking across them to see the relative movements, and as the speed changes in regular manner, the governor movements are seen not to be together, but even traveling in opposite directions at times, indicating that they can't all be right. From what I have observed I feel this is an explanation of earlier reported hunting swings across this Northwest interconnection that was observed to occur a few times a minute in a plain showing on the charts. But the case is different with modern precision governors, contrary to the commonly expressed fear of "governors fighting each other," for if all are very sensitive to start early in any speed change, and are stabilized to stop where needed through proper dashpot action insured by correct field adjustments, they don't do the wrong thing any more than does some other highly developed precision apparatus. Then with all working correctly in response to any change in speed, their benefits are cumulative in maintaining close speed.

To bear out these statements by referring to a situation of interconnection and power regulation that is a very close parallel to the problem under discussion, I want to say that during the past year I have been working on improvements on governors where a rough mining load at one end near a group of hydro plants is connected over the mountains with a larger system which has a regular lighting and commercial load but uses an automatic frequency controller. The situation was just the same as here, in that the tie-line loading must not be allowed to go high enough to cause difficulty. Hence in order to prevent the heavy mining load fluctuations from flowing through the interconnection, these changes must be absorbed largely on their own system, that is, by their own hydro units. Now this must be done in spite of the average frequency controller on the other system. This called for the highest order of sensitivity in these governors, also the fastest rate of operation practicable. By modernizing these governors, they were put into the class of good modern governors and by using the very smallest droop practicable, they were enabled to respond well to every quick change of speed to take most of the load changes on their own system. In order to help hold the steady-state loading desired in the tie line, a telemetering arrangement was put in to indicate in the controlling station the power flow in the interconnection. This is valuable to the operator in avoiding slow drifts at any time including those during the beginning and end of working shifts at the mines.

In order to assist in this holding of the average loading in the tie line, it is the plan to go farther and make the whole thing automatic, by arranging to put this tie-line loading influence directly on the governors. This is being done, however, in a manner considerably different from the usual practice of using the synchronizing motor, by making the action direct on the governor controls. It is hoped that more operating results of this whole equipment may be reported later.

Sherwin H. Wright (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In system planning and design it is usually transient stability, rather than steady-state stability that receives first consideration. This is logical, for steady-state stability limits are usually much higher and less important than the transient stability limits. Attention has therefore been focused for some time on methods of increasing transient stability limits, resulting in such developments as high-speed breakers and relays, quick-response excitation, and most recently, high-speed reclosing breakers.

During this development period system interconnections have become the rule rather than the exception and while transient stability of such interconnections is still lower than steady-state limits, yet the latter are for various reasons quite important, as in the case described in this paper by Pierce and Hamilton. The authors describe a case involving several large-size systems as evidenced by the capacities given in the article:

	Kilowatts
The Montana Power Company	250,000
The Washington Water Power Company	200,000
Puget Sound Power and Light Company	300,000
Pacific Power and Light Company and Northwestern Electric Company	120,000
Portland General Electric Company	175,000
Washington Gas and Electric Company	30,000

Thus it is seen that the total generating capacity involved is considerable, and it is quite probable that there are similar system groups in existence where phase-angle control apparatus can be and will be successfully applied.

It is pertinent to note that this phase-angle control equipment, since it controls the phase angle, is applicable in situations where phase-angle difference is a suitable and preferable control index. In the application described by the authors it was recognized (1) that the highest limit is the steady-state stability limit, (2) that the steady-state voltage limit is somewhat lower, and (3) that the control should be set for a value lower than (2) in order to allow margin for continual power swings between the systems.

As stated by the authors, the reason that kilowatt loading in itself is not a proper index is because along the interconnection there are loads which vary in both location and magnitude; such a situation gives phase-angle the preference over kilowatt loading as a control index. Under such conditions of variable location and magnitude of intermediate loads, phase-angle control may find successful application in not only cases where steady-state stability or steady-state voltage determines the practical operating limit, but also in cases where the practical limit is determined by transient stability for faults on the interconnection itself.

H. L. Melvin (Ebasco Services, Inc., New York, N. Y.): This discussion of the paper by Messrs. Pierce and Hamilton is presented for the purpose of amplifying somewhat the section dealing with operating experience. Because of the familiarity of the discusser with the interconnected operation of the Pacific Northwest systems during the past several years, the liberty is being taken of relating two or three items of interest which might more properly be presented by representatives of the several utility companies.

The paper gives a brief report of the high-speed chart-marking tests at interconnection points and controlling power stations. It was suspicioned that variations in governor sensitivity and dead time between the closing of governor-motor contacts and the kilowatt response of the generators were responsible to a large degree for the occasional large power swings and to some extent the amplitude of the regularly occurring swings. To check this suspicion, a photoelectric frequency recorder with an auxiliary mechanical pen was borrowed from the General Electric Company and preliminary tests were made to determine the characteristics of several governors on the interconnected systems. These tests demonstrated conclusively that unco-ordinated governor and automatic phase-angle control action could be accountable for the magnitude of at least a part of the power swings. The tests were not complete nor was the frequency recorder alone adequate for the purpose. Accordingly, the Washington Water Power Company purchased and has just received a General Electric, two-element, photoelectric recorder which will make a record of frequency and kilowatts; also, it has two auxiliary pens for mechanical attachment, all on one variable high-speed chart. It is planned to use this instrument for analyzing, adjusting, and co-ordinating the operation of governors, also automatic tie-line load-control equipment on the interconnected systems. The recording and analyzing of power flow at interconnection points with variations in system frequency also can be accomplished.

Since the paper was prepared, there has been placed in service an automatic tie-line load-control equipment of General Electric Company manufacture between the systems of the Puget Sound Power and Light and The Washington Water Power companies. Kilowatts interchange on the Chelan-Wenatchee interconnection is transmitted by carrier signals to the White River plant on the Puget Sound Company's system and the output of this plant is automatically controlled to hold the interchange at desired values. It, like the automatic phase-angle equipment between Washington and Moutana, is working successfully.

Though all of the systems from Montana to the Pacific Coast are now operated successfully in parallel, it is expected that improvements will be realized through further application of automatic load-control equipment and better co-ordination of governor and automatic control action after completion of tests with the photoelectric frequency and kilowatt meter. Progress will be made only if this work is carried on in the splendid co-operative spirit which exists among the operating organizations; also between the electrical, hydraulic, and mechanical engineers involved.

It should be remembered that the capacities of the interconnections are rather limited and distances in the Northwest are relatively great. However, with maximum utilization of the interconnection facilities it is believed that present capacities will be adequate for some time. Economics and service are controlling considerations rather than the control of interchange to precise demand values, which in reality should have little if any place in interchange agreements.

R. E. Pierce and **B. W. Hamilton:** The authors are gratified at the interest shown in the new method of control described. We concur in the view of the several discussers, and would like to emphasize some of these points.

Mr. Geyer brought out very well that the apparent lack of co-ordination between the various governors and with the automatic control equipment would perhaps not exist were the governors of modern and sensitive design and properly adjusted. We also anticipate that somewhat improved co-ordination and over-all system performance can be obtained, even with the present installed governors, following completion of the further studies mentioned and following whatever maintenance and adjustments that may be indicated as necessary. This will probably include making certain governors less sensitive for small speed variations (and considerable more thought being given to speed-droop setting). Mr. Geyer also describes a somewhat parallel case, where it is planned that automatic control of an interconnection will result in practically all of the load swings on one system being absorbed on that system. The results secured will be of interest, and we hope will be reported.

As Mr. Marquis points out, some form of automatic control is usually necessary to maintain scheduled power flows between systems, but that in the situation described, we were attempting to do somewhat more than maintain a scheduled flow. Inasmuch as any predetermined scheduling of power flow at some arbitrarily selected interconnection point (usually wherever ownership of facilities changes) does not always result in maximum utilization of the transmission facilities, it is pertinent, as Mr. Melvin has done, to raise the question as to whether definite scheduling of interchange flows to precise demand values is always the best over-all practice. In our opinion, there are probably many interconnections where the full capabilities of

Static Power Limits of Synchronous Machines

By CHARLES F. DALZIEL
ASSOCIATE AIEE

THIS paper presents an analysis of the determination of static power limits of power systems and offers an accurate method considering resistance, shunt loads, saliency, and saturation. The conventional methods of determining the static power limits of systems composed of synchronous machines and shunt loads based on unsaturated cylindrical-rotor theory give only approximate results. It is fortunate that the actual maximum power transfer is generally larger than the calculated value obtained from the customary analysis. Machine saturation and saliency increase the power limit, the difference decreasing with increased system impedance. The validity of the method is shown by the check between experimental values obtained from three small machines and theoretical curves. It was found that errors due to neglecting saturation are more significant than those due to neglecting saliency.

Static power limits are important because they may be exceeded under emergency conditions, even though the system is designed to operate well below these limits normally. Although transient stability will probably continue to be regarded the most important factor limiting the power transfer through most practical systems, recent accomplishments which increase the transient limit reduce the difference between the transient and static power limits, and an accurate analysis of static power limits is of increasing importance. It is possible that studies made on operating systems might indicate minor or preferred changes in load dispatching which would result in higher power limits and greater margins of stability, ultimately reflecting increased reliability of system operation.

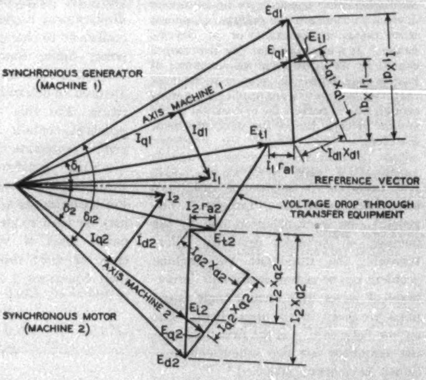


Figure 1. Vector diagram — synchronous generator-motor system

Much data included in this paper were obtained by graduate and senior students under the writer's supervision. No serious experimental difficulties were encountered, and a number of the runs were repeated and rechecked at various times during the past three years.

It has been shown that inertia effects may cause a difference between the maximum power output and the static stability limit.[1] In the experimental work reported here, the difference between the maximum power and static stability limits was negligible for synchronous generator-motor systems. Systems involving two generators and shunt loads were much more stable, and it was possible to operate the machines with displacement angles up to 91 electrical degrees. However, when the displacement angles exceeded that corresponding to maximum power output, the machines were in unstable equilibrium and required very careful supervision, any slight misadjustments causing immediate loss of synchronism. It was concluded that for practical purposes static stability may be regarded as a problem of stable equilibrium in which all transient phenomena and inertia effects are neglected.[2] This is in agreement with the recent report of the AIEE committee on the subject.[3]

The stability limit is determined by the first synchronous machine to reach its maximum power output. A system is considered stable if all the machines are operating in stable equilibrium. It will be unstable if any machine is unstable.[2] A machine operates in stable equilibrium if each additional increment of load is accompanied with a corresponding increasing increment of displacement angle. For each machine a critical load will finally be reached which is the maximum load the machine can carry and operate in stable equilibrium with respect to the other machines. If further attempt be made to load the

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Paper number 38-108, recommended by the AIEE committee on electrical machinery, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted June 8, 1936; made available for preprinting July 11, 1938.

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The author wishes to acknowledge and thank L. R. Fink, A. H. Keith, and A. J. Gilardi, graduate students, and also N. Y. A. student workers at the University of California, for their valuable assistance. He wishes also to acknowledge and thank the several companies for their interest, comments, and system constants furnished for these analyses.

1. For all numbered references, see list at end of paper.

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the facilities should be utilized, and efforts made to equitably share the burdens and benefits between the interconnecting parties, rather than to make extraordinary efforts to control the operation to maintain arbitrarily predetermined interchange values.

We agree with the opinion expressed by Mr. Wright that phase-angle control may find application where the practical limit is determined by transient stability, a well as in cases, such as the one described by the authors, where it was desired to operate beyond the transient stability limit and as close as practicable to the steady-state limit. In either case, the phase-angle difference between systems is the basic determining factor between stable and unstable conditions.

machine, it will be found that increasing increments of displacement angle are accompanied by decreasing increments of load, and the machine will be unstable equilibrium.

Static stability refers to the normal power transfer through a system, and should not be confused with transient stability in which the effects of disturbances are considered. Two static power limits are distinguished:

I. The steady-state power limit is defined as the maximum power that may be transferred through a network without loss of synchronism when the machine excitations are maintained constant at the values necessary to produce the terminal voltages required for the initial system loading. It is assumed that the disturbance caused by the addition of each increment of load has ceased before the next increment is applied.[4]

II. The dynamic power limit is defined as the maximum power that may be transferred through a network without loss of synchronism when the machine excitations are varied either manually or by automatic devices to maintain the voltages at one or more points at constant, or at definite values. It is assumed that the disturbance caused by the addition of an increment of load is over before the next is added, and that the increments are applied at a slow enough rate to permit the restoration of the system voltages before the next increment is added.

The power-angle equations for a system involving n salient-pole machines have been taken from Longley.[5] The general case is complicated due to the number of variables involved. Fortunately, the important two-machine problem can be solved easily and derivations of power and current-angle equations are given in the appendices. The author believes this is the first time concise equations for two salient-pole machines have been published.[6]

Determination of the Steady-State Power Limit

The machine internal voltages are determined from the known or assumed load and substituted in equations 11 and 12 (see figure 1). The displacement angle (δ) is arbitrarily increased and the first machine to reach its maximum power output determines the power limit of the system. The steady-state limit is of minor interest in power systems, but it is important in determining the pull-out torque in synchronous machines using fixed excitation or manual control.

Determination of the Dynamic Power Limit

The dynamic power-angle curve intersects each of a family of steady-state power-angle curves at points of known voltage and current. The limit of stable equilibrium of a synchronous machine occurs when the dynamic curve intersects the first steady-state curve at its maximum point. This point may be determined by plotting stability coefficients for increasing values of power or displacement angle. The first stability coefficient to change sign determines the power limit. Since the voltages and currents are known, equations 13 and 14 are used to determine the stability coefficients. For this case the system impedances contain X_q of the machines, and E_d represents the voltage behind the quadrature synchronous reactance.

Although not stated in the paper by Edith Clarke and Lorraine,[2] their analysis may be used to determine the dynamic power limit of systems composed of three or more salient-pole machines if their equations 1 to 8 contain the X_q of the machines and the corresponding voltages behind X_q.

For cases in which the system voltages are maintained constant, all shunt loads including synchronous condensers and induction motors may be represented approximately by simple shunt impedances. This treatment assumes that the induction motor portion of the system load remains stable. Since the maximum power transfer is ordinarily limited by the synchronous machines on the system, this assumption should not introduce significant errors in the result. See references 2 and 7 for accurate treatment.

Consideration of Saturation

Saturation in the synchronous machines was neglected in the preceding discussion. The power or current equations give accurate results if saturated values of the machine X_q corresponding to the machine air gap voltages are used.[6,8] Determination of the steady-state power limit is complicated by variations of X_q and the correction for saturation with current. Fortunately, the dynamic limit, which is the most important from a practical standpoint, may be determined easily, since the voltages are known and the current or power is the independent variable. The saturated X_q corresponding to the air-gap voltage is obtained and E_q determined. These values are then used to determine the stability coefficients using equations

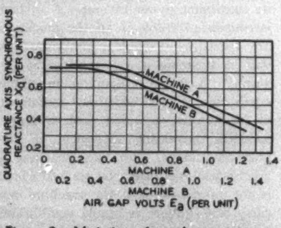

Figure 2. Variation of quadrature-axis synchronous reactance with air-gap voltage

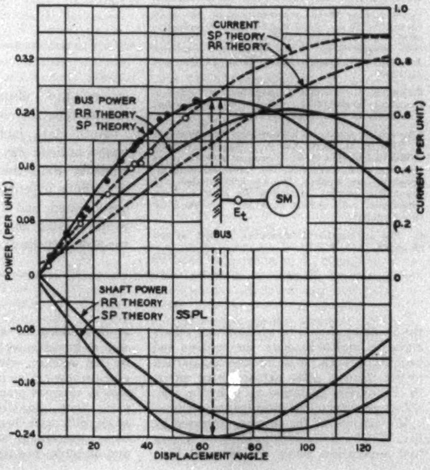

Figure 3. Unsaturated synchronous motor operating from an infinite bus

SP—Salient pole

RR—Round rotor

SM—Synchronous motor

SSPL—Steady-state power limit

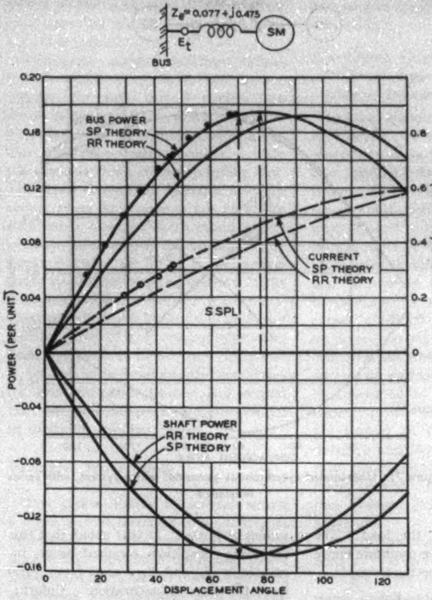

Figure 4. Unsaturated synchronous motor operating from an infinite bus, with series impedance

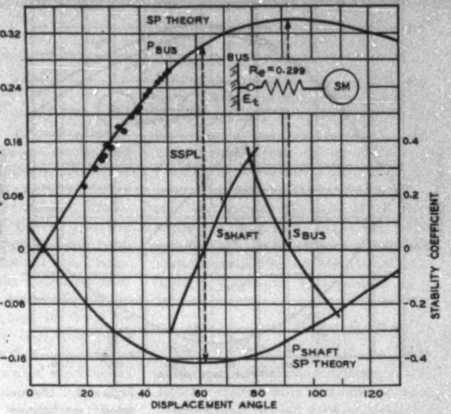

Figure 5. Unsaturated synchronous motor operating from an infinite bus, with series resistance

13 and 14. In this case the system impedances, Z_{11}, Z_{22}, and Z_{12} contain the machine X_{qs}, and the E_{ds} are the corresponding voltages behind the X_{qs}.

It is believed that the dynamic power limit may be approached in large power systems, since variations in load generally occur at a slow enough rate to permit modern voltage regulators and governors to maintain the system voltage and frequency. The method permits an accurate determination including resistance, saliency, and saturation. It should be of particular value in determining the maximum power transfer over single-circuit tie lines, as well as the emergency power limits of transmission systems in general.

Experimental Results

The laboratory work centered about two similar synchronous machines rated: 15 kva, 220 volts, 39.4 amperes, 1,200 rpm, 60 cycle, 3 phase. The quadrature axis synchronous reactances versus air-gap voltage are given in figure 2.*

The unsaturated machine constants are:

Machine A. $R_a = 0.044$, $X_l = 0.072$, $X_d = 1.15$, $X_q = 0.742$.

Machine B. $R_a = 0.045$, $X_l = 0.071$, $X_d = 1.10$, $X_q = 0.722$.

Steady-State Power and Current Characteristics, Unsaturated Synchronous Motor

Figures 3, 4, and 5 show calculated and test results of machine *A*, energized from an infinite bus at half voltage. With the excitation held constant, values of power and current were obtained for various loads up to pull-out. Theoretical power and current-angle curves using both round-rotor and salient-pole analyses were plotted to compare results. Although the round-rotor theory yields poor results with respect to displacement angle, the differences at pull-out were small, and the errors decreased with increased system impedance. The difference between the shaft and bus power was due to the I^2R loss in the machine and series impedance. Motor power was plotted negative to avoid confusion.

It was concluded that the common method of determining the *unsaturated* power limits should give results of sufficient accuracy for most engineering purposes. It will be shown that the effects of saturation are likely to cause much greater errors than those caused by neglecting saliency.

Steady-State Power Angle Curves, Synchronous Generator–Motor System

Figures 6 and 7 show calculated and test results with machine *B* driving machine *A*. With the excitations held constant, corresponding to approximately half voltage at no load, values of power were obtained for various loads up to pull-out. Again the agreement between test and calculated results substantiates the analysis and should lead to a better understanding of the unsaturated synchronous machine.

Figure 8 shows similar results under saturated conditions. For the steady-state test, the machine excitations were held constant corresponding to rated voltage at no load. The power-angle curve was obtained as before. Calculated power-angle curves using unsaturated machine constants are included for comparison and indicate the necessity for considering saliency and saturation if accurate results are required. It is obvious that this statement applies to both steady-state and dynamic power limits. It was concluded that errors due to neglecting saturation are more

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* Per unit quantities are used throughout this discussion. 1.00 per unit power equals 15 kw.

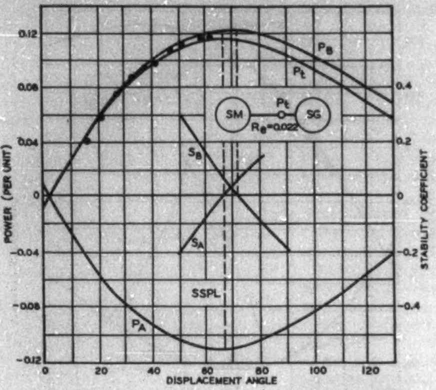


Figure 6. Unsaturated synchronous generator-motor system

SG — Synchronous generator

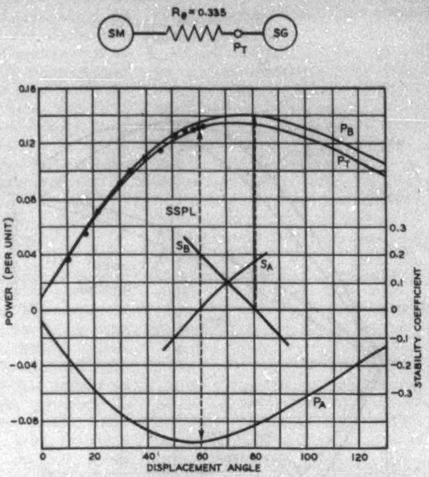


Figure 7. Unsaturated synchronous generator-motor system, with series resistance

significant than those due to neglecting saliency.

For the dynamic stability test, the machine excitations were varied to hold the motor terminal voltage and power factor constant at unity. The dynamic limit of the system was not reached due to limitations of equipment. The correlation between observed and calculated values for this highly saturated case is an indication of how accurately saturation may be included, since E_2 was held constant at rated value and E_1 varied from 1.00 to about 1.25.

Two Synchronous Generators, Variable Shunt Load, and Series Impedance

In this test an attempt was made to duplicate the general two-machine problem. A variable three-phase balanced resistance load connected to the terminals of generator *A* was energized from both generators. The power supplied at the load was held in the ratio $P_1/P_2 = 2/1$, and the frequency was held at 60 cycles. Starting at no load, the delivered power-angle curves were obtained by gradually increasing the shunt load.

For the steady-state test the machine excitations were held constant corresponding to a no-load voltage, $E_2 = 0.4515$. The load was applied and the power-angle curve obtained. It is interesting to note that the two generators were inherently more stable than during the generator-motor tests and several points were obtained in the region of unstable equilibrium (see figure 9).

For the dynamic test, the machine excitations were varied to hold the load voltage constant ($E_2 = 0.4515$), and the power factors unity at the load. The method of calculating the dynamic curve will be illustrated by a specific example.

Let $P = 0.50$, $P_1 = 0.333$, and $P_2 = 0.167$

$$I = P/E = 1.108\underline{/0},$$

$$I_1 = 0.738\underline{/0} \text{ and } I_2 = 0.370\underline{/0}$$

The series impedance, $Z_e = 0.123 + j0.692$
$= 0.702\underline{/79.9}$

$$\begin{aligned}
E_1 &= E_2 + I_1Z_e \\
&= 0.4515\underline{/0} + 0.738\underline{/0} \times 0.702\underline{/79.9} \\
&= 0.744\underline{/43.2} \\
E_{a1} &= E_1 + I_1Z_{l1} \\
&= 0.744\underline{/43.2} + 0.738\underline{/0}(0.045 + j0.071) \\
&= 0.804 \\
X_{q1} &= j0.571 \text{ from figure 2} \\
E_{q1} &= E_1 + I_1Z_{q1} \\
&= 0.744\underline{/43.2} + \\
&\qquad 0.738\underline{/0}\,(0.045 + j0.571) \\
&= 1.096\underline{/58.2}
\end{aligned}$$

By a similar process, $X_{q2} = j0.723$ and

$$\begin{aligned}
E_{q2} &= 0.538\underline{/29.7} \\
\delta_{12} &= 58.2 - 29.7 = 28.5 \\
z_1 &= (0.045 + 0.123) + j(0.571 + 0.692) \\
&= 1.272\underline{/82.4} \\
z_2 &= (0.044 + j0.723) = 0.724\underline{/86.5} \\
z_3 &= E_2/I = \frac{0.4515\underline{/0}}{1.108\underline{/0}} = 0.407\underline{/0} \\
z_{12} &= z_1 + z_2 + z_1z_2/z_3 = 3.14\underline{/129.6} \\
\alpha_{12} &= -39.6 \\
S_1 &= \cos(28.5 + 39.6) \\
&= 0.373 \text{ from equation 13} \\
S_2 &= \cos(28.5 - 39.6) \\
&= 0.981 \text{ from equation 14}
\end{aligned}$$

The complete curve was obtained by repeating the process using increasing values of power. It was noted that this operating condition required large increases in E_1, and X_{q1} varied from 0.700 to 0.468 due to saturation. Unfortunately the dynamic stability limit was not reached due to limitations of the equipment.

The dash-line curves of figure 10 illustrate the determination of the dynamic limit when the machine excitations were varied to hold both generator terminal voltages equal at 0.4515. The stability coefficient S_{1a} indicates a theoretical maximum delivered load of 0.280 per unit. Although it was possible to supply a slightly greater load, the system became very unstable and soon lost synchronism.

The solid-line curves of figure 10 illustrate the determination of the dynamic limit when a 7.5-kva 220-volt 19.7-ampere cylindrical-rotor machine used as a synchronous condenser was connected at the load to maintain voltages. During this test the power supplied to the load was held in the ratio $P_1/P_2 = 2/1$, and machine *A* was held at unity power factor. The losses of the condenser were considered as part of the load. Stability coefficient S_{1b} determines the stability limit of the system with a delivered power equal to 0.270 per unit.

The assumption that a synchronous condenser can be replaced with a static condenser for dynamic analysis is verified from inspection of the curve. Experimental points using three different static condensers are in good agreement with the other results.

Calculated curves showing various factors which may limit the power transfer for this typical system are given in figure 11, in which the ratio P_1/P_3 has been varied over a wide range. The assumed operating conditions were the same as those of the preceding test. The rapid increase in dynamic stability occurs when the machines are operating with nearly constant displacement angles between them, a condition similar to the dynamic curve of figure 9. A similar study applied to practical systems might indicate that large increases in the maximum power would be realized by minor redistribution of the system generating capacity. Curves of this type should be of interest in determining the conditions for optimum power transfer and suggest the extreme conditions to be considered in transient stability studies.

Practical Application

The power limits of several actual systems were determined to illustrate the possibilities in the way of practical application. Although the actual power limits were unknown, available operating experience was consistent with the conclusions reached. The reactances of most of the machines were not available and typical unsaturated values were used.[3,9,10] These were multiplied by 0.90 to give a conservative correction for saturation.[11]

San Diego Consolidated Gas and Electric-Southern California Edison Interconnection

In the summer of 1932, the small 50/60-cycle frequency converter interconnection of the San Diego Consolidated Gas and Electric-Southern California Edison Company was operated for a considerable period in utilizing "spillway" from the Edison system. During certain times of the day when the system loads were changing most, it was a common experience for the converter load to increase to some value slightly in excess of 7,500 kw, and its breakers would trip on out-of-step operation. The converter excitation system included one automatic voltage regulator which could be connected to either side of the machine. It was found possible to choose the system on which instability would develop, and best results were obtained with the regulator connected on the 60-cycle system. It was desired to hold the average converter load at about 5,000 kw.

In calculating the dynamic power limit it was assumed that all terminal voltages were maintained constant at rated values and that the San Diego system loads remained constant at the average values given in figure 12. It was concluded that the 60-cycle side of the converter should control the dynamic stability of the system and permit a maximum load transfer of approximately 16,400 kw (see figure 13). The steady-state power limit of the 50-cycle system was determined at approximately 8,000 kw assuming constant converter excitation corresponding to an initial load transfer of 5,000 kw. The result compares favorably with operating experience and stresses the value of automatic excitation equipment. It is the writer's opinion that the higher power limit could have been approached and the operating difficulties reduced had modern excitation equipment been provided.

Hetch Hetchy-Pacific Gas and Electric System

The City of San Francisco's Hetch Hetchy system is an example of a large hydroelectric project feeding power over a long transmission line into an infinite bus (see figure 14). Since the lines were originally designed for 154 kv, but were insulated and operate at approximately 110 kv, and the operating orders limit the power transmitted over one line to 40,000 kw, it was thought that a study of the maximum or emergency power limits might be valuable as well as instructive. The dynamic power limit was determined at approximately 92,500 kw for normal operating voltages and

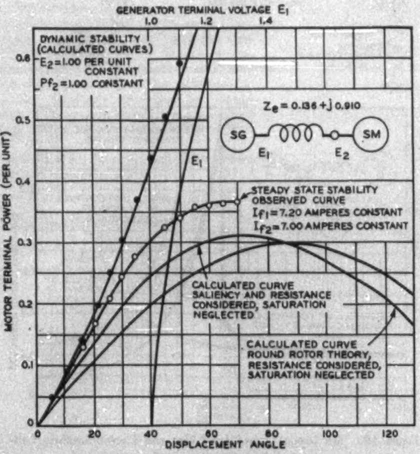


Figure 8. Saturated synchronous generator-motor system with series impedance

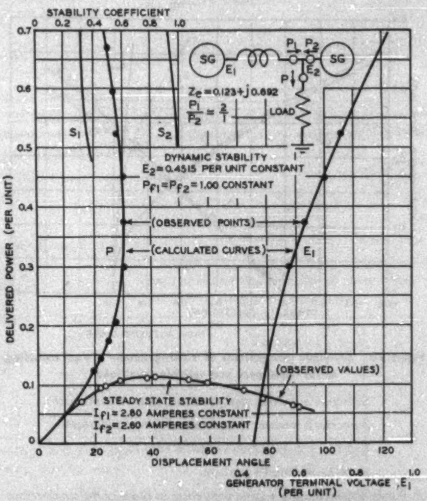


Figure 9. Steady-state and dynamic power limits of a two-generator system including series impedance and variable shunt load

one line in service. With the exception of the generator quadrature reactances, actual system constants were used.[13]

Several times during emergency switching and trip-outs, full load (approximately 77,000 kw) was dumped on the remaining line for a few minutes until the station operators had time to readjust the governors. It was reported that although there was naturally quite a disturbance when one line went out, (depending upon the severity and location of the trouble) in no case was there any indication of instability when one line was carrying the load. This is consistent with the calculated value and should increase confidence in the proposed analysis.

Boulder Dam System

The dynamic power limit of the Boulder Dam-City of Los Angeles system should be of considerable importance, since the system originally designed for a transient stability limit of 235,000 kw has already carried peak loads of approximately 281,000 kw. System data were taken from Scattergood's paper,[13] and supplemented with the actual quadrature reactances of the Boulder Dam generators. The equivalent steam-plant impedances were assumed by the writer. The Los Angeles load was assumed at 0.80 power factor lagging and the theoretical synchronous condenser kva required to maintain the system voltages was computed. Using a factor of 0.80 to allow for imperfections in assumptions, system hunting, etc., the dynamic delivered power limit of the 275-kv transmission system was approximately 294,000 kw with the steam plant floating. The power limit increased materially as the steam plant was given load. It may be interesting to mention that similar studies using round-rotor theory and neglecting saturation lowered the theoretical dynamic power limit 43,000 kw. Perhaps a more important problem involves the dynamic power limit with one line section out of service; however, lack of time prevented extended investigation.

The above examples and practical problems should be sufficient to illustrate the application of the static stability limits of power systems. It is believed that the effects of saliency and saturation are of sufficient importance to warrant consideration, especially for practical cases where an accurate determination of the maximum power limits are under question.

Appendix I

Derivation of Power-Angle Equations

The following is an extension of the familiar power-angle equations. Lower-case bold-face letters are used to denote

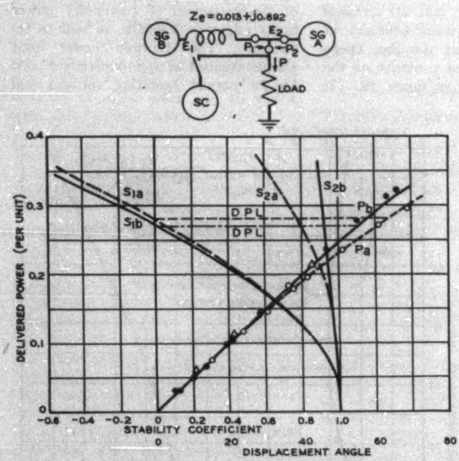


Figure 10. Dynamic power limits of a two-generator system including series impedance and variable shunt load

Subscript *a* denotes data obtained with no condenser

Subscript *b* denotes data obtained using condensers

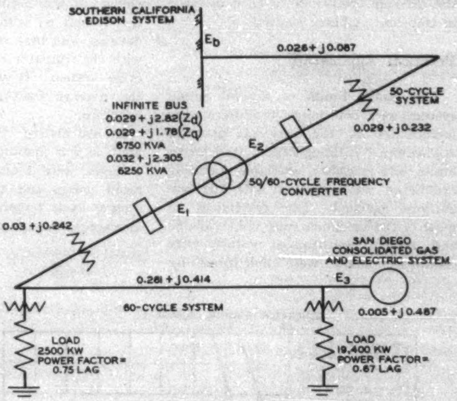
Generator *A* excitation varied to hold Pf_2 at unity

$E_1 = E_2 = 0.4515$ constant

△ Points obtained using a static condenser in place of the synchronous condenser

DPL—Dynamic power limit *SC*—Synchronous condenser

Figure 11. Factors controlling power limit of system



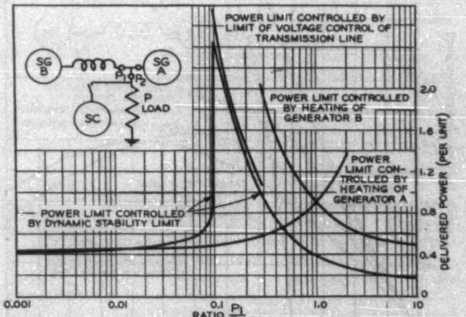


Figure 12. San Diego Consolidated Gas and Electric-Southern California Edison interconnected system

System impedances on 20,000-kva base

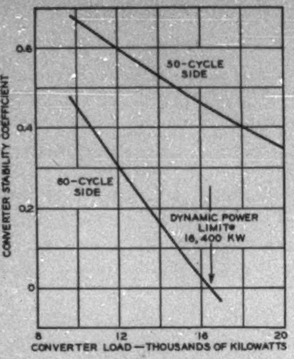


Figure 13. Dynamic power limits of 50/60-cycle converter

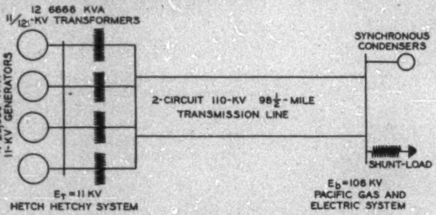


Figure 14. Hetch Hetchy-Pacific Gas and Electric interconnected system

vectors, and capitals represent scalars. Subscript *q* used with current or voltages represents the quadrature-axis component; similarly, subscript *d* denotes the direct-axis component. The first numerical subscript following *q* or *d* refers to the voltage producing that current. The second numerical subscript, or when only one subscript is used, denotes the branch or machine considered. Subscripts *q* or *d* used with reactance refer to the synchronous-machine constants according to present usage. The system impedances are denoted by a double subscript notation consistent with the above.[2,5,6,10,14]

Referring to figure 1, the shaft power output of machine 1 is:

$$P_1 = E_{q1} I_{q1} \qquad (1)$$

By the principle of superposition

$$I_{q1} = I_{q11} - I_{q21} \ldots - I_{qn1} \qquad (2)$$

$$I_{d1} = I_{d11} - I_{d21} \ldots - I_{dn1} \qquad (3)$$

also

$$i_{11} = \frac{e_{q1}}{z_{11}},\ i_{21} = \frac{e_{q2}}{z_{21}},\ \ldots\ i_{n1} = \frac{e_n}{z_{n1}}$$

The impedances contain the system parameters, machine resistance, and the quadrature-axis synchronous reactance. Obviously the scalar value of $Z_{1n} = Z_{n1}$, and $\phi_{1n} = \phi_{n1}$. Let δ_n be the angle of e_{qn} from a reference.

$$\delta_{1n} = \delta_1 - \delta_n$$

Introducing scalars:

$$I_{q11} = \frac{E_{q1}}{Z_{11}} \cos \phi_{11},\ I_{q21} = \frac{E_{q2}}{Z_{12}} \cos(\delta_{12} + \phi_{12}),\ \ldots$$

$$\ldots I_{qn1} = \frac{E_{qn}}{Z_{1n}} \cos(\delta_{1n} + \phi_{1n})$$

and

$$I_{d11} = \frac{E_{q1}}{Z_{11}} \sin \phi_{11},\ I_{d21} = \frac{E_{q2}}{Z_{12}} \sin(\delta_{12} + \phi_{12}),\ \ldots$$

$$\ldots I_{dn1} = \frac{E_{qn}}{Z_{1n}} \sin(\delta_{1n} + \phi_{1n})$$

Substituting $\alpha_{1n} = 90 - \phi_{1n}$ in the foregoing:

$$I_{q1} = \frac{E_{q1}}{Z_{11}} \sin \alpha_{11} + \frac{E_{q2}}{Z_{12}} \sin(\delta_{12} - \alpha_{12}) \ldots + \frac{E_{qn}}{Z_{1n}} \sin(\delta_{1n} - \alpha_{1n}) \qquad (4)$$

and

$$I_{d1} = \frac{E_{q1}}{Z_{11}} \cos \alpha_{11} - \frac{E_{q2}}{Z_{12}} \cos(\delta_{12} - \alpha_{12}) \ldots - \frac{E_{qn}}{Z_{1n}} \cos(\delta_{1n} - \alpha_{1n}) \qquad (5)$$

Substituting (4) in (1):

$$P_1 = \frac{E_{q1}^2}{Z_{11}} \sin \alpha_{11} + \frac{E_{q1}E_{q2}}{Z_{12}} \sin(\delta_{12} - \alpha_{12}) + \frac{E_{q1}\,E_{q3}}{Z_{13}} \sin(\delta_{13} - \alpha_{13}) \ldots + \frac{E_{q1}\,E_{qn}}{Z_{1n}} \sin(\delta_{1n} - \alpha_{1n})$$

Similarly,

$$P_k = \frac{E_{qk}^2}{Z_{kk}} \sin \alpha_{kk} + \frac{E_{qk}E_{q1}}{Z_{k1}} \sin(\delta_{k1} - \alpha_{k1}) + \frac{E_{qk}\,E_{q2}}{Z_{k2}} \sin(\delta_{k2} - \alpha_{k2}) \ldots + \frac{E_{qk}E_{qn}}{Z_{kn}} \sin(\delta_{kn} - \alpha_{kn}) \qquad (6)$$

The relation between E_t and E_q is apparent from figure 1:

$$E_{t1} = E_{q1} + I_{d1}(X_{d1} - X_{q1})$$

Substituting equation 5:

$$E_{t1} = E_{q1}\left(1 + \frac{(X_{d1} - X_{q1})}{Z_{11}} \cos \alpha_{11}\right) - E_{q2}\frac{(X_{d1} - X_{q1})}{Z_{12}} \cos(\delta_{12} - \alpha_{12}) \ldots - E_{qn}\frac{(X_{d1} - X_{q1})}{Z_{1n}} \cos(\delta_{1n} - \alpha_{1n})$$

Let

$$A_1 = 1 + \frac{(X_{d1} - X_{q1})}{Z_{11}} \cos \alpha_{11}$$

$$A_n = 1 + \frac{(X_{dn} - X_{qn})}{Z_{nn}} \cos \alpha_{nn}$$

$$B_{12} = \frac{(X_{d1} - X_{q1})}{Z_{12}}$$

$$B_{kn} = \frac{(X_{dk} - X_{qk})}{Z_{kn}}$$

Then

$$E_{t1} = A_1 E_{q1} - E_{q2}B_{12} \cos(\delta_{12} - \alpha_{12}) - E_{q3}B_{13} \cos(\delta_{13} - \alpha_{13}) \ldots - E_{qn}B_{1n} \cos(\delta_{1n} - \alpha_{1n})$$

Similarly

$$E_{tk} = -E_{q1}B_{k1} \cos(\delta_{k1} - \alpha_{k1}) \ldots + A_k E_{qk} \ldots - E_{qn}B_{kn} \cos(\delta_{kn} - \alpha_{kn}) \qquad (7)$$

Values of $E_{q1}, E_{q2}, E_{qk} \ldots E_{qn}$, are obtained from simultaneous solution of these equations, for example, see equation 8.

$$E_{qk} = \frac{\begin{vmatrix} A_1 & -B_{12}\cos(\delta_{12} - \alpha_{12}) & \ldots & E_{t1} & \ldots & -B_{1n}\cos(\delta_{1n} - \alpha_{1n}) \\ -B_{21}\cos(\delta_{21} - \alpha_{21}) & A_2 & \ldots & E_{t2} & \ldots & -B_{2n}\cos(\delta_{2n} - \alpha_{2n}) \\ \ldots & \ldots & \ldots & \ldots & \ldots & \ldots \\ -B_{k1}\cos(\delta_{k1} - \alpha_{k1}) & -B_{k2}\cos(\delta_{k2} - \alpha_{k2}) & \ldots & E_{tk} & \ldots & -B_{kn}\cos(\delta_{kn} - \alpha_{kn}) \\ \ldots & \ldots & \ldots & \ldots & \ldots & \ldots \\ -B_{n1}\cos(\delta_{n1} - \alpha_{n1}) & -B_{n2}\cos(\delta_{n2} - \alpha_{n2}) & \ldots & E_{tn} & \ldots & A_n \end{vmatrix}}{\begin{vmatrix} A_1 & -B_{12}\cos(\delta_{12} - \alpha_{12}) & \ldots & -B_{1k}\cos(\delta_{1k} - \alpha_{1k}) & \ldots & -B_{1n}\cos(\delta_{1n} - \alpha_{1n}) \\ -B_{21}\cos(\delta_{21} - \alpha_{21}) & A_2 & \ldots & -B_{2k}\cos(\delta_{2k} - \alpha_{2k}) & \ldots & -B_{2n}\cos(\delta_{2n} - \alpha_{2n}) \\ \ldots & \ldots & \ldots & \ldots & \ldots & \ldots \\ -B_{k1}\cos(\delta_{k1} - \alpha_{k1}) & -B_{k2}\cos(\delta_{k2} - \alpha_{k2}) & \ldots & A_k & \ldots & -B_{kn}\cos(\delta_{kn} - \alpha_{kn}) \\ \ldots & \ldots & \ldots & \ldots & \ldots & \ldots \\ -B_{n1}\cos(\delta_{n1} - \alpha_{n1}) & -B_{n2}\cos(\delta_{n2} - \alpha_{n2}) & \ldots & -B_{nk}\cos(\delta_{nk} - \alpha_{nk}) & \ldots & A_n \end{vmatrix}} \qquad (8)$$

The power equations for the two machines are obtained from equation 6. Substituting ${}_2\delta_1 = -\delta_{12}$ and rearranging:

$$\left.\begin{aligned} P_1 &= \frac{E_{q1}^2}{Z_{11}} \sin \alpha_{11} + \frac{E_{q1}E_{q2}}{Z_{12}} \sin(\delta_{12} - \alpha_{12}) \\ P_2 &= \frac{E_{q2}^2}{Z_{22}} \sin \alpha_{22} - \frac{E_{q1}E_{q2}}{Z_{12}} \sin(\delta_{12} + \alpha_{12}) \end{aligned}\right\} \quad (9)$$

From equation 7:

$$E_{t1} = A_1E_{q1} - E_{q2}B_{12}\cos(\delta_{12} - \alpha_{12})$$
$$E_{t2} = -E_{q1}B_{21}\cos(\delta_{12} + \alpha_{12}) + A_2E_{q2}$$

Solving for E_{q1} and E_{q2} and simplifying:

$$\left.\begin{aligned} E_{q1} &= \frac{A_2E_{t1} + E_{t2}B_{12}\cos(\delta_{12} - \alpha_{12})}{A_1A_2 + B_{21}B_{12}\sin^2 \alpha_{12} - B_{21}B_{12}\cos^2 \delta_{12}} \\ E_{q2} &= \frac{A_1E_{t2} + E_{t1}B_{21}\cos(\delta_{12} + \alpha_{12})}{A_1A_2 + B_{21}B_{12}\sin^2 \alpha_{12} - B_{21}B_{12}\cos^2 \delta_{12}} \end{aligned}\right\} \quad (10)$$

Substituting equation 10 in equation 9 gives the power-angle equations for two salient-pole machines. The stability coefficients are determined by differentiation. Only significant terms are included. Common multiplying factors and terms becoming negligible at the maximum power output have been omitted.

POWER-ANGLE EQUATIONS FOR TWO SALIENT-POLE MACHINES

The shaft power output of the leading machine is:

$$P_1 = \left[\frac{A_2E_{t1} + E_{t2}B_{12}\cos(\delta_{12} - \alpha_{12})}{A_1A_2 + B_{12}B_{21}\sin^2 \alpha_{12} - B_{12}B_{21}\cos^2 \delta_{12}}\right]^2 \frac{\sin \alpha_{11}}{Z_{11}} + \left[\frac{A_2E_{t1} + E_{t2}B_{12}\cos(\delta_{12} - \alpha_{12})}{A_1A_2 + B_{12}B_{21}\sin^2 \alpha_{12} - B_{12}B_{21}\cos^2 \delta_{12}}\right] \times \left[\frac{A_1E_{t2} + E_{t1}B_{21}\cos(\delta_{12} + \alpha_{12})}{A_1A_2 + B_{12}B_{21}\sin^2 \alpha_{12} - B_{12}B_{21}\cos^2 \delta_{12}}\right] \frac{\sin(\delta_{12} - \alpha_{12})}{Z_{12}}$$

The stability coefficient of the leading machine is:

$$S_1 = A_1A_2E_{t1}E_{t2}\cos(\delta_{12} - \alpha_{12}) + A_1E_{t2}^2B_{12}\cos 2(\delta_{12} - \alpha_{12}) + A_2E_{t1}^2B_{21}\cos 2\delta_{12} - 2A_2E_{t1}E_{t2}B_{12}\frac{Z_{12}}{Z_{11}}\sin \alpha_{11}\sin(\delta_{12} - \alpha_{12}) \quad (11)$$

The shaft power output of the lagging machine is:

$$P_2 = \left[\frac{A_1E_{t2} + E_{t1}B_{21}\cos(\delta_{12} + \alpha_{12})}{A_1A_2 + B_{12}B_{21}\sin^2 \alpha_{12} - B_{12}B_{21}\cos^2 \delta_{12}}\right]^2 \frac{\sin \alpha_{22}}{Z_{22}} - \left[\frac{A_2E_{t1} + E_{t2}B_{12}\cos(\delta_{12} - \alpha_{12})}{A_1A_2 + B_{12}B_{21}\sin^2 \alpha_{12} - B_{12}B_{21}\cos^2 \delta_{12}}\right] \times \left[\frac{A_1E_{t2} + E_{t1}B_{21}\cos(\delta_{12} + \alpha_{12})}{A_1A_2 + B_{12}B_{21}\sin^2 \alpha_{12} - B_{12}B_{21}\cos^2 \delta_{12}}\right] \frac{\sin(\delta_{12} + \alpha_{12})}{Z_{12}}$$

The stability coefficient of the lagging machine is:

$$S_2 = A_1A_2E_{t1}E_{t2}\cos(\delta_{12} + \alpha_{12}) + A_2E_{t1}^2B_{21}\cos 2(\delta_{12} + \alpha_{12}) + A_1E_{t2}^2B_{12}\cos 2\delta_{12} + 2A_1E_{t1}E_{t2}B_{21}\frac{Z_{12}}{Z_{22}}\sin \alpha_{22}\sin(\delta_{12} + \alpha_{12}) \quad (12)$$

Where: δ_{12} is the displacement angle between the axes of machines 1 and 2 in electrical degrees. Z_{11}, Z_{22}, and Z_{12} are the driving point, receiving point, and transfer impedances, respectively, and contain X_q of the machines. α_{11}, α_{22}, and α_{12} are their associated angles ($\alpha = 90 - \phi_z$). In most cases the α angles are small, and the output of a generator is positive; similarly, the output of a motor is negative.

When the machines have round rotors the A constants become unity, the B terms zero, and the machine $X_q = X_d$, $E_t = E_d$.

POWER-ANGLE EQUATIONS FOR TWO ROUND-ROTOR MACHINES

$$P_1 = E_{d1}^2\frac{\sin \alpha_{11}}{Z_{11}} + \frac{E_{d1}E_{d2}}{Z_{12}}\sin(\delta_{12} - \alpha_{12}) \qquad S_1 = \cos(\delta_{12} - \alpha_{12}) \quad (13)$$

$$P_2 = E_{d2}^2\frac{\sin \alpha_{22}}{Z_{22}} - \frac{E_{d1}E_{d2}}{Z_{12}}\sin(\delta_{12} + \alpha_{12}) \qquad S_2 = \cos(\delta_{12} + \alpha_{12}) \quad (14)$$

These equations are recognized as the familiar round-rotor power equations. They may be used to determine the power transfer between two round-rotor machines, or any two points of known voltage, E_{d1} and E_{d2}.

Appendix II

Derivation of Current Equations

The machine currents are given by equations 4 and 5. Similarly, the voltages are obtained from equation 7 or 8. The machine currents for the two-machine case are derived as follows:

The component currents of machine 1 are:

$$I_{q1} = \frac{E_{q1}}{Z_{11}}\sin \alpha_{11} + \frac{E_{q2}}{Z_{12}}\sin(\delta_{12} - \alpha_{12})$$
$$I_{d1} = \frac{E_{q1}}{Z_{11}}\cos \alpha_{11} - \frac{E_{q2}}{Z_{12}}\cos(\delta_{12} - \alpha_{12}) \quad (15)$$

and the component currents of machine 2 are:

$$I_{q2} = \frac{E_{q2}}{Z_{22}}\sin \alpha_{22} - \frac{E_{q1}}{Z_{12}}\sin(\delta_{12} + \alpha_{12})$$
$$I_{d2} = \frac{E_{q2}}{Z_{22}}\cos \alpha_{22} - \frac{E_{q1}}{Z_{12}}\cos(\delta_{12} + \alpha_{12}) \quad (16)$$

Substituting E_{q1} and E_{q2} from equation 10 in equations 15 and 16 gives the—

CURRENT-ANGLE EQUATIONS FOR TWO SALIENT-POLE MACHINES

The quadrature-axis component of current of the leading machine is:

$$I_{q1} = \left[\frac{A_2E_{t1}+E_{t2}B_{12}\cos(\delta_{12}-\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right] \times \frac{\sin \alpha_{11}}{Z_{11}} + \left[\frac{A_1E_{t2}+E_{t1}B_{21}\cos(\delta_{12}+\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right] \times \frac{\sin(\delta_{12} - \alpha_{12})}{Z_{12}}$$

The direct axis component of current of the leading machine is:

$$I_{d1} = \left[\frac{A_2E_{t1}+E_{t2}B_{12}\cos(\delta_{12}-\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right] \times \frac{\cos \alpha_{11}}{Z_{11}} - \left[\frac{A_1E_{t2}+E_{t1}B_{21}\cos(\delta_{12}+\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right] \times \frac{\cos(\delta_{12} - \alpha_{12})}{Z_{12}}$$

The total current is:

$$I_1 = \sqrt{I_{q1}^2 + I_{d1}^2} \quad (17)$$

at an angle $= \tan^{-1} I_{d1}/I_{q1}$ from the axis of machine 1.

The quadrature-axis component of current of the lagging machine is:

$$I_{q2} = \left[\frac{A_1E_{t2}+E_{t1}B_{21}\cos(\delta_{12}+\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right] \times \frac{\sin \alpha_{22}}{Z_{22}} - \left[\frac{A_2E_{t1}+E_{t2}B_{12}\cos(\delta_{12}-\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right] \times \frac{\sin(\delta_{12} + \alpha_{12})}{Z_{12}}$$

and

$$I_{d2} = \left[\frac{A_1E_{t2}+E_{t2}B_{12}\cos(\delta_{12}+\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right]\times\frac{\cos\alpha_{22}}{Z_{22}} - \left[\frac{A_2E_{t1}+E_{t1}B_{21}\cos(\delta_{12}-\alpha_{12})}{A_1A_2+B_{12}B_{21}\sin^2\alpha_{12}-B_{12}B_{21}\cos^2\delta_{12}}\right]\times\frac{\cos(\delta_{12}+\alpha_{12})}{Z_{12}}$$

$$I_2 = \sqrt{I_{q2}^2 + I_{d2}^2} \quad \text{at an angle} = \tan^{-1} I_{d2}/I_{q2} \qquad (18)$$

When the machines have round rotors the A constants = 1, the B constants = 0, the machine $X_q = X_d$, and $E_t = E_d$.

CURRENT-ANGLE EQUATIONS FOR TWO ROUND-ROTOR MACHINES

$$I_{q1} = \frac{E_{d1}\sin\alpha_{11}}{Z_{11}} + \frac{E_{d2}\sin(\delta_{12}-\alpha_{12})}{Z_{12}}$$

$$I_{d1} = \frac{E_{d1}\cos\alpha_{11}}{Z_{11}} - \frac{E_{d2}\cos(\delta_{12}-\alpha_{12})}{Z_{12}}$$

$$I_1 = \sqrt{I_{q1}^2 + I_{d1}^2} \quad \text{at an angle} = \tan^{-1} I_{d1}/I_{q1} \qquad (19)$$

and

$$I_{q2} = \frac{E_{d2}\sin\alpha_{22}}{Z_{22}} - \frac{E_{d1}\sin(\delta_{12}+\alpha_{12})}{Z_{12}}$$

$$I_{d2} = \frac{E_{d2}\cos\alpha_{22}}{Z_{22}} - \frac{E_{d1}\cos(\delta_{12}+\alpha_{12})}{Z_{12}}$$

$$I_2 = \sqrt{I_{q2}^2 + I_{d2}^2} \quad \text{at an angle} = \tan^{-1} I_{d2}/I_{q2} \qquad (20)$$

It should be noted that the component currents refer to the axes of the particular machine considered, see figure 1.

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Discussion

S. B. Crary (General Electric Company, Schenectady, N. Y.): The experimental checks which the author has obtained are very good and appear to substantiate his conclusion "that for practical purposes static stability may be regarded as a problem of stable equilibrium in which all transient phenomena and inertia effects may be neglected (reference 2)." However, as the author's experimental results necessarily only covered a very limited range, it is desirable to view any general conclusions drawn directly from such experiments quite critically. Mr. Dalziel's reference 2 presents a conservative method for determining the stability limit which has great practical value when applied with a knowledge of the conditions under which the results may be too conservative. Discussions of the limitations of the method may be found on pages 475, 476, 601, and 602, AIEE TRANSACTIONS, volume 53, 1934. An example of a case in which the inertia effects are important is a large generator delivering load to a large shunt load of constant impedance and a small synchronous motor. Actually the angular displacement between the motor and generator may approach 180 degrees with stable operation. Such a conclusion may be reached analytically when the relative inertia of the generator and motor are considered.

Reference is also made in the paper to the report of the AIEE subcommittee on stability (reference 3), to substantiate the contention that the inertia effects may be neglected. The subcommittee report must have been unintentionally misleading to the extent that it was possible for Mr. Dalziel to misinterpret it in this respect.

The following statement is made in the paper "For cases in which the system voltages are maintained constant, all shunt loads including synchronous condensers and induction motors may be accurately represented by simple shunt impedances. This treatment assumes that the induction portion of the system load remains stable. Since the maximum power transfer is ordinarily limited by the synchronous machine on the system, this assumption should not introduce errors in the result (reference 7)." That such a simplification cannot be made in many practical studies is well known and reference 7 does not show that this is possible. Reference 7 does show that a composite load may be replaced with good approximation by an equivalent load having a given real and reactive power and a given rate of change of real power and reactive power with voltage. These four characteristics are sufficient in order to obtain the effect of a composite load on the system. However, only when the rate of change of real and reactive power with respect to voltage are related to the total amount of real and reactive power in a definite manner can the composite load be replaced by a simple shunt impedance. This unfortunately is the exception rather than the rule. The case of fixed impedance load rather than the actual characteristics of a composite load will in general lead to optimistic or too high steady-state stability limits as the real power component of a composite load does not fall off or decrease as rapidly as that of a constant shunt impedance with drop in voltage.

A. J. Gilardi (Ohio Brass Company, Barberton): I should like to supplement Professor Dalziel's consideration of the Hetch Hetchy-Pacific Gas and Electric system by a concrete example which incidentally includes typical stability curves of the power system.

For accurate analysis equations 11 and 12 of the paper should be used to calculate the steady-state power limits. The steady-state power curve and margin of stability so determined accurately considers saliency and saturation. However, since the transmission line under consideration has considerable series impedance it will be sufficiently accurate to compute the steady-state power curve using $X_{q\,sat}$ in equations 13 and 14. In figures 3 and 4 of the paper it is shown that the error due to neglecting saliency decreases with increased system impedance. The simplification obtained by neglecting saliency greatly reduces the labor of computation and the resulting errors are small. The steady-state power limits and stability coefficients computed using 0.90 $X_{q\,unsat}$ in determining S_1 and S_2 from equations 13 and 14 gives results which are approximately correct where accuracy is not essential (i.e., at the large margins of stability) and accurate results at the power limit where good accuracy is desired.

The accompanying figure 1 illustrates the power system under consideration, except that only one circuit of the transmission line was used in the computations. The receiving-end bus feeds a shunt load

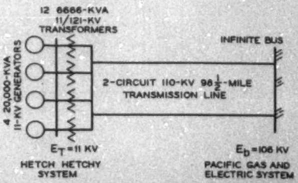

Figure 1. Hetch Hetchy-Pacific Gas and Electric interconnected system

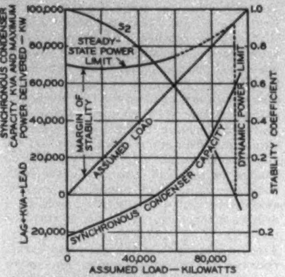

Figure 2. Steady-state and dynamic power limits of Hetch Hetchy system

and the synchronous condenser serves to maintain constant voltages on the system. The following equations were used to determine the stability limits utilizing the simplifications of the preceding paragraph:

$$P_1 = \frac{e_{g1}^2}{Z_{11}} \sin \alpha_{11} + \frac{e_{g1}E_b}{Z_{12}} \sin (\delta_{12} - \alpha_{12}) \quad (1)$$

$$S_1 = \cos(\delta_{12} - \alpha_{12}) \quad (2)$$

$$P_2 = \frac{E_b^2 \sin \alpha_{22}}{Z_{22}} - \frac{e_{g1}E_b}{Z_{12}} \sin (\delta_{12} + \alpha_{12}) \quad (3)$$

$$S_2 = \cos (\delta_{12} + \alpha_{12}) \quad (4)$$

Since α_{12} is positive the power limits are determined by P_2 and S_2. The results of computations for various loads using the foregoing equations are shown in figure 2. These results were obtained using actual system constants with the exception of the generator reactances. The curve for S_1 is not shown since S_2 reaches zero first. The synchronous-condenser kilovolt-ampere curve indicates the amount of capacity required to maintain constant system voltage at any given load. It may be seen that the dynamic power limit is approximately 92,500 kw with one line in service. As mentioned in the paper one line has delivered approximately 77,000 kw during emergency switching and trip-outs.

Figure 3 compares the margin of stability of figure 2 with that obtained for the same system using the unsaturated cylindrical-rotor theory. It is evident that the latter approach gives only an approximate solution which is less accurate than by using the theory outlined in the paper.

Charles F. Dalziel: The author wishes to thank S. B. Crary for the constructive comments drawn from his rich experience in this field. The final draft of the paper has been changed in an endeavor to meet his objections regarding the representation of loads. It was originally intended to check the method and the simplifying assumptions under question by comparing theoretical results with the operating records of several large power systems. The analyses of the practical problems presented in the paper are consistent and in good agreement with available operating records. However, due to limited data, the results fall somewhat short of the original objective. The writer would appreciate receiving system data for cases where the stability limits are fairly accurately known, and which could be reduced to a two-generator system for analysis.

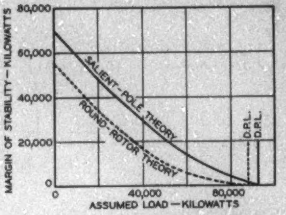

Figure 3. Comparison of results obtained by use of round-rotor or salient-pole theory

Mr. Gilardi's detailed analysis of the Hetch Hetchy system should be of interest to those applying the method to actual systems. It should be repeated that most of the generator impedances in the practical problems were unknown, and typical average values were used. These were multiplied by 0.90 to give an approximate correction for saturation. This may explain the small effect of saliency and saturation on Mr. Gilardi's power limits in contrast to the experimental differences of figure 8.

Applications of Copper-Oxide Rectifiers

By E. W. MORRIS
MEMBER AIEE

Synopsis: Since the development of the copper-oxide rectifier in 1927, no paper has presented to the Institute a comprehensive list of the many problems to which the device may be applied successfully. It is the purpose of this paper to describe a few of the representative installations made in the various industries.

WHEN the copper-oxide rectifier was announced,[1] limited information was available regarding such characteristics as aging, the effect of ambient temperatures, and the safe values of current and voltage per disk. Applications made since 1927 have removed many of these uncertainties. Continued research has produced valuable information about the characteristics of the device, and has improved the method of manufacture and application to industry.[2,3] It has become indispensable in the design of control and electronic equipment,[4] and is well adapted to applications requiring high efficiency at low voltage and to those requiring high overload capacities for short periods of time.

The copper-oxide rectifier consists essentially of a copper disk, one side of which is covered with cuprous oxide formed by heating to a high temperature. This rectifier disk then has the characteristics of asymmetric resistance, allowing electrons to pass more readily from copper to oxide than in the reverse direction, as indicated in figure 1. The rating of a disk is based upon ability to dissipate the losses, and rectifier disks usually are assembled into units with cooling fins and spacers to assist in heat dissipation. Soft metal washers insure intimate contact with the oxide.

These assembled units may be connected into groups for half- and full-wave rectification, for any number of phases, and in parallel or series for increased current or voltage ratings.

As valves in d-c control circuits they are convenient in isolation of circuits, preventing current flow in reverse directions to a common bus.

Applications of copper-oxide disks made during the past ten years can be classified as rectifiers for battery loads, rectifiers for resistance loads, and valves in control circuits.

I. Rectifiers for Battery Loads

After the development of the copper-oxide rectifier, one of the most immediate applications was that of small battery chargers. Sizes of chargers have gradually increased until now they are available in ratings up to 6 amperes at 130 volts. A duplex charger having a rating of 15 amperes at 30 volts and 0.5 ampere at 24 volts is shown in figure 2. Simplicity results in an assembly consisting of an insulating transformer, the copper-oxide rectifying units, some type of overload protection, and a dial switch to care for current adjustment and aging.

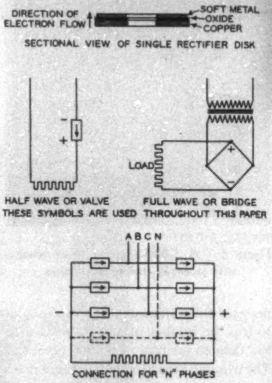
DIRECTION OF ELECTRON FLOW
SOFT METAL
OXIDE
COPPER
SECTIONAL VIEW OF SINGLE RECTIFIER DISK
+
−
LOAD
HALF WAVE OR VALVE
FULL WAVE OR BRIDGE
THESE SYMBOLS ARE USED THROUGHOUT THIS PAPER
A B C N
CONNECTION FOR "N" PHASES

Figure 1

The nearest approach to an ideal battery charger has been obtained in the recent development of a self-regulating charger, the diagram of which is shown in figure 3. It is particularly adaptable for floating battery service, and automatically varies the charging rate proportionate with the demands of the battery by means of saturable reactors. The main rectifier, I, furnishes charging current to the battery. Between the transformer secondary and the main rectifier is located a saturable reactor, *A*. The saturation of reactor *A* is controlled partly by a series coil which is energized by the charging current, partly by a shunt coil, but mainly by rectifier II. This rectifier is supplied from the primary side of the transformer through a second saturable reactor, *B*. Rectifier III is furnished with a constant a-c voltage by means of a small regulator connected across the primary of the power transformer. The constant d-c output voltage of rectifier III is opposed by the battery voltage so that any difference between the two will result in a flow of current through the d-c winding of reactor *B;* hence the saturation of reactor *B* will vary according to the battery voltage. Reduction in the cell

Figure 2. Station-type rectifier for charging telephone batteries

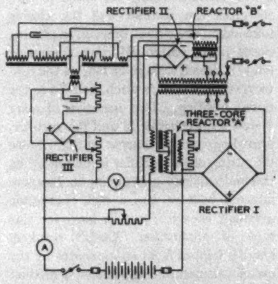

Figure 3. Automatic self-regulating battery charger

Paper number 38-105, recommended by the AIEE committee on general power applications, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 16, 1938; made available for preprinting July 12, 1938.

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[1] For all numbered references, see list at end of paper.

voltage below the average for which the charger is set to regulate will cause the charger to turn on and deliver full charging current to the battery. As the cell voltage increases to a point above the average voltage, the charging current will be reduced to its minimum value. Such a charger is capable of maintaining the cell voltage within limits of plus or minus one per cent for all load conditions and for variations in the a-c line voltage of not more than plus or minus five per cent.

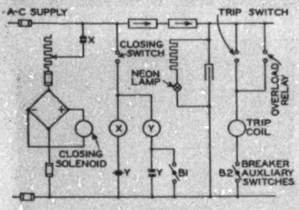


Figure 4. Circuit breaker closing and tripping from an a-c supply

II. Rectifiers for Resistance Loads

In the field of industrial control, versatility has been added by the use of the copper-oxide rectifier. The advantages of d-c operation of contactors and relays can be obtained from an a-c source and the rectifying units. D-c fields can be excited from an a-c supply and regulated by means of the proper arrangement of rectifiers and reactors.

RECTIFIERS FOR CIRCUIT-BREAKER OPERATION

Solenoid type of circuit-breaker mechanisms may be operated from an a-c source by means of rectifiers connected as in figure 4. Operation of the closing switch energizes relay *X*, which in turn energizes the rectifier and the closing solenoid. The circuit breaker, in closing, closes pallet switch *B*1, thus energizing relay *Y*, which seals in as long as the contacts of the closing switch are held in the closed position. The operation of relay *Y* de-energizes relay *X* and the rectifier, providing an automatic cutoff. Since the rating of the rectifier is limited by its thermal capacity and ability to dissipate internal heat, it has a high short-time overload capacity, and is ideal for this type of application. Circuit breakers may be tripped from an a-c source by the use of rectifiers and capacitors, a distinct advantage in the elimination of battery maintenance at isolated or unattended substations. Capacitor tripping has the advantage of requiring current transformers only of sufficient size to energize the fault-detector relays. The tripping energy is stored in capacitors from the a-c supply voltage during normal conditions, and is available to trip the breaker for at least six seconds after charging potential is entirely removed. In figure 4, when the overload relay or manual tripping contacts close, the capacitor discharges through the trip coil. The device is so designed that sufficient charge is available to trip the breaker at any time with not less than 65 per cent of normal potential. The tripping capacitor will recharge in 0.2 second. Figure 5 shows, in the lower left-hand corner, the small size of rectifier required to close a 5,000-ampere three-pole air circuit breaker.

Figure 5. A 5,000-ampere circuit breaker with solenoid closing mechanism

Figure 6. Voltage regulator for a-c generator

VOLTAGE REGULATORS FOR A-C GENERATORS

It has long been recognized that a-c generators inherently have a poor regulation, lacking the advantage of a series field for maintaining a nearly constant voltage under varying loads. The device shown in figure 6 was developed to meet the demand for a simple voltage regulator, with no moving parts, for small-size a-c generators. It consists of copper-oxide rectifier units assembled with the proper transformers, reactors, and resistors to provide an increase in the exciter shunt-field current proportional to the increase in voltage required under varying loads on the generator. As indicated in figure 7, changes in generator load are reflected in two full-wave rectifier units. Rectifier I is energized by a current transformer, and is adjusted to produce a compounding effect to some voltage less than normal at full load. Rectifier II, energized from the generator potential, supplies the additional rectified field current necessary for maintaining normal generated voltage. A small change from normal potential on the circuits of rectifiers III and IV causes a relatively large change in the output of the three-core reactor in series with rectifier II.

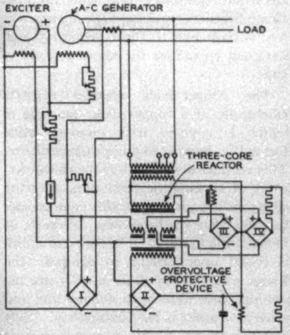


Figure 7. Voltageregulator

Under normal conditions of operation, this type of regulator will maintain the voltage within plus or minus three per cent from no load to full load.

Another simple form of a-c regulator is shown in figure 8, this requiring special design of the generator and exciter fields

RECTIFIERS FOR THE PREVENTION OF ELECTROLYSIS

Corrosion of underground structures can be mitigated by the use of cathodic protection, and is conveniently accomplished by connecting the underground structure to the negative terminal of a d-c source and by maintaining it negative to the earth by 0.3 volt or greater. Corrosion occurs in underground structures due to stray currents from d-c systems, and from electrochemical reactions between the structure and the surrounding soil. Small currents flow from the underground structure through the soil to other parts of the structure, corrosion occurring where the currents leave the structure. Figure 9 shows an outdoor installation of a rectifier unit rated 30 amperes at ten volts, used to protect a section of a pipe line from corrosion. The use of the copper-oxide rectifier for this d-c supply is preferred to other d-c sources, because of its efficiency at low voltage and the fact that a minimum of maintenance is required, such as an occasional check of the output to adjust for seasonal variations in the soil. The device is noiseless and free from radio interference, factors which are important when the device must be located in residential districts. The schematic diagram in figure 10 indicates how this type of device is connected to protect a section of pipe against corrosion.

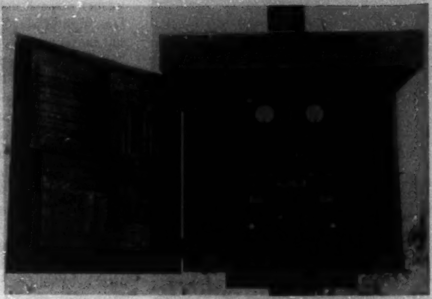


Figure 9. Installation of rectifier for protection against electrolysis on pipe line

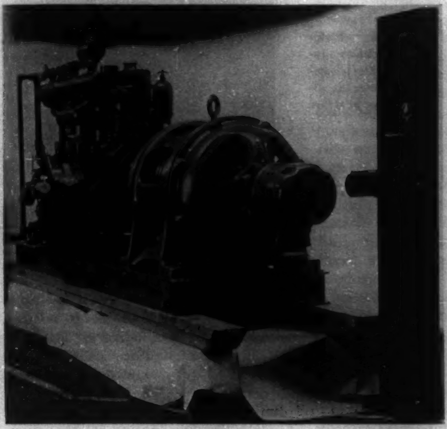


Figure 8. The rectifiers on this switchboard provide voltage regulation for the large engine-driven a-c generator

III. Valves in Control Circuits

The asymmetric resistance characteristics of this rectifier makes it particularly suitable as a valve to control the path of flow of direct current. This type of application has increased the versatility of control circuits and has reduced the number of mechanical relays necessary to complete many control functions. This is best illustrated by a few of the examples given in figure 11.

Remote control of many devices may be accomplished in a minimum time by the system of polarized lines, shown for two wires in figure 11*A*. Relay *A* or *B* will operate depending on the relative polarities of the bus to which they are connected. This simple two-conductor system may be arranged to operate over one conductor and a good ground return such as a water pipe, which is sometimes convenient in the remote control of pumping plants. By the use of three or more wires the number of relays that can be operated increases[5] because of the many possible combinations of polarized lines.

The rectifier valve has become quite useful in isolating the flow of direct current within parts of a circuit, as shown in figure 11*B*. This shows a system of relays and rectifier valves, each of which may be energized either from a common test bus or through contacts on alarm devices. Each alarm contact allows but one relay to operate, the valve in that circuit preventing current flow to the test bus and to other relays. The circuit, and all signal lamps, may be checked conveniently by depressing the test button, wherein all of the relays are energized simultaneously. Figure 12 illustrates a group of rectifier valves and relays used

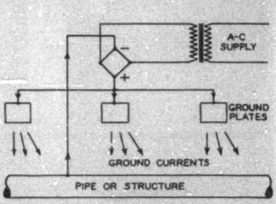
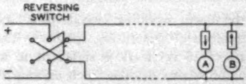
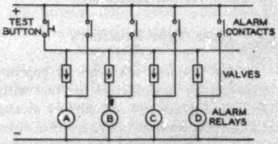


Figure 10. Cathodic protection against electrolysis



(a) – SELECTIVE OPERATION OF RELAYS OVER POLARIZED LINES



(b) – VALVES FOR ISOLATION OF ALARM CIRCUITS

Figure 11

Figure 12. Valve-type rectifiers installed in annunciator circuits (cover removed from one group only)

Figure 13. Plate-type rectifiers installed in film laboratory for stripping silver from film

in the alarm signaling circuits of a large hydroelectric generating plant. This group of 16 rectifiers is sufficient to include all alarm devices, such as may be actuated by bearing, coil, and core temperatures, for one generator.

IV. Plate-Type Rectifiers

During the first ten years of copper-oxide-rectifier manufacture, contact with the oxide surface was obtained by means of soft metal washers. The rectifier disks were assembled in the proper direction on bolts with the necessary cooling fins and spacers. A special cupped spring washer on each bolt was tightened to obtain high contact pressure between the soft metal washers and the oxide surfaces. The size of disks was limited to a diameter of $1^{1}/_{2}$ inches by the ability to maintain uniform pressure over the entire surface.

Continued research during that time developed a satisfactory method of spraying a conducting coating on large copper-oxide plates.[6] The necessity for high contact pressure having been removed, the new rectifier plates with sprayed coating are assembled a fraction of an inch apart and the losses are dissipated by means of forced air. Electrical connections between plates are made with clips. Four sizes of plates, 10 inches and 12 inches in length, and 3 inches and $4^{3}/_{4}$ inches in width, have been used in group assemblies. The rating in output of a three- by ten-inch plate assembled in a properly ventilated, three-phase rectifier, is 20 watts at six volts d-c maximum. A complete three-phase full-wave rectifier which would require six plates, would then have an output rating of 120 watts at six volts, or 20 amperes, six volts d-c maximum. In the larger plates current capacity increases in proportion to the area, but the voltage rating remains the same. The plates can of course be connected in series or parallel, to any desired capacity. The development of the plate-type copper-oxide rectifier will not supplant, but will supplement, the standard design disk assemblies, and will increase the number of applications in the high-current low-voltage field.

The major field for such units, is in the electrochemical industry, including, for example, chemical processing, electrolytic cleaning, and electroplating. Figure 13 shows an installation of two plate-type rectifiers each rated four volts, 200 amperes d-c output at 220 volts, three-phase, 60 cycles, a-c input. These are installed in a laboratory for use in stripping silver from salvaged film. Figure 14 shows a large-capacity plate-type rectifier. This rectifier has an output rating of 1,500 amperes, six volts maximum for 220 volts, three-phase, 60 cycles, a-c input.

Conclusions

No attempt has been made herein to list all possible applications, but to describe a few representative types and to show installation photographs of rectifiers in new kinds of services. Units have been assembled with ratings which vary from 0.001 watt to several kilowatts in capacity. This is a device which finds applications in any field, whether it be transportation, power, or industry. The future should see increased application for simplifying control and metering problems.

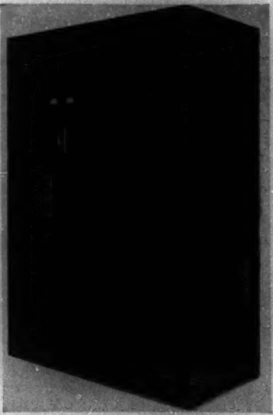


Figure 14. Plate-type rectifier, 1,500 amperes, 6 volts d-c output, built for electroplating service

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The Electrostatic Unbalance of Transmission Lines and Its Effect on the Application of Petersen Coils

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RECENTLY three Petersen coils were installed on the 66-kv transmission system of the Metropolitan Edison Company of Pennsylvania. These Petersen coils provide service protection to approximately 320 miles of overhead transmission lines against outages and voltage dips which would otherwise result when single phase-to-ground flashovers occur on the system. Much of the 66-kv transmission system consists of two-circuit lines with vertical configurations of the line conductors with no transpositions prior to the Petersen-coil installation.

During the tuning tests on these Petersen coils which were made before putting them in service, it was found that excessive currents were flowing through the coils when tuned with the system, even though there was no ground fault on the system. These currents far exceeded the continuous current ratings of the coils, and they were of such magnitude that the system neutral in some cases shifted as much as 70 per cent above ground. The shift in the neutral above ground, if allowed to remain on the system, would have needlessly subjected the line insulation and connected apparatus to dynamic overvoltages. Accordingly, an investigation was made to determine the cause of this excessive current.

Measurements obtained of the line-to-ground voltages with the system neutral isolated revealed an abnormally unbalanced capacity to ground. That is, the capacities to ground of the three phases were unequal, causing unequal voltages and, therefore, with the Petersen coils in the neutral, excessive current flowed. The unbalanced capacity was accounted for by the lack of transpositions in the transmission lines. It became apparent that by rearranging some of the circuits, the direct capacities of the three phases to ground could be balanced and the circulating current passing through the Petersen coils would be reduced to a small fraction of its original value.

This paper deals with the analysis of zero-sequence electrostatic unbalance of transmission lines to neutral, illustrates the methods of calculation, applies these calculations to obtain a proper balance in the most economical manner, and presents experimental data which verify the methods and procedure used for the solution of the problem. The subject of electrostatics is considered not in its entirety, but only to the extent necessary for the application of impedors (also grounding transformers and neutral reactors). It is hoped that the information embodied in this paper may be of use to others contemplating the use of Petersen coils, neutral reactors, or grounding transformers. The material which follows should not be construed as an attempt to add to the subject of electrostatics; it is instead the application of well-known fundamentals to a very practical situation. For this reason there is at the conclusion of the text a list of general references which contain all of the necessary theory and methods which are used herein.

Briefly, a Petersen coil is an iron-core variable inductance which, when system leg voltage is applied across it, passes a current the magnitude of which is equal to the charging current in a line-to-ground fault on the system when the system neutral is isolated. During the occurrence of a single line-to-ground fault, the Petersen coil causes the flow of a reactive current which is equal to and 180 degrees out of phase with the capacitive current which flows in the ground fault. The result is that the current in the arc is reduced to very nearly zero, the arc is extinguished, and the temporary ground fault disappears without having become a permanent ground fault or having developed into a line-to-line fault as is so often the case. The coil operation does not compensate for the small amount of in-phase or loss current which is usually present in the arc. In practice the current from these two sources is small and not sufficient to maintain the arc. Owing to the fact that a perfect electrostatic balance cannot be obtained practically, it is to be expected that some current will flow through the coil with system normal. This has been the case in previous installations. This flow of current does not interfere with the functioning of the coil in arc extinction in any practical case; the main objection to it, if it exists to an abnormal degree, is that of neutral displacement.

Figure 1 shows the schematic diagram of an impedor applied to a transmission line. The capacitance values of the line are shown as lumped constants in this diagram, and zero-sequence capacitance will be considered in this manner throughout the discussion. This can be done with impunity because in the application of impedors one is concerned primarily with the parallel circuit constants of the lines; the series reactance and resistance of the transmission lines are neglected in the analysis which follows. In the case of an actual transmission system, there will be a coil placed in the neutral of every ground bank, or a coil may serve a group of two or more ground banks. In some instances it may be desirable to separate a ground bank from ground completely and thereby save the cost of an additional coil. Of course, a Petersen-coil-protected system must be electrically insulated by transformers from every other system which is either grounded or ungrounded.

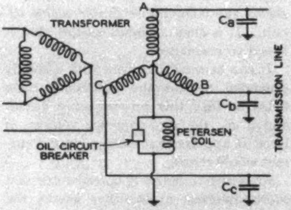

Figure 1. A Petersen coil applied to a transmission line

On the Metropolitan Edison Company system a coil was used at each of the three grounding banks which were originally chosen for number and positions according to relaying and switching requirements. It is desirable, when a system is operating in sections, that each section should be able to operate alone

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Paper number 38-119, recommended by the AIEE committees on power transmission and distribution, and protective devices and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted March 2, 1938; made available for preprinting July 7, 1938.

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with Petersen-coil protection. The analysis loses none of its generality through the consideration of only one coil, and the reader is referred to the general theory on coil operation found in the list of references on that subject.

Now the installation of a coil upon a transmission system requires that the coil be tuned for the transmission lines which it is to protect; the coil must be tuned and set at a different tap when additional miles of line are added to the number which it must protect, or when the number of miles which it protects is reduced, such as in switching operations. This tuning process, varying the inductance by means of taps, corresponds to obtaining the resonance point for a simple series circuit. The tuning is accomplished while the transmission lines are operating under normal conditions without the presence of a ground fault. This is one of the several possible methods of tuning. For a given length of line, the tuned point is found when the tap setting, which corresponds to the maximum residual ground current, is obtained.

Figure 2 illustrates the electrical circuit. One tunes, of course, the zero-sequence circuit of figure 2. Figure 3 shows the comparable simple series circuit. It is such a circuit as this that is tuned to resonance.

It can be seen that even with a solidly grounded system the grounding bank helps to tune this zero-sequence circuit and if E_0, the zero-sequence voltage, is large, it appears that a large ground current would result.

Referring to figure 3, consider the coil short circuited, or in other words, the system is to be solidly grounded

Let

I_0 = zero-sequence component of phase current

I_n = current through neutral of ground bank

X_t = zero-sequence reactance of transformer

$X_{\text{cap.}}$ = average line-to-neutral zero-sequence capacitive reactance

R_g = equivalent ground resistance for entire circuit

X_{cl} = zero-sequence reactance of coil

Then:

$$I_n = 3I_0 = 3\frac{E_0}{3R_g + jX_t - jX_{\text{cap}}}.$$

Even when solidly grounded, the ground banks of the Metropolitan Edison Company had heavy ground currents. The reason for this was a large E_0 which was caused by the electrostatic or capacitive unbalance of the three lines to ground.

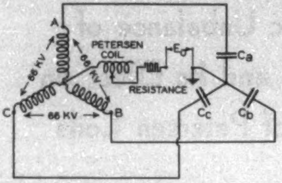

Figure 2. The three-phase circuit of a Petersen-coil installation

Now when the impedor is in the neutral of the ground bank, the current becomes:

$$I_n = 3I_0 = 3\frac{E_0}{3R_g + jX_t + jX_{cl} - jX_{\text{cap.}}}$$

The coil is in tune for the transmission system when X_{cl} is of such value that

$$X_t + X_{cl} = X_{\text{cap.}}$$

and this leaves the resistance R_g as the only factor limiting the current for a given voltage.

In tuning on the Metropolitan Edison system, the residual ground current increased on the resonance peak to as much as five times the continuous current rating of the Petersen coil. Obviously, the coils could not be put in service under such conditions.

In the haste to place the coils into operation during the lightning season, a futile attempt was made to achieve electrostatic balance by interchanging the top and bottom phases of a section of vertically spaced line. It is shown in the analysis to follow that for vertical construction, the top and bottom wires have higher capacitances than the middle wire which was left unchanged. In all of the work the effect of grounded crossarms and poles was neglected.

Vertical construction is used predominately on the lines of the Metropolitan Edison Company, although it has been found that all of the other types of construction, double and single lines, resulted in capacitive unbalance which was appreciable. The system under consideration was almost completely untransposed and this led, of course, to the resultant state of unbalance.

There follows the analysis applied to a typical transmission line as shown in figure 4.

Let

r = radius of conductors in inches

Q_a, Q_b, etc. = charge on conductors A, B, etc., in statcoulombs per centimeter

E_a, E_b, etc. = voltage above ground of conductors A, B, etc.

C_a, C_b, etc. = capacity of respective conductors to ground

Then using the method of images (Attwood, "Electric and Magnetic Fields," page 113) one obtains the following:

$$E_a = 2\left(Q_a \log_e \frac{2ag}{r} + Q_b \log_e \frac{ab'}{ab} + Q_c \log_e \frac{ca'}{ca}\right) \quad (1)$$

$$E_b = 2\left(Q_a \log_e \frac{ab'}{ab} + Q_b \log_e \frac{2bg}{r} + Q_c \log_e \frac{ca'}{ca}\right) \quad (2)$$

$$E_c = 2\left(Q_a \log_e \frac{ca'}{ca} + Q_b \log_e \frac{bc'}{bc} + Q_c \log_e \frac{2cg}{r}\right) \quad (3)$$

Similar equations may be written for any number of conductors. Now in general:

$$C\ (\text{Capacity}) = \frac{Q\ (\text{Charge})}{E\ (\text{Voltage})} \quad (4)$$

The direct or zero-sequence capacitance of a conductor A to ground is the ratio of the charge on conductor A to its potential (the zero-sequence potential) when all other conductors except the reference conductor, in this case the ground, are at the potential (zero-sequence potential) of A. Solving equations 1 to 3 would give equations for Q_a, Q_b, and Q_c in terms of V_a, V_b, and V_c. The three wires are allowed to assume the same potential with respect to ground; that is, $V_a = V_b = V_c$. It is thus possible to solve by the method of determinants equations 1, 2, and 3 for Q_a, and by dividing through by V_a, according to equation 4, the coefficients V_a, V_b, and V_c are removed from the expression which now represents the capacitance, C_a.

$$C_a = \frac{\begin{vmatrix} 1 & 2\log_e \frac{ab'}{ab} & 2\log_e \frac{ca'}{ca} \\ 1 & 2\log_e \frac{2bg}{r} & 2\log_e \frac{bc'}{bc} \\ 1 & 2\log_e \frac{bc'}{bc} & 2\log_e \frac{2cg}{r} \end{vmatrix}}{\begin{vmatrix} 2\log_e \frac{2ag}{r} & 2\log_e \frac{ab'}{ab} & 2\log_e \frac{ca'}{ca} \\ 2\log_e \frac{ab'}{ab} & 2\log_e \frac{2bg}{r} & 2\log_e \frac{bc'}{bc} \\ 2\log_e \frac{ca'}{ca} & 2\log_e \frac{bc'}{bc} & 2\log_e \frac{2cg}{r} \end{vmatrix}} \text{ statfarads per centimeter} \quad (5)$$

Expressions can likewise be found for C_b and C_c.

It is more convenient for computation to use logarithms to the base 10, and to obtain capacitance in microfarads per mile; thus equations 6, 7, and 8 arise.

$$C_a = 0.0389 \times \frac{\begin{vmatrix} 1 & \log\frac{ab'}{ab} & \log\frac{ca'}{ca} \\ 1 & \log\frac{2bg}{r} & \log\frac{bc'}{bc} \\ 1 & \log\frac{bc'}{bc} & \log\frac{2cg}{r} \end{vmatrix}}{\begin{vmatrix} \log\frac{2ag}{r} & \log\frac{ab'}{ab} & \log\frac{ca'}{ca} \\ \log\frac{ab'}{ab} & \log\frac{2bg}{r} & \log\frac{bc'}{bc} \\ \log\frac{ca'}{ca} & \log\frac{bc'}{bc} & \log\frac{2cg}{r} \end{vmatrix}}$$

microfarads per mile (6)

The denominators of 6, 7, and 8 are the same and can be denoted in each case by the symbol D.

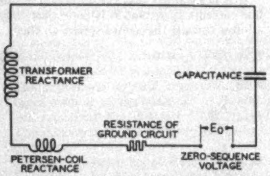

Figure 3. The equivalent zero-sequence resonant circuit

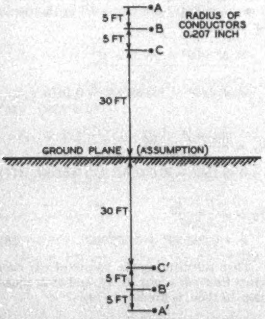

Figure 4. The transmission line spacings used in the calculations

$$C_b = 0.0389 \times \frac{\begin{vmatrix} \log\frac{2ag}{r} & 1 & \log\frac{ca'}{ca} \\ \log\frac{ab'}{ab} & 1 & \log\frac{bc'}{bc} \\ \log\frac{ca'}{ca} & 1 & \log\frac{2cg}{r} \end{vmatrix}}{D}$$

microfarads per mile (7)

$$C_c = 0.0389 \times \frac{\begin{vmatrix} \log\frac{2ag}{r} & \log\frac{ab'}{ab} & 1 \\ \log\frac{ab'}{ab} & \log\frac{2bg}{r} & 1 \\ \log\frac{ca'}{ca} & \log\frac{bc'}{bc} & 1 \end{vmatrix}}{D}$$

microfarads per mile (8)

Solving for the capacitances of the line of figure 4:

$$\log\frac{2ag}{r} = \log\frac{12 \times 2 \times 40}{0.207} = 3.666$$

$$\log\frac{2bg}{r} = \log\frac{12 \times 2 \times 35}{0.207} = 3.609$$

$$\log\frac{2cg}{r} = \log\frac{12 \times 2 \times 30}{0.207} = 3.541$$

$$\log\frac{ab'}{ab} = \log\frac{75}{5} = 1.176$$

$$\log\frac{ca'}{ca} = \log\frac{70}{10} = 0.846$$

$$\log\frac{bc'}{bc} = \log\frac{65}{5} = 1.114$$

$$D = \begin{vmatrix} 3.666 & 1.176 & 0.846 \\ 1.176 & 3.609 & 1.114 \\ 0.846 & 1.114 & 3.541 \end{vmatrix} = 37.08$$

$$(C_a \times D) = 0.0389 \times \begin{vmatrix} 1 & 1.176 & 0.846 \\ 1 & 3.609 & 1.114 \\ 1 & 1.114 & 3.541 \end{vmatrix}$$
$$= 0.0389 \times 6.523$$

$$C_a = \frac{(C_a \times D)}{D} = 0.0389 \times \frac{6.523}{37.08}$$
$$= 0.00685 \text{ microfarad per mile}$$

$$(C_b \times D) = 0.0389 \times \begin{vmatrix} 3.666 & 1 & 0.846 \\ 1.176 & 1 & 1.114 \\ 0.846 & 1 & 3.541 \end{vmatrix}$$
$$= 0.0389 \times 5.962$$

$$C_b = \frac{(C_b \times D)}{D} = \frac{0.0389 \times 5.962}{37.08}$$
$$= 0.00625 \text{ microfarad per mile}$$

$$(C_c \times D) = 0.0389 \times \begin{vmatrix} 3.666 & 1.176 & 1 \\ 1.176 & 3.609 & 1 \\ 0.846 & 1.114 & 1 \end{vmatrix}$$
$$= 0.0389 \times 7.028$$

$$C_c = \frac{(C_c \times D)}{D} = \frac{0.0389 \times 7.028}{37.08}$$
$$= 0.00737 \text{ microfarad per mile}$$

It is the problem now to show that such an electrostatic unbalance to neutral as indicated by the zero-sequence capacitance values above can produce a large E_0, or zero-sequence voltage, which is responsible for the large residual currents. In this computation, it makes no difference whether one considers one mile or 100 miles of line having the per mile capacitance values which have just been found.

Therefore:

$$X_a = \frac{-j}{2\pi f C_a} = \frac{-j}{377 \times 0.00685 \times 10^{-6}}$$
$$= -j387{,}000 \text{ ohms}$$

Note: The letter f designates frequency in the above expression

$$X_b = \frac{-j}{2\pi f C_b} = \frac{-j}{377 \times 0.00625 \times 10^{-6}}$$
$$= -j424{,}000 \text{ ohms}$$

$$X_c = \frac{-j}{2\pi f C_c} = \frac{-j}{377 \times 0.00737 \times 10^{-6}}$$
$$= -j360{,}000 \text{ ohms}$$

Now these zero-sequence capacitive reactances are considered as forming an unsymmetrical wye load with balanced line-to-line voltages of 66,000 volts. The method of symmetrical components with the usual notation is used.

$$X_0 = 1/3\,(X_a + X_b + X_c)$$
$$= \frac{-j}{3}\,(387{,}000 + 424{,}000 + 360{,}000) \text{ ohms}$$
$$= -j390{,}000 \text{ ohms} = -j390 \times 1{,}000 \text{ ohms}$$

$$X_1 = 1/3\,(X_a + aX_b + a^2X_c)$$
$$= 1/3\{-j387{,}000 - j424{,}000\,(-0.5 + j0.866) - j360{,}000\,(-0.5 - j0.866)\} \text{ ohms}$$
$$= (18.3 + j2) \times 1{,}000 \text{ ohms}$$

$$X_2 = 1/3\,(X_a + a^2X_b + aX_c)$$
$$= 1/3\{(-j387{,}000 - j424{,}000\,(-0.5 + j0.866) - j360{,}000\,(-0.5 - j0.866)\} \text{ ohms}$$
$$= (-18.3 + j2) \times 1{,}000 \text{ ohms}$$

Now

$$E_0 = I_1X_2 + I_2X_1 \qquad (9)$$

$$E_1 = I_1X_0 + I_2X_2 \qquad (10)$$

$$E_2 = I_2X_0 + I_1X_1 \qquad (11)$$

$$E_1 = \frac{66{,}000}{\sqrt{3}} = 38{,}100 \text{ volts (phase voltage effective value)}$$

$E_2 = 0$ (not generated)

Using equation 10 there results:

$$38{,}100 = I_1\,(-j390{,}000) + I_2\,(-18.3 + j2) \times 1000$$

or

$$38.1 = I_1\,(-j390) + I_2\,(-18.3 + j2) \qquad (12)$$

Using equation 11

$$0 = I_2(-j390{,}000) + I_1(18.3 + j2) \times 1{,}000$$
$$0 = I_2\,(-j390) + I_1\,(18.3 + j2) \qquad (13)$$

Remembering that I_1 and I_2 are vectors, one can obtain four linear simultaneous algebraic equations from (12) and (13), and these can be solved for I_1 and I_2 which are found to be:

$$I_1 = 0.000735 + j0.0979$$
$$I_2 = 0.00459 - j0.000735$$

Substituting these values in (9), it follows:

$$V_0 = (0.000735 + j0.0979)(-18.3 + j2) \times 1000 + (0.00459 - j0.000502) \times (18.3 + j2) \times 1000$$
$$V_0 = -125.25 - j1799.1$$

Absolute value of V_0 = 1,800 volts (effective value) (14)

In an actual test at the West Reading plant of the Metropolitan Edison Company it was found that 57 miles of line having the dimensions and construction which have just been considered operated with a zero-sequence voltage or neutral displacement voltage of 1,920 volts. This checks very closely with the value of 1,800 volts from the computations; it should be obvious that any discrepancy which is present can be attributed to the sag in the lines and other immeasurable irregularities in the lines. Furthermore, the computations have involved the assumption of the plane of zero potential or ground plane.

Transpositions were made at the regular switching stations or accessible H-frame structures of the above system for the 57 miles considered and as a result the displacement voltage was reduced to 144 volts. With this voltage even under conditions of resonance the Petersen coils could operate safely within their continuous ratings.

It should be pointed out that zero-sequence electrostatic balance to neutral can be and was obtained in actual practice, both by completely transposing as well as by interchanging two of the three line wires.

Refer to figure 4.

C_a = 0.00685 microfarad per mile
C_b = 0.00625 microfarad per mile
C_c = 0.00737 microfarad per mile

The arithmetical mean of C_b and C_c is 0.00681 microfarad per mile. Thus it can be seen that if A phase is always on top, B phase is half of the distance in the middle and the other half of this distance on the bottom, and C phase is half of the distance on the bottom and the other half of the distance in the middle position, an almost perfect balance of capacities to neutral is obtained.

Economic considerations were important in the revision of the existing transmission lines of the Metropolitan Edison Company. Transpositions were confined to switching stations or other accessible points such as H-frame locations; no new structures were erected. In some instances one line was arranged so as to electrostatically balance a second line, although an outage of either line would cause the line remaining in service to throw some unbalance on the system. There are several instances on the system of balancing one line against one or more lines. However, the system is large enough so that two or three line outages may occur without causing a prohibitive residual current, assuming, of course, that the coils have been retuned for the new system setup. In some very remote conditions of operation, the coils will have to be detuned or else taken out of service completely by short-circuiting them, which is equivalent to solidly grounding the transformer banks.

It seems necessary to add that in some instances the capacitances of the line insulators will be an important factor in the electrostatic unbalance to neutral. The capacitance of an individual pin-type insulator may be as high as 60 micro-microfarads. With bonded and grounded hardware the added balanced capacitance of the insulators could conceivably reduce the displacement voltage by ten per cent.

Obviously, the method of obtaining the direct capacitances to neutral of the line conductors can be extended to apply for transmission lines having one or more ground wires; double lines with or without ground wires yield also to this method. The equations for capacitances of double transmission lines with two ground wires are given in the appendix. Calculations involving all of these possibilities were made for the Metropolitan Edison Company system and the results were checked in the field; an accuracy was obtained in all cases comparable to the case which has just been considered in detail.

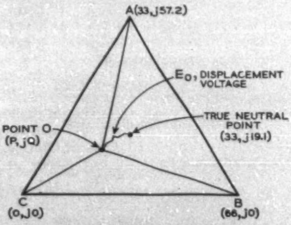

Figure 5. Potential diagram which applies to the method of the appendix

Appendix

The following alternative method is given for obtaining the displacement voltage E_0. It is especially useful for those who are unfamiliar with the symmetrical-component notation.

Figure 5 shows the potential diagram for three equal line voltages drawn in the complex domain. The line-to-line voltage is 66,000 volts. The co-ordinates in figure 5 are given in thousands of volts.

Let

E_{ao}, E_{bo}, and E_{co} = the voltages from O to A, B, and C, respectively, expressed in thousands of volts

As before

$$X_a = -j387{,}000 \text{ ohms}$$
$$X_b = -j424{,}000 \text{ ohms}$$
$$X_c = -j360{,}000 \text{ ohms}$$

Now

$$E_{ao} = 33 - P + j57.2 - jQ$$
$$E_{bo} = 66 - P + j0 - jQ$$
$$E_{co} = 0 - P + j0 - jQ$$

The above equations express the line-to-neutral voltages in terms of the co-ordinates of the complex number system.

If it is assumed that the direction of the line currents I_a, I_b, and I_c is such that they all flow toward the neutral point O, then:

$$I_a + I_b + I_c = 0 \qquad (15)$$

Now

$$I_a = \frac{E_{ao}}{X_a}$$
$$I_b = \frac{E_{bo}}{X_b}$$
$$I_c = \frac{E_{co}}{X_c}$$

These three currents will be in amperes if X_a, X_b, and X_c are in thousands of ohms, for the voltages are expressed in thousands of volts.

$$I_a = \frac{33 - P + j57.2 - jQ}{-j387} = j0.0852 - j0.00258P - 0.1475 + 0.00258Q \qquad (16)$$

$$I_b = \frac{66 - P - jQ}{-j424} = j0.1557 - j0.00236P + 0.00236Q \qquad (17)$$

$$I_c = \frac{-P - jQ}{-j360} = -j0.00278P + 0.00278Q \qquad (18)$$

Then substituting the values of the currents from equations 16, 17, and 18 in equation 15 there is produced (19).

$$j0.0852 - j0.00258P - 0.1475 + 0.00258Q + j0.1557 - j0.00236P + 0.00236Q - j0.00278P + 0.00278Q = 0 \qquad (19)$$

Now the algebraic sum of the terms directed along the axis of reals may be equated to zero.

$$-0.1475 + 0.00258Q + 0.00236Q + 0.00278Q = 0 \qquad (20)$$

$$0.00772Q = 0.1475$$

$$Q = 19.1$$

Similarly the algebraic sum of the terms directed along the axis of imaginaries may be equated to zero.

$$j0.0852 - j0.00258P + j0.1557 - j0.00236P - j0.00278P = 0 \quad (21)$$

$$0.00772P = 0.2409$$

$$P = 31.2$$

From figure 5 the co-ordinates of the true neutral are 33, j19.1. Now E_0 is the zero-sequence voltage or the displacement voltage from true neutral.

$$\begin{aligned} E_0 &= \sqrt{(19.1 - Q)^2 + (33 - P)^2} \\ &= \sqrt{(19.1 - 19.1)^2 + (33 - 31.2)^2} \\ &= 1.8 \text{ thousands of volts} \\ E_0 &= 1{,}800 \text{ volts} \end{aligned} \qquad (22)$$

This value of the residual voltage agrees with the value obtained by the first method.

The direct capacitances for a double-circuit transmission line with two ground wires can be found by writing equations similar to (1), (2), and (3) and proceeding as for the single-circuit three-wire line. Let A, B, and C represent the conductors for one circuit; D, E, and F will represent the conductors for a second circuit; M and N will represent two ground wires; and r_a, r_b, r_c, r_d, r_e, r_f, r_m, r_n will represent radii of the respective conductors. Then ag will be the distance of conductor A to ground. The expression ab' will represent the distance from A to the image B' of B, and ab will represent the distance from A to B. The same system will apply to all conductor distances.

There follows a list of terms which must be computed. The logarithms are to be taken using the base 10.

$$P_{aa} = \log \frac{2ag}{r_a}$$

$$P_{ab} = \log \frac{ab'}{ab}$$

$$P_{ac} = \log \frac{ac'}{ac}$$

$$P_{ad} = \log \frac{ad'}{ad}$$

$$P_{ae} = \log \frac{ae'}{ae}$$

$$P_{af} = \log \frac{af'}{af}$$

$$P_{am} = \log \frac{am'}{am}$$

$$P_{an} = \log \frac{an'}{an}$$

$$P_{bb} = \log \frac{2bg}{r_b}$$

$$P_{bc} = \log \frac{bc'}{bc}$$

$$P_{bd} = \log \frac{bd'}{bd}$$

$$P_{be} = \log \frac{be'}{be}$$

$$P_{bf} = \log \frac{bf'}{bf}$$

$$P_{bm} = \log \frac{bm'}{bm}$$

$$P_{bn} = \log \frac{bn'}{bn}$$

$$P_{cc} = \log \frac{2cg}{r_c}$$

$$P_{cd} = \log \frac{cd'}{cd}$$

$$P_{ce} = \log \frac{ce'}{ce}$$

$$P_{cf} = \log \frac{cf'}{cf}$$

$$P_{cm} = \log \frac{cm'}{cm}$$

$$C_a = 0.0389 \times \frac{\begin{vmatrix} 1 & P_{ab} & P_{ac} & P_{ad} & P_{ae} & P_{af} & P_{am} & P_{an} \\ 1 & P_{bb} & P_{bc} & P_{bd} & P_{be} & P_{bf} & P_{bm} & P_{bn} \\ 1 & P_{bc} & P_{cc} & P_{cd} & P_{ce} & P_{cf} & P_{cm} & P_{cn} \\ 1 & P_{bd} & P_{cd} & P_{dd} & P_{de} & P_{df} & P_{dm} & P_{dn} \\ 1 & P_{be} & P_{ce} & P_{de} & P_{ee} & P_{ef} & P_{em} & P_{en} \\ 1 & P_{bf} & P_{cf} & P_{df} & P_{ef} & P_{ff} & P_{fm} & P_{fn} \\ 0 & P_{bm} & P_{cm} & P_{dm} & P_{em} & P_{fm} & P_{mm} & P_{mn} \\ 0 & P_{bn} & P_{cn} & P_{dn} & P_{en} & P_{fn} & P_{mn} & P_{nn} \end{vmatrix}}{\begin{vmatrix} P_{aa} & P_{ab} & P_{ac} & P_{ad} & P_{ae} & P_{af} & P_{am} & P_{an} \\ P_{ab} & P_{bb} & P_{bc} & P_{bd} & P_{be} & P_{bf} & P_{bm} & P_{bn} \\ P_{ac} & P_{bc} & P_{cc} & P_{cd} & P_{ce} & P_{cf} & P_{cm} & P_{cn} \\ P_{ad} & P_{bd} & P_{cd} & P_{dd} & P_{de} & P_{df} & P_{dm} & P_{dn} \\ P_{ae} & P_{be} & P_{ce} & P_{de} & P_{ee} & P_{ef} & P_{em} & P_{en} \\ P_{af} & P_{bf} & P_{cf} & P_{df} & P_{ef} & P_{ff} & P_{fm} & P_{fn} \\ P_{am} & P_{bm} & P_{cm} & P_{dm} & P_{em} & P_{fm} & P_{mm} & P_{mn} \\ P_{an} & P_{bn} & P_{cn} & P_{dn} & P_{en} & P_{fn} & P_{mn} & P_{nn} \end{vmatrix}} \text{ microfarads per mile} \qquad (23)$$

$$C_b = 0.0389 \times \frac{\begin{vmatrix} P_{aa} & 1 & P_{ac} & P_{ad} & P_{ae} & P_{af} & P_{am} & P_{an} \\ P_{ab} & 1 & P_{bc} & P_{bd} & P_{be} & P_{bf} & P_{bm} & P_{bn} \\ P_{ac} & 1 & P_{cc} & P_{cd} & P_{ce} & P_{cf} & P_{cm} & P_{cn} \\ P_{ad} & 1 & P_{cd} & P_{dd} & P_{de} & P_{df} & P_{dm} & P_{dn} \\ P_{ae} & 1 & P_{ce} & P_{de} & P_{ee} & P_{ef} & P_{em} & P_{en} \\ P_{af} & 1 & P_{cf} & P_{df} & P_{ef} & P_{ff} & P_{fm} & P_{fn} \\ P_{am} & 0 & P_{cm} & P_{dm} & P_{em} & P_{fm} & P_{mm} & P_{mn} \\ P_{an} & 0 & P_{cn} & P_{dn} & P_{en} & P_{fn} & P_{mn} & P_{nn} \end{vmatrix}}{T} \text{ microfarads per mile} \qquad (24)$$

$$C_a = 0.0389 \times \frac{\begin{vmatrix} 1 & P_{ab} + P_{ae} & P_{ac} + P_{ab} & P_{am} + P_{an} \\ 1 & P_{bb} + P_{be} & P_{bc} + P_{bf} & P_{bm} + P_{bn} \\ 1 & P_{bc} + P_{ce} & P_{cc} + P_{cf} & P_{cm} + P_{cn} \\ 0 & P_{bm} + P_{em} & P_{cm} + P_{fm} & P_{mm} + P_{mn} \end{vmatrix}}{\begin{vmatrix} P_{aa} + P_{ad} & P_{ab} + P_{ae} & P_{ac} + P_{ab} & P_{am} + P_{an} \\ P_{ab} + P_{bd} & P_{bb} + P_{be} & P_{bc} + P_{bf} & P_{bm} + P_{bn} \\ P_{ac} + P_{cd} & P_{bc} + P_{ce} & P_{ce} + P_{cf} & P_{cm} + P_{cn} \\ P_{am} + P_{dm} & P_{bm} + P_{em} & P_{cm} + P_{fm} & P_{mm} + P_{mn} \end{vmatrix}} \text{ microfarads per mile} \qquad (25)$$

$$P_{cn} = \log \frac{cn'}{cn}$$

$$P_{dd} = \log \frac{2dg}{r_d}$$

$$P_{de} = \log \frac{de'}{de}$$

$$P_{df} = \log \frac{df'}{df}$$

$$P_{dm} = \log \frac{dm'}{dm}$$

$$P_{dn} = \log \frac{dn'}{dn}$$

$$P_{ee} = \log \frac{2eg}{r_e}$$

$$P_{ef} = \log \frac{ef'}{ef}$$

$$P_{em} = \log \frac{em'}{em}$$

$$P_{en} = \log \frac{en'}{en}$$

$$P_{ff} = \log \frac{2fg}{r_f}$$

$$P_{fm} = \log \frac{fm'}{fm}$$

$$P_{fn} = \log \frac{fn'}{fn}$$

$$P_{mm} = \log \frac{2mg}{r_m}$$

$$P_{mn} = \log \frac{mn'}{mn}$$

$$P_{nn} = \log \frac{2ng}{r_n}$$

Equation 23 gives the value of the capacitance of conductor A. Equation 24, which has the same denominator T as equation 23, gives the value of the capacitance of conductor B. If the column of six one's and two zero's successively replaces in the numerator the A column (farthest left), and the B, C, D, E, F, M, and N columns, expressions will be obtained for the capacitances of A, B, C, D, E, F, M, and N conductors in the order named. Equation 23 has the replacement in the A column; equation 24 in the B column. The same denominator T is used for all of the expressions.

These eighth-order determinants are rather difficult to solve. If the two circuits and ground wires are unsymmetrically placed on the towers, there can be no great simplification, and the eighth-order determinants will have to be solved laboriously by the method of minors to reduce to lower order determinants.

If the two transmission circuits are symmetrically placed on the tower line so that conductors A and D, B and E, C and F have the same capacitances, and ground wire M has the same position with respect to circuit ABC as N has with circuit DEF, then a considerable simplification may be accomplished. Equation 23 may then be reduced (25).

Equations similar to (25) may be written for C_b or C_e, and C_c or C_f. This is accomplished by successive replacement of columns in the numerator by the 1-1-1-0 column. A fourth-order determinant is, of course, easily reduced to three third-order determinants by the method of minors, and these in turn are readily solved.

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Discussion

F. Von Voigtlander (The Commonwealth and Southern Corporation, Jackson, Mich.): Doctor Lyon's analysis and computation of the electrostatic unbalance of a transmission line nicely illustrate the application of the theory of electrostatics to the practical solution of system capacitance unbalance problems.

It is interesting to note, however, that for single-circuit lines, the solution of the unbalanced voltages can be found very simply and directly for any common configuration by reference to charts 3, 22, and 23 of Engineering Report No. 16 of the Joint Subcommittee of Development and Research of the National Electric Light Association and the Bell Telephone System.

Using the example cited in the paper, from chart 3 of Report No. 16, we find that the highest voltage listed for five-foot vertical spacing is 22 kv. Entering chart 23 with 22 kv and lowest conductor 30 feet from the ground, we find the residual voltage to be 1.8 kv. Multiplying by the ratio of the actual line voltage (66 kv) to the voltage corresponding to the given spacing (22 kv), we find the characteristic residual voltage for the 66-kv line to be 5.4 kv. The zero-sequence voltage is one-third of the residual, so we have 1.8 kv for the neutral displacement which agrees exactly with Mr. Lyon's results for the totally nontransposed line. The effect of transpositions can readily be taken into account by multiplying this unbalanced voltage by the ratio of the equivalent nontransposed length to the total length of the system.

For twin circuits and circuits with ground wires, the results from these charts would have to be modified, but for single circuits without ground wires, they are well within the accuracy required for this type of work.

J. A. M. Lyon: The interesting discussion of F. Von Voigtlander tells of an easy method to obtain the residual voltage of a transmission line by means of published charts to which he has made complete reference. There is no denying that the use of such charts is extremely expeditious in obtaining the residual voltage for a simple transmission line composed of a single circuit without a ground wire.

It should be emphasized again that the author chose for the illustration of the method of his paper, the simplest case which occurred on the transmission system involved. It should also be pointed out again that a large portion of the total line mileage is equipped with double-circuit lines; some of these double-circuit lines as well as some of the single-circuit lines have ground wires. The author indicated in his paper a general method which is applicable to all these double- and single-circuit lines with and without ground wires.

Mr. Von Voigtlander grants that his method would not be satisfactory in these more complicated cases, at least not without numerous modifications and extensions of the charts. Such changes in his method would destroy the original simplicity and would thus defeat its purpose.

The Pumping System of the Colorado River Aqueduct

By J. M. GAYLORD
FELLOW AIEE

Synopsis: This paper describes the power features of the aqueduct being built to supplement the water supply of Southern California. Three hundred thousand kilowatts of power ultimately will be transmitted from Boulder Dam to operate 45 synchronous motors driving centrifugal pumps. High efficiency, long life, and simplicity of design are given precedence over close voltage regulation and the elimination of momentary interruptions.

THE Colorado River Aqueduct, which will double the present water supply of Southern California, is in its sixth year of construction and the initial development, originally estimated to cost $220,000,000, is approximately 85 per cent completed. This giant water-supply project is being built by The Metropolitan Water District of Southern California, a public corporation composed at this time (August, 1938) of the cities of Anaheim, Beverly Hills, Burbank, Compton, Fullerton, Glendale, Long Beach, Los Angeles, Pasadena, San Marino, Santa Ana, Santa Monica, and Torrance, a total of 13. The District was organized in 1928; bonds were voted in 1931; in September 1932, the Reconstruction Finance Corporation agreed to buy the first bonds; and construction was started in December of the same year. It is expected that water will be delivered through the aqueduct in 1939.

The project is closely linked to Boulder Dam since the 1,100,000 acre-feet of water appropriated by the District out of the flow of the river will be stored by the Government in Lake Mead and 36 per cent of the firm energy developed at Boulder will be used for the operation of pumping plants on the aqueduct. The power and pumping system, comprising five pumping plants and a 230-kv transmission system, will ultimately demand 300,000 kw of power and use $2^{1}/_{4}$ billion kilowatt-hours per year, making the project of special interest to the electrical and mechanical engineer.

The Aqueduct

The aqueduct has its intake on the Colorado River just above the confluence of the Bill Williams River 150 miles below Boulder Dam. The Parker Dam, just below the mouth of the Bill Williams, is now being completed by the United States Bureau of Reclamation using funds furnished by the District, and will raise the water 72 feet above river level creating a reservoir of 717,000 acre-feet capacity from which the aqueduct will draw its supply. From its intake at Parker reservoir the main aqueduct extends 241.7 miles to the terminal reservoir at Cajalco. This distance is covered by 92.1 miles of tunnel, 54.5 miles of cut and cover conduit, 62.8 miles of open-lined canals, 28.7 miles of inverted siphons, 1.2 miles of pump-delivery lines, 1.1 miles of open ditch, and 1.3 miles of passage through reservoirs.

From the Cajalco reservoir, distribution conduits will deliver water to the member cities of the District. Five pumping stations lift the water from Parker reservoir at elevation 450 feet above sea level to 1,807 feet, the highest point on the aqueduct, from which point flow is by gravity to the terminal reservoir at elevation 1,405. The problem from the standpoint of the electrical and mechani-

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Paper number 38-116, recommended by the AIEE committees on power transmission and distribution, general power applications, and electrical machinery, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted April 19, 1938; made available for preprinting July 19, 1938.

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1. For all numbered references, see list at end of paper.

cal engineer was to design and build five pumping plants capable of raising 1,500 second-feet of water through a total net lift of 1,617 feet by means of power obtained principally from Boulder Dam. Figure 1 is a general map of the project showing the location of the transmission lines, pumping plants, and related features of the project.

The Intake plant, figure 10, draws its water directly from the Parker reservoir, lifts it 291 feet, and delivers it through the Colorado River tunnel to Gene Wash reservoir. The Gene pumping plant, figure 11, lifts the water 303 feet additional and delivers it through two tunnels to the Copper Basin reservoir. Since both the Colorado River and Copper Basin tunnels operate as pressure conduits, surge tanks are provided at the tops of each lift. The outflow from Copper Basin is regulated by gates operated by supervisory control from Gene pumping plant. From Copper Basin the water flows by gravity 60 miles to the Iron Mountain pumping plant, figure 12, where the third lift, 144 feet, is made. At this plant no natural storage reservoir is available but a regulating forebay reservoir of 100 acre-feet capacity has been

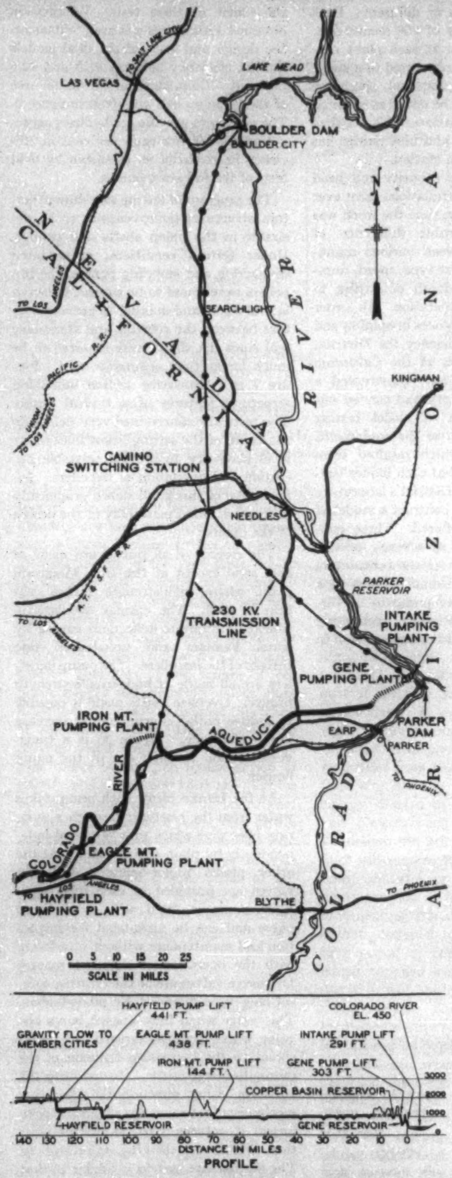

LAS VEGAS
TO SALT LAKE CITY
LAKE MEAD
BOULDER DAM
BOULDER CITY
CALIFORNIA
NEVADA
RIVER
ARIZONA
TO LOS ANGELES
SEARCHLIGHT
PACIFIC
UNION
TO LOS ANGELES
KINGMAN
CAMINO
SWITCHING STATION
NEEDLES
A.T. & S.F. R.R.
TO LOS ANGELES
PARKER
RESERVOIR
230 KV.
TRANSMISSION
LINE
INTAKE
PUMPING
PLANT
GENE
PUMPING PLANT
IRON MT.
PUMPING PLANT
AQUEDUCT
EARP
PARKER
DAM
PARKER
TO PHOENIX
RIVER
COLORADO
EAGLE MT.
PUMPING PLANT
COLORADO
TO HAYFIELD
PUMPING PLANT
LOS ANGELES
BLYTHE
TO PHOENIX
0 5 10 15 20 25
SCALE IN MILES
HAYFIELD PUMP LIFT
441 FT.
COLORADO RIVER
EL. 450
GRAVITY FLOW TO
MEMBER CITIES
EAGLE MT. PUMP LIFT
438 FT.
INTAKE PUMP LIFT
291 FT.
IRON MT. PUMP LIFT
144 FT.
GENE PUMP LIFT
303 FT.
COPPER BASIN RESERVOIR
3000
2000
1000
0
HAYFIELD RESERVOIR
GENE RESERVOIR
140 130 120 110 100 90 80 70 60 50 40 30 20 10 0
DISTANCE IN MILES
PROFILE

Figure 1. Map of power system for Colorado River Aqueduct and profile of section that includes pumping plant

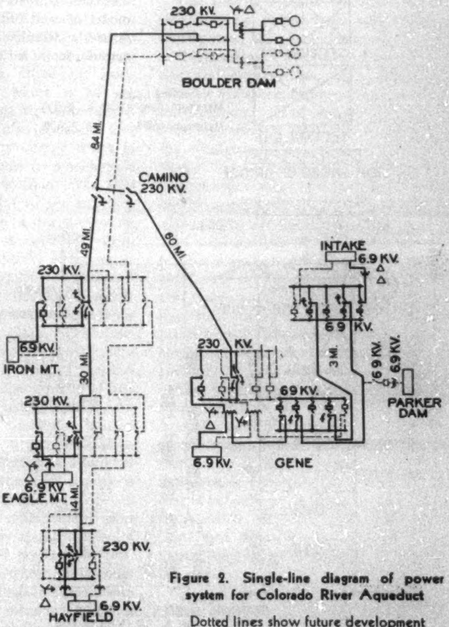

230 KV.
BOULDER DAM
84 MI.
CAMINO
230 KV.
49 MI.
60 MI.
INTAKE
6.9 KV.
230 KV.
6.9 KV.
6.9 KV.
IRON MT.
230 KV.
3 MI.
6.9 KV.
6.9 KV.
30 MI.
230 KV.
6.9 KV.
PARKER
DAM
6.9 KV.
GENE
6.9 KV.
EAGLE MT.
14 MI.
230 KV.
6.9 KV.
HAYFIELD

Figure 2. Single-line diagram of power system for Colorado River Aqueduct

Dotted lines show future development

constructed. Normally water is pumped direct from the open canal and in case of power failure it overflows into the reservoir. Two of the pumps in the plant have auxiliary inlets from the reservoir permitting water accumulated during a power outage to be pumped out later. The forebay reservoir will hold the full aqueduct flow for 45 minutes and a siphon spillway provides a means of wasting water onto the desert during a prolonged outage. The next pumping plant in the series is Eagle Mountain, figure 13, with a lift of 438 feet, located 40 miles from the top of the Iron Mountain lift. At this plant also, a 100-acre-foot regulating forebay reservoir has been provided. Sixteen miles farther along the aqueduct is located the Hayfield reservoir, capacity 83,000 acre-feet and the Hayfield pump lift of 441 feet, figure 14. At this plant independent intakes are provided from the canal and from the reservoir, so that any pump can draw water from either source.

Hydraulic Equipment

The pumps are all of the single-stage single-suction vertical-shaft volute type without guide vanes or diffusers. Each pump has a capacity of 200 second-feet. The first installation at each plant consists of three pumps connected to a single discharge pipe. Additional discharge pipes and pumps will be added as required to meet water demands until a total of three discharge lines and nine pumps has been installed in each station.

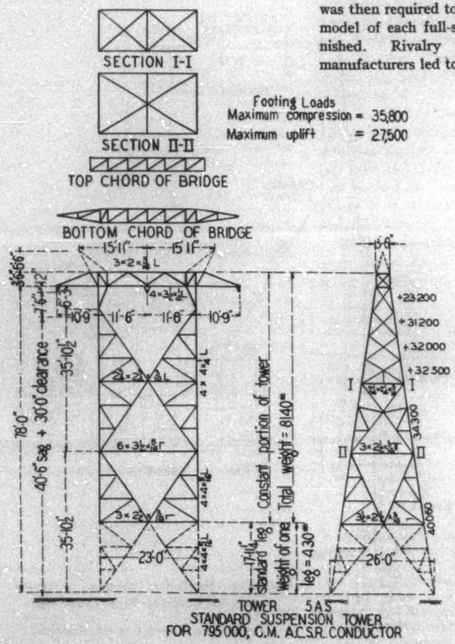


Figure 3. Standard suspension tower, type 5AS

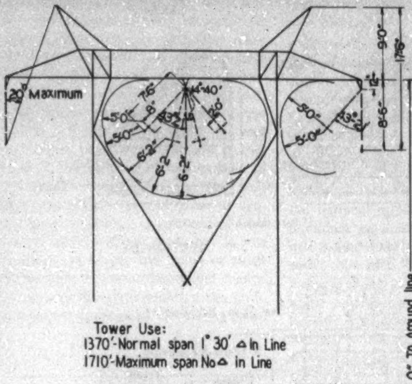
For 795,000 circular mils, aluminum steel reinforced conductor

Few pumps of the capacity and head required for these installations have ever been built, and at the time the work was undertaken considerable difference of opinion existed between various manufacturers as to the best type, speed, number of stages, and depth of setting to insure satisfactory operation. In order to resolve these differences in opinion and to improve pump efficiency the District, with the co-operation of the California Institute of Technology, constructed a pump testing laboratory and carried out a two-year program of model testing prior to the purchase of the equipment. The specifications which resulted from these tests required that each bidder submit for testing in the District's laboratory prior to the award of contract a model of one of the pumps offered. Three contracts were awarded as a result of this competition and each of the contractors was then required to submit for testing a model of each full-size pump to be furnished. Rivalry between competing manufacturers led to keen competition in the course of these tests. Information obtained during tests was used to improve the design and each of the final models showed efficiency between 91.5 and 92.5 per cent. Characteristic curves for one of the Gene models are given in figure 9. The contracts provide for bonuses aggregating $55,000 for each per cent in efficiency in excess of 88 as shown by field tests of the full-sized pumps.

The program of testing also showed certain structural improvements to be desirable in the pump shafts and casings. Under certain conditions, encountered in starting and stopping pumps, the impellers were found to be subject to heavy lateral forces and in order to prevent contact between the rotating and stationary seal rings the shafts were required to be much larger than ordinarily used. Figure 7 is a transverse section indicating structural features of a typical pump. The tests also determined very definitely the depth of the setting below inlet water level necessary to prevent unstable operation and cavitation of impellers. Information of this kind, shown graphically in figure 8, aided materially in the design of the plants.

The casings of all pumps are made of cast steel except at the Iron Mountain plant where high-strength nickel cast iron is used. The casings are heavily ribbed to minimize deflections caused by water hammer and unbalanced side thrusts of the impellers. The pump impellers are all made of high-tensile-strength bronze. Oversize shafts made it possible to reduce radial clearances and increase pump efficiency. Figure 20 is a transverse section through one of the pump houses.

At the Intake plant, each pump takes water from the reservoir through a separate inlet pipe which may be closed independently by plain sliding gates. At the other plants motor-operated butterfly valves are provided in each inlet pipe. Each pump is fitted with a discharge valve and can be unwatered for inspection and maintenance without interfering with the operation of adjacent pumps. Discharge valves are of the rotating-conical-plug type operated by oil cylinders. This valve when fully opened has a circular water passage tapered to correspond to the increasing diameter of the pump-discharge nozzle. In operating the valve in either direction the operating mechanism first raises the plug slightly to unseat it, rotates it to the desired position, then lowers the plug, and reseats it. The oil pressure system is similar to that ordinarily used to operate the Servomotors of hydraulic turbine governors.



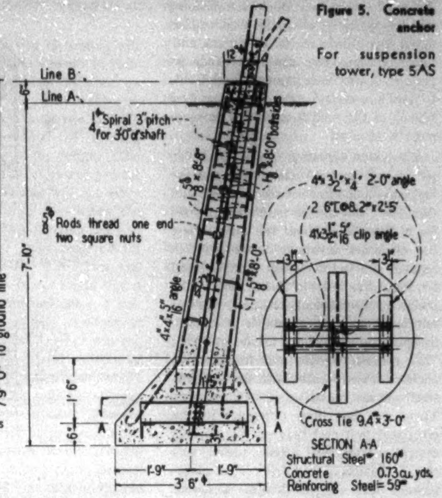
Tower Use:
1370'-Normal span 1° 30' △ in Line
1710'-Maximum span No △ in Line

Figure 4. Clearance diagram

For standard suspension tower, type 5AS for 795,000 circular mils aluminum steel reinforced conductor

Figure 5. Concrete anchor

For suspension tower, type 5AS



Oil for operation is kept under air pressure in accumulator tanks in sufficient quantity for two complete valve operations without recourse to the oil pumps. Pressure surges in the pipe lines, which occur when the pumps are suddenly stopped due to power failure, are held within permissible values by the operation of the discharge valves under the control of a device similar to a hydraulic turbine governor. Upon failure of the power supply the discharge valves close rapidly to the point of definite throttling of the flow. During this interval the moving water column in the discharge pipe comes to rest due to the action of gravity and return flow from the pump is allowed to commence. The latter part of the closing stroke is made very slowly and the reverse flow brought to zero with definite limitation as to increase in pressure. The device is susceptible of adjustment to insure complete closure without excessive pressure rise and before dangerous speed reversal occurs.

Power Supply

The Metropolitan Water District was one of the three principal underwriters of Boulder Dam. The Boulder Canyon Act authorized the Secretary of the Interior to begin construction of the dam only after he had obtained power contracts guaranteeing the return of the Government's investment within a term of fifty years. Contracts with The Metropolitan Water District, the City of Los Angeles (Bureau of Power and Light), and the Southern California Edison Company, meeting the above requirements were consummated in the spring of 1930. Under its contract the District was allotted and agreed to pay for 1,526,000,000 kilowatt-hours per year (subject to a small annual diminution and the three-year load-building allowance), which is 36 per cent of the firm energy to be developed at the dam. It was also given the first right to use secondary energy which is expected to be available in considerable quantities from time to time. The District also has the right to use the firm energy allotted to the States of Arizona and Nevada, but not used by them. To deliver the full capacity of the aqueduct will require 2,250,000,000 kilowatt-hours annually, or 724,000,000 kilowatt-hours per year in excess of its firm power allotment at Boulder Dam. The aqueduct is designed to provide water for a long period of development of the territory supplied and the growth of the demand will undoubtedly extend over many years. The District's present firm energy supply will undoubtedly be in excess of its requirements for a number of years, but when this firm supply is exceeded the deficiency can be made up by taking over some of the States' firm power, if unused, or by a combination of secondary and standby power to be purchased from other allottees of Boulder Dam power. In addition to these power resources the District owns one-half of a 100,000-kw power site at Parker Dam.

Transmission Line

The District's power system was designed as an independent unit to efficiently meet the particular requirements of the situation. Sturdy mechanical and electrical construction, high efficiency, and low maintenance were given primary consideration. Of secondary importance were regulation of speed and voltage and the elimination of momentary interruptions. The aqueduct, as a whole, was designed with water storage and carrying capacity sufficient to allow for service outages of seven per cent of the time. This condition practically eliminates any serious objection to momentary interruptions of electrical power service. For this reason no attempt was made to design what could be considered an interruption-proof transmission line. Overhead ground wires and counterpoise were omitted.

The transmission voltage of 230,000 was selected as the result of extensive studies to determine the type and voltage of the system which would give lowest cost during a 50-year period considering maintenance, depreciation, interest, and cost of lost power. A similar study of the transmission problem between Intake, Gene, and Parker power plant resulted in the adoption of 69 kv for this part of the system.

The initial and final development of the system are shown in figure 2. The first installation is a single circuit throughout.

A second circuit from Boulder to Camino will be necessary when the load reaches about two-thirds of its maximum and, unless energy costs are lower than at present, it will be economically advisable to build a second circuit on the west branch of the system when full development is attained.

The lowest elevation of the line is 750 feet above sea level near Gene, and the highest is 3,650 feet between Camino and Boulder. The towers were strengthened for a distance of about ten miles where the elevation is above 3,200 feet. A reduced conductor tension was also used for about five miles in this section. The design for the transmission line towers was selected from a large number of studies, designs, and estimates, including wooden "H" frames, flexible-steel frames with anchor towers at each tenth span, guyed-steel frames, and rigid-steel towers. There was but little difference in the estimated first cost between the various designs. The rigid steel tower was chosen because of its lower cost of maintenance and rugged design. Concrete footings, figure 5, were used throughout the line, except in a few places where towers were set on native rock and excepting the Danby Dry Lake crossing where creosoted pile foundations, figure 6, were found to be necessary to protect the metal and concrete from the corrosive action of the concentrated alkali of the soil. Five different towers are used and the designs provide for legs shorter or longer than standard to meet side-hill conditions. One tower of each type was tested at 50 per cent overload and one standard suspension tower was tested to destruction. In the latter test, failure occurred in the overhanging arm of the bridge at 90 per cent overload.

Two complete lines were located and designed, one for copper and one for steel-reinforced aluminum conductor and alternative bids for all work and materials were taken at the same time. Alternative bids also were permitted on towers to be designed by the bidder to determine the savings, if any, due to the rotated base and waisted designs. The aluminum line proved to be approximately ten per cent cheaper than the copper line and the District design of conventional tower, figure 3, was materially lower in price than any of the others. The line includes 227 miles of steel-reinforced aluminum circuit and ten miles using hollow copper conductor of the twisted I-beam type.

Simple, strong fittings of standard design are used. Suspension clamps were selected after tests at the factory to determine the seat curvature best adapted to minimize vibration stresses. Strain clamps are of the compression type. The aluminum conductor and its steel core are gripped separately, but the clamps for copper compress the entire cable. Vibration dampers of the Stockbridge type are used on both aluminum and copper. Armor rods are also used on the aluminum conductors as an additional safeguard against vibration damage and to protect the conductor against arcs. Arcing horns are not used on insulator assemblies but sufficient clearance has been allowed to permit their installation at a later date if desired. It is a well-established fact that vibration troubles increase with conductor tension and in the interest of long life and low-maintenance cost a conservative value was adopted, namely, a tension of 30 per cent of the ultimate strength at 25 degrees Fahrenheit with a wind load of eight pounds per square foot. The normal level span was fixed at 1,370 feet, or less than the economic span, in the belief that the resulting lower tower height would reduce the lightning hazard.

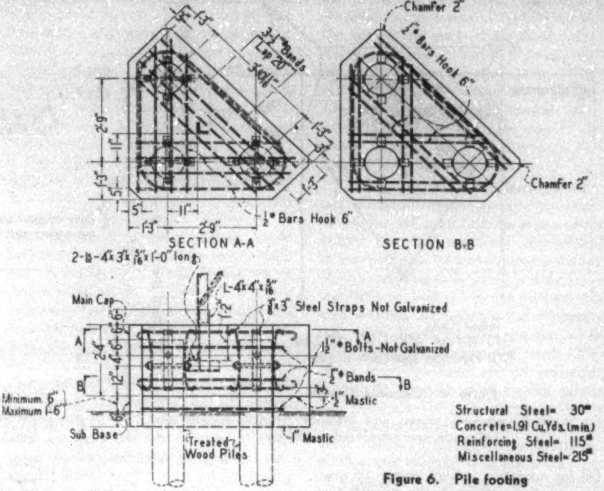


Figure 6. Pile footing

For special suspension tower, type 5AH

In establishing clearances between the transmission-line conductors and steel, the dimensions were adjusted to maintain full flashover value of the insulators with the conductor subjected to a wind velocity of about 32 miles per hour. A higher wind velocity will reduce the clearances, but even with an actual wind velocity of 56 miles per hour the clearances will be sufficient to prevent flashovers due to switching surges ordinarily to be expected. The transmission-line profile, of course, is not level and, in order to allow for all possible conditions, clearances were determined for a three-degree angle in the line. The conductor-to-steel clearances are 60 inches with a three-degree angle and a wind pressure of eight pounds per square foot and 74 inches with a 3-degree angle and a wind pressure of $2^1/_2$ pounds per square foot. Figure 4 is the clearance diagram for the standard suspension tower.

The towers carry two ground wires for approximately one mile from each station. Mid-span clearance between conductors and ground wires is 30 feet with aluminum conductors and 20 feet with copper conductors.

Motors

The completed installation will include 45 6.9-kv synchronous motors ranging in size from 4,300 to 12,500 horsepower. Each motor is equipped with direct-connected main and pilot exciters, and is designed for full-voltage across-the-line starting. All motors are totally enclosed for recirculation of air, are water cooled, and provided with carbon-dioxide fire protection. In the preliminary studies of system design it was apparent that normal characteristics of generators and motors would not give the required stability of operation and a radical reduction in system reactance was necessary. Within practical limits the reactance values of the generators could be reduced at less cost than could the corresponding values of the motors. There was, however, an economic limit to such reduction and, having reached this limit, the improvement of motor characteristics offered the next best possibility. Normal value of synchronous reactance in motors of this size is about 110 per cent, but it was necessary to reduce this value to about 50 per cent to obtain the desired results. In making this determination the problem was set up on an a-c calculating board and solution obtained for the steady-state operating limit. The effect of saturation of the iron in the motors was taken into consideration and their reactance was expressed in terms of equivalent reactance which corresponds to that under actual operating conditions. After the motor characteristics for steady-state operation had been determined, further study was made on a calculating board of the transient conditions occurring when each motor is started. As a result of these studies an equivalent reactance value of 34 per cent at 105 per cent rated output and 95 per cent voltage was specified. Calculations indicated that this figure will give a power margin of about 25 per cent for the present transmission system with four motors operating at each plant.

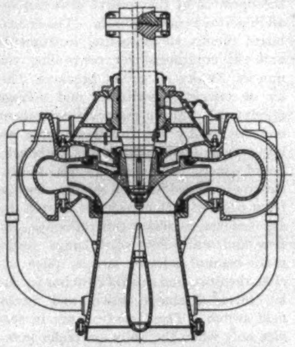


Figure 7. Transverse section through pump, capacity 200 cubic feet a second against 310-foot head, 400 rpm

Extra heavy shaft to resist unbalanced radial forces when starting

Main bearing is self-aligning, casing heavily reinforced with cast steel ribs

Across-the-line starting of motors will produce heavy momentary drops in voltage but, since this system supplies no power customers and practically no lighting load, voltage drops are not objectionable. This method of starting greatly simplifies station design. The initial rush of current at starting must be limited to a value that will not allow the terminal voltage to drop below the minimum required for starting the first motor or below the value required for stable operation when more than one motor is in service. It was found that a single amortisseur winding gave a sufficiently low inrush as well as adequate starting torque and this type of winding was adopted because of its substantial construction. The specifications require the motors to produce sufficient "break away" torque to overcome starting friction and the most difficult condition is that which occurs when the initial motor is started in the pumping plant farthest from the generators. As each additional motor is placed in operation, those already in service, with the assistance of their voltage regulators, help to maintain normal voltage giving greatly improved starting conditions. Starting the last motor, however, is the most critical operation as the system is then closer to its power limit. When the 20th motor is started on the single-line system, there will be a margin of stability of about 25 per cent if all regulators are out of service and 35 per cent with the regulators in service. Each motor is required to develop at least 14 per cent of rated torque in starting with normal voltage maintained at Camino. Each motor must bring the pump to synchronous speed with water in the casing, discharge valve closed, and the voltage back of the transient reactance held normal at the generators. At the speed for field application the torque must be sufficient to drive the pump under the above hydraulic conditions with the field short-circuited through a starting resistor or with the field short-circuited upon itself and also with field applied at the most unfavorable angle for synchronizing. These requirements insure that the motors may be easily synchronized regardless of the angle at which the field is applied.

Receiving Stations

The main transmission line extends to all of the pumping plants except Intake which is supplied with power at 69 kv from Gene. At each plant one bank of transformers has been installed and this constitutes one-half of the ultimate installation. Transformers are wye-connected on the high side with neutrals solidly grounded and delta-connected on the low tension sides. All transformers are water cooled and provided with nitrogen-seal equipment. The 230-kv receiving stations are provided with a main bus and a transfer bus as indicated in figure 15. Because of lack of ground space a high steel structure is used and in the case of the disconnecting switches between the incoming circuits and the oil circuit breaker, the blades of the disconnecting switches are placed in a vertical position. All other high-voltage disconnects are of the horizontal-break type supported on tripods made up of standard high-strength line insulators. The disconnecting switches are motor-operated and controlled from the main bench-

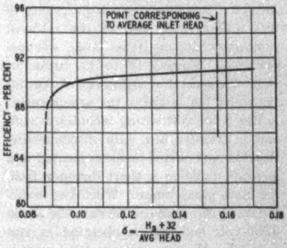


Figure 8. Typical cavitation characteristic

H_s = Static inlet head on center line of impeller. Average head = average total pumping head

Note the abrupt drop in efficiency as the inlet head is reduced

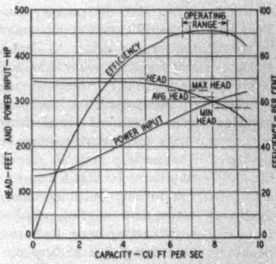


Figure 9. Complete pump characteristic

Laboratory test of contractor's model for Gene pumping plant. Model ratio 1 to 5.53, tested at 2,133.5 rpm

Note efficiency of over 90 per cent throughout operating range

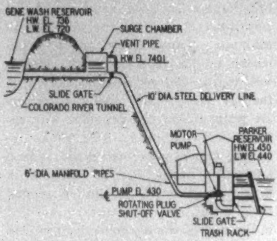

GENE WASH RESERVOIR
H.W. EL. 736
L.W. EL. 720
SURGE CHAMBER
VENT PIPE
H.W. EL. 740 L
SLIDE GATE
COLORADO RIVER TUNNEL
10' DIA. STEEL DELIVERY LINE
MOTOR
PUMP
PARKER RESERVOIR
H.W. EL. 450
L.W. EL. 440
6'-DIA. MANIFOLD PIPES
PUMP EL. 430
ROTATING PLUG SHUT-OFF VALVE
SLIDE GATE
TRASH RACK

Figure 10. Intake pumping plant

Schematic profile

Pump data: Average head 294 feet—capacity 200 cubic feet a second each—speed 400 rpm—motors 9,000 horsepower each

Number of pumps: 3—initial installation
9—ultimate installation

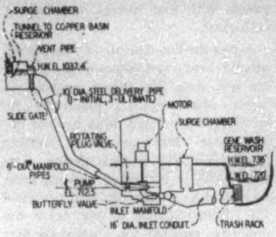

SURGE CHAMBER
TUNNEL TO COPPER BASIN RESERVOIR
VENT PIPE
H.W. EL. 1037.4'
10' DIA. STEEL DELIVERY PIPE (1-INITIAL, 3-ULTIMATE)
MOTOR
SLIDE GATE
SURGE CHAMBER
ROTATING PLUG VALVE
GENE WASH RESERVOIR
H.W.EL. 736
L.W.EL. 720
6'-DIA. MANIFOLD PIPES
PUMP EL. 712.5
BUTTERFLY VALVE
INLET MANIFOLD
16' DIA. INLET CONDUIT
TRASH RACK

Figure 11. Gene pumping plant

Schematic profile

Pump data: Average head 310 feet—capacity 200 cubic feet a second each—speed 400 rpm—motors 9,000 horsepower each

Number of pumps: 3—initial installation
9—ultimate installation

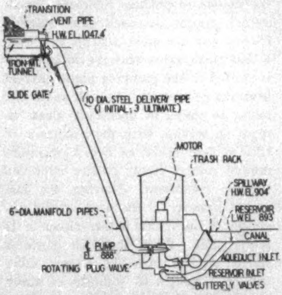

TRANSITION
VENT PIPE
H.W. EL. 1047.4'
IRON MT. TUNNEL
SLIDE GATE
10' DIA. STEEL DELIVERY PIPE (1 INITIAL; 3 ULTIMATE)
MOTOR
TRASH RACK
SPILLWAY H.W. EL. 904'
RESERVOIR L.W. EL. 893
6'-DIA. MANIFOLD PIPES
CANAL
PUMP EL. 888'
ROTATING PLUG VALVE
AQUEDUCT INLET
RESERVOIR INLET
BUTTERFLY VALVES

Figure 12. Iron Mountain pumping plant

Schematic profile

Pump data: Average head, 146 feet—capacity 200 cubic feet a second each—speed 300 rpm—motors 4,300 horsepower each

Number of pumps: 3—initial installation
9—ultimate installation

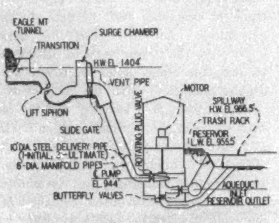

EAGLE MT. TUNNEL
TRANSITION
SURGE CHAMBER
H.W. EL. 1404'
VENT PIPE
MOTOR
LIFT SIPHON
SLIDE GATE
ROTATING PLUG VALVE
SPILLWAY H.W. EL. 966.5'
TRASH RACK
RESERVOIR L.W. EL. 955.5'
10' DIA. STEEL DELIVERY PIPE (1-INITIAL, 3-ULTIMATE)
6'-DIA. MANIFOLD PIPES
PUMP EL. 944'
BUTTERFLY VALVES
AQUEDUCT INLET
RESERVOIR OUTLET

Figure 13. Eagle Mountain pumping plant

Schematic profile

Pump data: Average head 440 feet—capacity 200 cubic feet a second each—speed 450 rpm—motors 12,500 horsepower each

Number of pumps: 3—initial installation
9—ultimate installation

board. The high-voltage oil circuit breakers, having a rupturing capacity of 2½ million kva, are of the oil-blast explosion-chamber type and solenoid operated. Figures 16 to 19, inclusive, show the general arrangement of the stations and the design of the switch racks.

The 6,900-volt wiring includes a main and a transfer bus with disconnecting switches and circuit breakers located in a separate building a short distance from the main pump house. When two transformer banks are in service the main 6,900-volt bus will be operated as two sections. Disconnecting switches permit sectionalizing at any one of six places between positions 3 and 7, allowing approximately equal loading of each part of the bus and each bank of transformers. A bus-tie position is provided which by means of disconnecting switches may be connected between either half of the main bus and the transfer bus. Thus the bus-tie oil circuit breaker may be substituted for any motor oil circuit breaker or that for the station light and power position. All disconnecting switches are group-operated and are provided with mechanical interlocking devices and keys to prevent opening or closing of disconnecting switches should the associated breaker be closed, or opening of a circuit breaker cell door unless the circuit breaker disconnecting switches are open, or, connecting more than one circuit to the transfer bus at any one time. The 6,900-volt buses are constructed from one-quarter-inch by four-inch copper bars except at Eagle and Hayfield plants where 5-inch copper channels are used. Special heavy-duty expansion-type bus clamps are provided. All buses are bolted together with ⅝-inch Everdur bolts and all contacts are silver plated. The 6.9-kv oil circuit breakers have an interrupting capacity of 500,000 kva and are rated at 600 and 1,200 amperes. These breakers are of the oil-blast type and are solenoid operated. The oil circuit breaker isolating switches are six-pole, group-operated, and mechanically interlocked with the circuit breakers. All others are three-pole, group-operated switches. All disconnecting switches are enclosed in steel compartments and a set of key interlocks is provided to prevent improper operation.

Station Control

The control rooms are so arranged that the operator at the control desk can see all important control panels. The benchboard carries all indicating instruments and the equipment for controlling the motors, 230-kv oil circuit breakers, 230-kv disconnecting switches, and voltage regulators. Station metering equipment and the 230-kv line and transformer relays are mounted on the rear of the benchboard panels. Individual panels or cubicles are provided for each motor. On the front panel of each cubicle are located the annunciator, auxiliary control relays, oil-flow and water-flow pilot lamps, automatic-manual selector switch, pilot exciter rheostat, and control switches for inlet valve, discharge valve, and motor field switch. These controls are in service only when the units are under manual control and are inoperative during automatic operation. The rear panel of

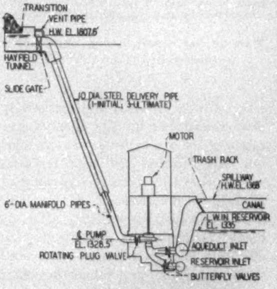

TRANSITION
VENT PIPE
H.W. EL. 1807.6'
HAYFIELD TUNNEL
SLIDE GATE
10' DIA. STEEL DELIVERY PIPE (1-INITIAL; 3-ULTIMATE)
MOTOR
TRASH RACK
SPILLWAY H.W.EL. 1368
6'-DIA. MANIFOLD PIPES
CANAL
L.W. IN RESERVOIR EL. 1335
PUMP EL. 1328.5'
ROTATING PLUG VALVE
AQUEDUCT INLET
RESERVOIR INLET
BUTTERFLY VALVES

Figure 14. Hayfield pumping plant

Schematic profile

Pump data: Average head 444 feet—capacity 200 cubic feet a second each—speed 450 rpm—motors 12,500 horsepower each

Number of pumps: 3—initial installation
9—ultimate installation

Figure 15. Single-line wiring diagram—Iron Mountain pumping plant

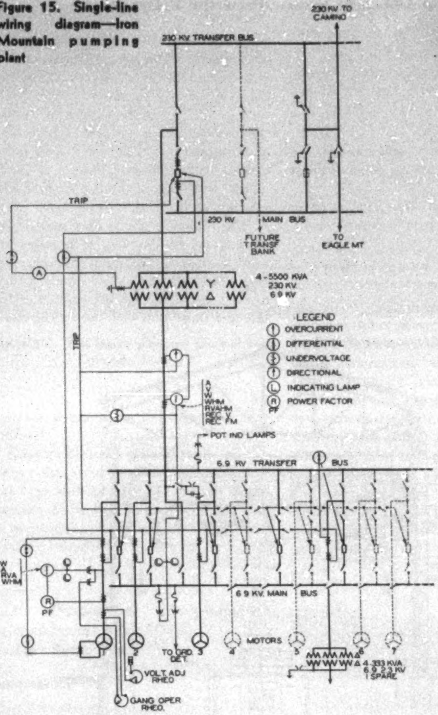
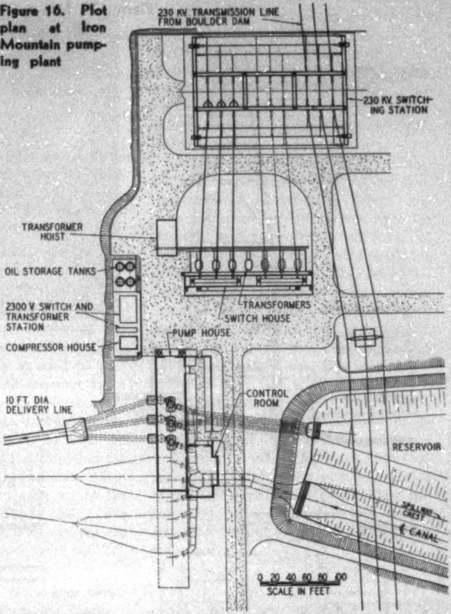

230 KV TO CAMINO
230 KV TRANSFER BUS
TRIP
230 KV
MAIN BUS
FUTURE TRANSF BANK
TO EAGLE MT
4-5500 KVA
230 KV
6.9 KV
TRIP
LEGEND
OVERCURRENT
DIFFERENTIAL
UNDERVOLTAGE
DIRECTIONAL
INDICATING LAMP
POWER FACTOR
PF
A
V
W
WHM
RVAHM
REC V
REC FM
POT IND LAMPS
6.9 KV TRANSFER BUS
6.9 KV. MAIN BUS
W
A
RVA
WHM
PF
1
2
TO GRD DET
3
4
MOTORS
5
6
7
4-333 KVA
6.9-2.3 KV
1 SPARE
VOLT ADJ RHEO
GANG OPER RHEO.

Figure 16. Plot plan at Iron Mountain pumping plant


230 KV TRANSMISSION LINE FROM BOULDER DAM
230 KV SWITCHING STATION
TRANSFORMER HOIST
OIL STORAGE TANKS
2300 V SWITCH AND TRANSFORMER STATION
COMPRESSOR HOUSE
TRANSFORMERS
SWITCH HOUSE
PUMP HOUSE
CONTROL ROOM
10 FT. DIA DELIVERY LINE
RESERVOIR
SPILLWAY CREST
℄ CANAL
0 20 40 60 80 100
SCALE IN FEET

Reservoir receives water from aqueduct in excess of that pumped

Substructure of pump house completed for ultimate of nine pumps

Outdoor hoist for transformer repair

Spare single-phase transformer can be connected in either of two banks without moving

the cubicle carries the overload and differential relays, voltage regulator with its cut-out switch, and the watthour meter. Other panels carrying d-c controls, temperature and water-line indicators, and telephone equipment are located in frames set flush with the walls of the control room. Access to the rear of these panels is obtained from adjacent rooms. Incoming control cables terminate on racks in the control room basement which are provided with terminal blocks for cable conductors and panel wiring.

Under automatic control, before a pumping unit can be started, it is required that the water for cooling and oil for lubrication be functioning properly, that the inlet valve be open, and the discharge valve closed. The pump is then started with the motor field short-circuited through a resistance. As the motor approaches synchronous speed, a synchronizing relay, described later, causes the field switch to close, bringing the motor into synchronism. The voltage regulator is then automatically brought into service and the pump discharge valve opened, establishing flow in the discharge pipe and fully loading the motor. In the normal stopping of a pump the discharge valve is first closed and then the motor oil circuit breaker opened. In case of an emergency shut-down, the motor oil circuit breaker opens immediately and the discharge valve closes under control of the hydraulic surge suppressor previously described.

In the motor-starting scheme, provision is made for automatically applying the motor field when the field flux linkages are a maximum. This occurs twice every slip cycle and the rotor is then in a favorable position for being brought into synchronism. At the instant of maximum field flux linkages the induced field current is zero and the armature current is a minimum. Near synchronous speed the armature current pulsates at a rate proportional to the slip. The field-application relay which is of the wattmeter type closes its contacts at a minimum instantaneous value of the pulsating current. As the pulsations become less frequent, the relay contacts remain closed for a longer period. When the closing period attains a predetermined duration, a series of timing relays operate to close the field breaker at multiples of a half slip cycle later corresponding to a time for maximum field flux linkages. The motor speed at which the field-application relay operates is independent of the voltage. This is accomplished by equipping the synchronous relay with a restraining coil having a torque proportional to the square of the voltage and an operating coil proportional to the motor power input which is proportional to the square of the voltage. After synchronizing the motor the circuit of the potential coil of the wattmeter-type relay is altered so that it becomes a power-factor relay which trips the motor breaker on low power factors resulting from out-of-step conditions.

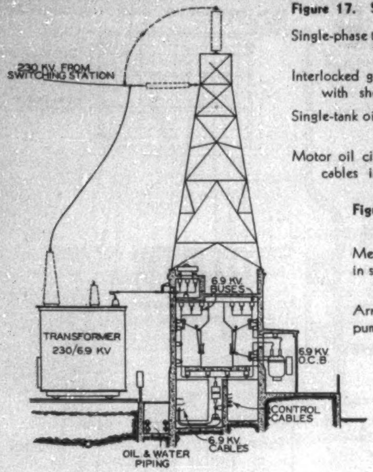

230 KV. FROM SWITCHING STATION
6.9 KV. BUSES
TRANSFORMER 230/6.9 KV
6.9 KV. O.C.B.
CONTROL CABLES
6.9 KV. CABLES
OIL & WATER PIPING

Figure 17. Section through 6.9-kv switch houses

Single-phase transformers with short bar connections to buses

Interlocked gang operated disconnecting switches with sheet-metal barriers between circuits

Single-tank oil circuit breakers in cells that open to out-of-doors

Motor oil circuit breakers at switch house with cables in tunnel direct to motor terminals

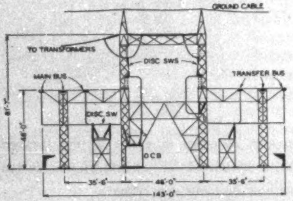

GROUND CABLE
TO TRANSFORMERS
DISC SWS
MAIN BUS
TRANSFER BUS
DISC SW
OCB
81'-7"
48'-0"
35'-6"
46'-0"
35'-6"
143'-0"

Figure 18. Transformer section of 230-kv switching station

Steel structure is latticed column and girder construction

Busses are of copper line conductor supported by strain insulators

Bus taps are copper tubing

Remote controlled motor-operated disconnecting switches are either horizontal or vertical break

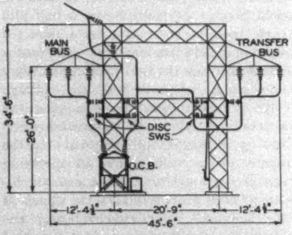

MAIN BUS
TRANSFER BUS
34'-6"
26'-0"
DISC SWS.
O.C.B.
12'-4½"
20'-9"
12'-4½"
45'-6"

Figure 19. Section of 69-kv switching station

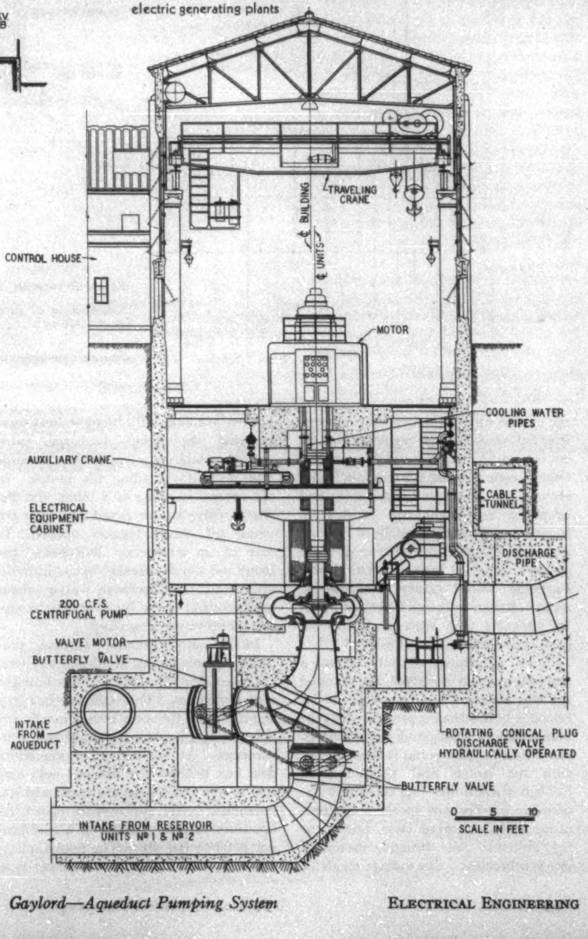
Steel structure is latticed column and girder construction

Disconnecting switches over oil circuit breakers are six-pole, one mechanism per circuit

Figure 20. Iron Mountain pumping plant—section through pump house

Mechanically connected butterfly valves in suction permit water to be taken from either the aqueduct or reservoir

Arrangement of pump, motor, cranes, and pump house very similar to that in hydroelectric generating plants


TRAVELING CRANE
℄ BUILDING
℄ UNITS
CONTROL HOUSE
MOTOR
COOLING WATER PIPES
AUXILIARY CRANE
CABLE TUNNEL
ELECTRICAL EQUIPMENT CABINET
DISCHARGE PIPE
200 C.F.S. CENTRIFUGAL PUMP
VALVE MOTOR
BUTTERFLY VALVE
INTAKE FROM AQUEDUCT
ROTATING CONICAL PLUG DISCHARGE VALVE HYDRAULICALLY OPERATED
BUTTERFLY VALVE
INTAKE FROM RESERVOIR UNITS Nº 1 & Nº 2
0 5 10
SCALE IN FEET

Protective Relays

Relays for a power system such as this are principally for the protection of lines and apparatus rather than the sectionalizing of the equipment for continuity of service. The simpler types of relays are used and each line and each piece of major apparatus is provided with relays in order to facilitate the location of faults. Phase protection of the 230-kv system is accomplished by directional-type relays at the pumping plants and impedance-type relays at Boulder. The latter are used because of the small difference between currents at Boulder resulting from a short circuit at the extremities of the line and those that flow during the start-

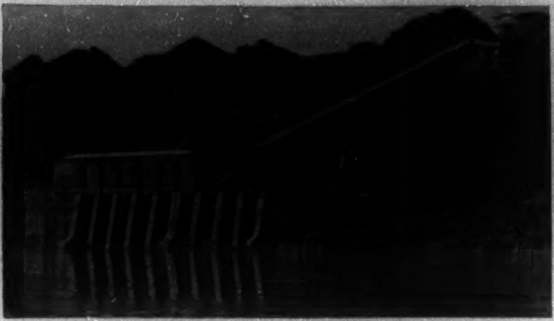


Figure 21. The Intake pumping plant at the Colorado River

ing of a motor when the system is near full load.

Relays for line-to-ground faults on the 230-kv line are of the overcurrent induction type and operate from current transformers in the transformer neutrals. At Gene pumping plant, because of the three-winding transformer bank with both 230-kv and 69-kv neutrals grounded, a directional overcurrent relay is used to prevent the 230-kv breaker tripping for a fault in the 69-kv system. The relay has two current elements, one of which measures the sum of the currents in both the 230- and 69-kv neutrals and the other the current in only the 230-kv neutral. When the currents in the two coils are in the same direction, the contacts close. For ground faults on the 230-kv system the only current in the relay is from the 230-kv fault and the relay contacts are set to close under this condition. For faults on the 69-kv system the current in the 69-kv neutral is greater than that in the 230-kv neutral and opposite in direction. Thus the relative direction of current in the coils is reversed and the relay contacts are held open.

For phase protection of the 69-kv lines duo-directional induction-type relays with instantaneous overcurrent relays and cross-connected current transformers are placed at both Gene and Intake pumping plants. For protection of each single 69-kv line and to back up the duo-directional relays, overcurrent induction-type relays are placed at Gene and directional-overcurrent induction-type relays at Intake. The relays at Gene for line-to-ground protection are the overcurrent induction type and at Intake the directional overcurrent type with residual voltage obtained from capacitors on the breakers for the directional element.

Differential protection with induction-type differential relays is used on each motor and each transformer. The motors also have overload relays with a long-time setting to prevent tripping during starting.

Appendix. Equipment Characteristics

Boulder Power Plant

GENERATORS

Number installed initially	2
Number to be installed ultimately	4
Kilovolt-amperes each at unity power factor	82,500
Voltage	16,500
Number of poles	40
Speed, revolutions per minute	180
Short-circuit ratio	2.74
Direct-axis transient reactance, per cent	17.5
Moment of inertia (WR^2) each—not less than	105,000,000
Total weight, pounds, approximate	2,000,000
Outside diameter, feet	40
Type of insulation	Class *B*
Excitation—nominal 250 volts, main and pilot exciter direct connected	
Exciter response	0.50

POWER TRANSFORMERS

Number initially installed	4
Number to be installed ultimately	7
Type	Water cooled outdoor
Rating of each, kilovolt-amperes, single-phase	55,000
Voltage	230,000 Y/16,500 delta
Frequency, cycles per second	60
Impedance, per cent	10.8
Exciting current at normal voltage, per cent	2.4
Full-load efficiency, per cent	99.51
Half-load efficiency, per cent	99.52
Total weight per unit with oil, pounds	243,000
Floor space and height, feet	$10^1/_2$ by $12^1/_2$ by 30
Quantity of oil per unit, gallons	9,300

OIL CIRCUIT BREAKERS—HIGH VOLTAGE

Type	Outdoor 3 tank
Voltage class, kilovolts	230
Interrupting capacity, kilovolt-amperes	2,500,000
Normal current rating, amperes	1,200
Speed of opening, cycles	Less than 8
Weight with oil, pounds	204,000
Gallons of oil, per breaker	15,500

Pumping Plants

HIGH-VOLTAGE OIL CIRCUIT BREAKERS

	Intake and Gene	Gene, Iron Mountain, Eagle, and Hayfield
Type	Outdoor 3 tank	Outdoor 3 tank
Voltage class, kilovolts	69	230
Interrupting capacity, kilovolt-amperes	500,000	2,500,000
Normal current rating, amperes	600	1,200
Speed of opening, cycles	Less than 5	Less than 5
Weight with oil, pounds	13,200	183,600
Floor space and height, feet	11 by 6 by 11	12 by $45^1/_2$ by $21^1/_2$
Gallons of oil	600	13,000

LOW-VOLTAGE OIL CIRCUIT BREAKERS

	Intake, Gene, Eagle Mountain, and Hayfield	Iron Mountain Plus Auxiliary Service, All Plants
Type	Indoor cell	Indoor cell
Voltage class	6,900	6,900
Interrupting capacity, kilovolt-amperes	500,000	500,000
Normal current rating, amperes	1,200	600
Speed of opening, cycles	Less than 5	Less than 5
Weight with oil, pounds	3,378	3,374
Gallons of oil	82	82

MOTORS

Number initially installed	3 at each plant—total 15
Number to be installed ultimately	9 at each plant—total 45
Type each	Totally enclosed synchronous
Voltage	6,900
Excitation	125 volts, main and pilot exciter direct connected
Exciter response	1.0

	Intake and Gene	Iron Mountain	Eagle Mountain and Hayfield
Horsepower rating, each	9,000	4,300	12,500
Speed, revolutions per minute	400	300	450
Number of poles	18	24	16
Short circuit ratio	2.15	2.19	2.26
Direct axis transient reactance, per cent	25.4	21.5	26
Starting inrush of normal current, per cent	586	646	517
Moment of inertia (WR^2) each, pound feet^2	397,600	296,000	430,000
Type of insulation	Class *B*	Class *B*	Class *B*
Total weight, pounds each	160,000	97,500	180,000

TRANSFORMERS

Type, each single phase	Outdoor, water cooled
Frequency, cycles per second	60

	Intake	Gene	Iron Mountain	Eagle Mountain and Hayfield
Number used initially	12	3	3	3 each
Number used ultimately	24	6	6	6 each
Number spare units	..	1	1	1 for both plants
Rating kilovolt-amperes each at 230 kv	..	22,000	5,500	15,000
Ditto, each at 69 kv	2,000	11,000	..	..
Ditto, each at 6,900 volts	2,000	11,000	5,500	15,000
High voltage	69,000 Delta	230,000Y	230,000Y	230,000Y
Intermediate voltage	..	69,000Y	..	..
Low voltage	6,900 Delta	6,900 Delta	6,900 Delta	6,900 Delta
Impedance—				
230 to 6.9 kv (per cent)	..	9.29	9.77	10.00
230 to 69 kv (per cent)	..	11.28	..	..
69 to 6.9 kv (per cent)	9.1	20.53	..	..
Exciting current at normal voltage, per cent	1.26	2.75	3.39	3.50
Full-load efficiency, per cent	99.00	99.39	99.13	99.35
Half-load efficiency, per cent	99.05	99.30	99.06	99.27
Total weight per unit, pounds	40,300	211,000	109,000	155,000
Floor space and height, feet	8 by 8 by 16	10 by 17 by 27	$8^1/_2$ by 12 by $24^1/_4$	$9^1/_4$ by $15^1/_2$ by 25

PUMPS

Characteristics and Principal Dimensions

	Intake	Gene	Iron Mountain	Eagle Mountain	Hayfield
1. Total pumping head including pipe losses in feet—Average	294	310	146	440	444
2. Ditto—Maximum	303	324	156	452	476
3. Ditto—Minimum	272	286	133	427	432
4. Inlet pressure heads in feet when total pumping head is—Average	14	17	15	20	37
5. Ditto—Maximum	9	5	5	10	5
6. Ditto—Minimum	19	23	16	22	37
7. Speed rpm	400	400	300	450	450
8. Specific speed, gallons-per-minute units	1,690	1,624	2,140	1,403	1,395
Impeller dimensions, inches					
9. Outside diameter	76	78	$74^1/_2$	$81^{15}/_{16}$	$81^{15}/_{16}$
10. Inlet diameter	40	40	$40^7/_8$	34	34
11. Width at discharge	9.703	8.833	$12^5/_{16}$	7.0	7.0
12. Inside diameter discharge flange	42	42	48	$40^1/_2$	$40^1/_2$
13. Number of vanes	8	8	6	9	9
14. Shaft diameter, inches	22	22	20	24	24

The discharge capacity of each pump at average or rated head is 200 cubic feet per second.

230-Kv Transmission Line

CONDUCTORS

Steel Reinforced Aluminum (ACSR)

Circular mils of aluminum	795,000
Strands of aluminum	26
Diameter of aluminum strands, inches	.1749
Strands of steel	7
Diameter of steel strands, inches	.1360
Diameter of conductor, inches	1.108
Weight per foot, pounds	1.093
Ultimate strength, pounds	31,200

Copper

Circular mils total	510,900
I-beam section core, circular mils	44,300
Intermediate layer, 22 wires, diameter, inches	.0966
Outer layer, 28 wires, diameter, inches	.0966
Diameter of conductor, inches	1.004
Weight per foot, pounds	1.61
Ultimate strength, pounds	22,500

Overhead Ground Wire

Number per tower	2
Double-galvanized high-strength strand, $^1/_2$ inch with 7 wires	
Diameter of each wire, inches	.165
Weight per foot, pounds	.517
Breaking strength, pounds	21,000

Conductor Tensions

	Copper	ACSR
Maximum design tension at elevations over 3,200 feet at 0° F., $^1/_2$ inch of ice and a wind pressure of 6 pounds per square foot, pounds	..	14,600
Maximum design tension at elevations under 3,200 feet at 25° F. and a wind pressure of 8 pounds per square foot, pounds	6,700	9,360
Maximum design tension adjacent Gene and Boulder at 25° F. and a wind pressure of 8 pounds per square foot, pounds	..	5,600 adjacent to Boulder 8,100 adjacent to Gene

Ground-Wire Tensions

	Over Copper	Over ACSR
Maximum design tension in $^1/_2$ inch steel ground wire	3,000	6,100

TOWERS

Type 5AS, Standard Suspension, Aluminum Conductor

Designed for light loading (8 pound wind at 25° F.):
1. Normal level span of 1,370 feet, with an angle in the line of 1° 30′.
2. Maximum span of 1,710 feet with no angle.
3. One broken conductor or one broken ground wire.

Weight of constant portion of tower, with standard legs, pounds	10,216
Weight of steel in concrete footings, per tower, pounds	788
Quantity of concrete in footings, per tower, cubic yards	2.92
Number of towers	736

Type 5AA, Suspension Tower, Aluminum Conductor

Designed for light loading (8 pound wind at 25° F.):
1. Normal level span of 1,370 feet, with an angle in the line of 9°.
2. Maximum span of 1,800 feet with an angle in the line of 9°.
3. Maximum span of 2,600 feet with an angle in the line of 5°10′.
4. One broken conductor or one broken ground wire.

Weight of constant portion of tower with standard legs, pounds	11,841
Weight of steel in concrete footings, per tower, pounds	1,008
Quantity of concrete in footings, per tower, cubic yards	3.80
Number of towers	87

Type 5AR, Suspension Type for Aluminum Conductors—for use at railroad crossings

Designed for heavy loading (6 pound wind on wires covered with $^1/_2$ inch of ice):
1. Normal level span of 1,376 feet.
2. Two broken conductors or one broken ground wire.

Weight of constant portion of tower with standard legs, pounds	13,625
Weight of steel in concrete footings, per tower, pounds	1,528
Quantity of concrete in footings, per tower, cubic yard	4.92
Number of towers	12

Type 5AD, Anchor Tower

Designed for light loading (8 pound wind at 25° F.):
1. Span of 1,500 feet, 39° angle in line and no wires broken.
2. Span of 1,700 feet, 39° angle in line, one conductor and one ground wire broken on one side of tower.
3. Span of 1,500 feet for wind and 5,700 feet for vertical loading, 17° angle in line and all wires broken on one side of tower.
4. Span of 1,500 feet for wind and 5,700 feet for vertical loading, 17° angle in line, and on one side of tower all wires broken, on other side of tower one outside conductor and one ground wire broken.

Weight of constant portion of tower with standard legs, pounds	14,836
Weight of steel in concrete footings, per tower, pounds	2,112
Quantity of concrete in footings, per tower, cubic yards	8.52
Number of towers	37

Type 5AH, Suspension Tower, Extra Insulation

Designed for light loading (8 pound wind at 25° F.):
1. Normal level span of 1,370 feet with an angle in the line of 2° 40′.
2. Maximum span of 1,940 feet with no angle in the line.
3. One broken conductor or one broken ground wire.

Weight of constant portion of tower with standard legs, pounds	10,682
Weight of steel in concrete footings, per tower, pounds	816
Quantity of concrete in footings, per tower, cubic yards	3.28
Number of towers	68
Number of towers with pile footings	24

All towers designed to withstand a wind pressure of 13 pounds per square foot on $1^1/_2$ times the projected area of each tower face.

69-Kv Transmission Line

TOWERS

	Suspension	Dead End
Weight of constant portion of tower with standard legs, pounds	11,050	16,260
Weight of steel in concrete footings, pounds	788	2,112
Quantity of concrete in footings, cubic yards	2.92	8.52
Number of towers	7	6

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Discussion

R. W. Sorensen (California Institute of Technology and Board of Consulting Engineers for The Metropolitan Water District of Southern California): Mr. Gaylord's paper, so ably presented by Mr. Peabody, sums up a most unique and gargantuan project; namely, that of lifting a river of no mean size over 1,600 feet in an upward direction in order that the water of this river might flow steadily from the Colorado River along a 300-mile course to Southern California.

At first glance, one may say why are electrical engineers so much interested in a pumping system? But after all, such a system is made possible only by the availability of a perfectly enormous amount of electric power, over 300,000 kilowatts or one-third the firm power developed at Boulder Dam. Because Mr. Peabody has had a large part in the development of the pumps, and because the unique improvements made therein by research work are so striking and important, he has perhaps left in your minds the idea that the electrical features of this great project may have been somewhat secondary. Such, however, is not the case. Many factors of importance, such as the almost unparalleled management of the general manager and chief engineer, Dr. Frank E. Weymouth, and the work of the many engineers who have carried on the details, as well as the wholly voluntary and extraordinary services of the Board of Directors—have all contributed to an accomplishment, almost complete, of heretofore unprecedented dimensions. For example, this enormous project, costing $220,000,000 is being completed inside the original bond issue. This feature is due to careful analysis of problems of which we may take the transmission line as an example.

A study of the materials used in that line, as given in the paper, shows that high-quality work has been done throughout, but that no overconstruction has been used. For example, the line is built to have a few outages per year; because such outages do no particular harm, they simply result in stopping the flow of water for a short time, which for many years will be immaterial. Thus we find the cost of line necessary to be only about $9,500 a mile, which is indeed a low price for a 230-kv line. Speaking of transmission lines, may I say that in planning these lines some exceedingly interesting problems were encountered.

Before any construction could be started a desert had to be made habitable. In order to do this electric power for construction purposes was provided throughout the length of the aqueduct by running a 69,000-volt line from Colton to the Colorado River, a distance of about 250 miles. Those of you who are interested in transmission lines can readily appreciate the problems pertaining to stability and other features involved in such a line. Like problems are, of course, encountered in the 230-kv line installed permanently for pumping, because it is quite evident that one cannot design a line by simple rule-of-thumb methods and expect that line to perform satisfactorily when the huge motors used are started unless special consideration is given to motors, generators, and line so all factors will work together for such extreme conditions.

I have enjoyed greatly my contact with Mr. Gaylord and the rest of the staff of The Metropolitan Water District of Southern California, because it has been a pleasure to help them analyze with great care each order for machinery, transmission-line conductor, insulators, etc., in a way which would assure for every part of the job the best possible solution at the lowest cost consistent with the kind of construction needed. Nearly all of the equipment described is now installed. In a few months operation will start and, after a reasonable time has elapsed, I expect Mr. Gaylord or some of his staff will be able to present a most interesting paper on the electrical operation of the system.

In the meantime I think the report made in the paper concerning this particular type of application of electric energy is a record worthy of attention as an example of how dependent we are upon electrical power transmission for, without that possibility, the Colorado River Aqueduct of the Metropolitan Water District would have been much more difficult to construct and operate—if not wholly impossible.

Narrow-Band Transmission System for Animated Line Images

By A. M. SKELLETT
NONMEMBER AIEE

Synopsis: A method of transmission and reproduction of line images is described which utilizes a cathode-ray tube for reproduction, the spot of which is made to trace out the line image 20 or more times a second. In an experimental test, a drawing of a woman's head was reproduced with an equivalent total band width of approximately 2,600 cycles. This was made up of two bands, each 1,300 cycles wide for the potentials to the two sets of cathode-ray deflector plates. Analysis of a more complex image, such as that of an animated cartoon shows that such material could be transmitted and reproduced by this method within a total band width of 10,000 cycles.

Means are described for transcribing from drawing or animated cartoon film into recordings (similar to sound recordings) from which the potentials for transmission and subsequent operation of the cathode-ray tube may be obtained.

IT IS inherent in the systems of television on which work is being most actively pursued, that high definition must at all times be realized, no matter how simple the transmitted image may be. Yet many useful images do not contain a great amount of detail and in sending them large blank areas on the available field will be wastefully transmitted. A line drawing or an animated cartoon is an example of such a simple image. If it were possible to transmit the figure and line background, without the blank areas, a great simplification would occur. The system proposed herein was designed to take advantage of this fact.

By the application of proper potentials the bright spot of a cathode-ray tube may be made to traverse any path desired. Furthermore, if the spot is made to follow a certain path, such as that shown in figure 1, over and over, 20 or more times a second, this trace will produce on the end of the tube a stationary image as in figure 2.

Two potentials, each varying with time, are needed properly to actuate the spot. They are applied, the one across the horizontal and the other across the vertical set of plates of the cathode-ray tube. They are directly proportional, respectively, to the x and y co-ordinates of the points of the figure (for example, points 1, 2, 3, etc., of figure 1) taken along the path of the spot in the direction in which it moves. These potentials for the image of figure 1 are shown in graph form in figure 3.

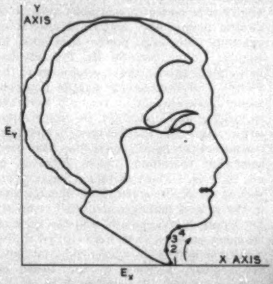

Figure 1. Resolution of a simple image into a single line. Total band needed for transmission: 2,000 cycles

Experimental Apparatus

The reproduction of figure 1 shown in figure 2 was obtained as follows: The x and y co-ordinates were plotted in polar co-ordinate form $[r = (a + x)$ or $(b + y)$, $\theta = t]$ in concentric tracks one inch wide on a cardboard disk 12 inches in diameter. The area under each curve, that is, between the x curve and $r = a$ and between the y curve and $r = b$, was then blackened, and a negative photostat made of it. See figure 4. This disk was mounted on the shaft of a variable-speed motor so that it could be rotated 20 times a second or more and brightly illuminated.

A lens and a radially oriented slit were arranged in such a manner that a portion of the image of the x track fell across the slit. Behind it a photoelectric cell received the illumination passing through the slit. Thus the output of the photocell was directly proportional to the x co-ordinate at each instant of time. A similar setup, mounted alongside, gave potentials proportional to the y co-ordinates.

These two potentials were amplified through separate identical amplifiers and sent over two pairs of wires to the cathode-ray tube.

The method just described of recording the X and Y potential tracks on a disk is suitable for a single drawing but not for animated drawings. For these a record on a long ribbon or film is more practicable.

A device which transcribes from drawings into X and Y potential tracks is called a "transcriber" and the apparatus for obtaining these potentials from the recorded tracks is called the "converter." In figure 5 the complete process from image to image is shown in schematic form, the recordings in this case being on film.

Band Width

For the simplest type of image in which the drawing may be resolved into a single unbroken line, for example, figure 1, the total frequency spectrum or band width needed will consist of the sum of the band widths needed to transmit accurately the X and Y potentials. These may be assumed to be equal. The band width may then be determined by the analysis of the X (or Y) potential variations of a single trace by means of harmonic analysis.

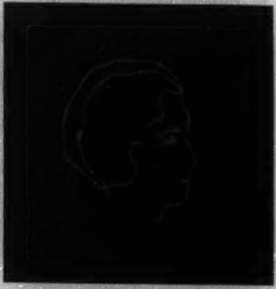

Figure 2. Photograph of an image reproduced by the first experimental setup. The chief differences between this image and that of figure 1 (from which the recorded tracks were obtained) were traced to errors in the recording

The magnitude of this potential E may be expressed as a function of time by a

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Paper number 38-114, recommended by the AIEE committee on communication and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 23, 1938; made available for preprinting July 12, 1938.

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Fourier equation with a sufficient number of terms.

$$E = f_1 \int_0^{\frac{1}{f_1}} E\,dt + \sum_{n=1}^{\infty} A_n \sin(\theta + \phi_n) \quad (1)$$

Here f_1 is the fundamental or trace frequency (about 20 per second) and A_n and ϕ_n are the amplitude and phase angle of the nth harmonic.

For any drawing the problem becomes that of determining the highest value of n needed for satisfactory reproduction. The band width will be that included between f_1 and the frequency of this nth component. The band-width will vary with f_1 for equal fidelity of reproduction. For instance if f_1 is increased to 30, then exactly the same image will be obtained with a band width from 30 to 3,000 cycles as would be realized with one from 20 to 2,000 cycles.

In general the two kinds of detail which will suffer most apparently by the elimination of higher order terms are straight portions and sharp bends. The over-all shape and form of the image and the larger details are dependent on the lower frequencies in the bands. These frequencies are therefore the most important ones. If the bands include enough harmonics to reproduce small details, even in approximate form, the larger details and general form will be reproduced with good fidelity. Thus the band width is largely determined by the fidelity of small detail desired.

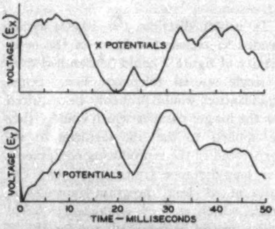


Figure 3. Potential variations needed to reproduce figure 1 on cathode-ray tube

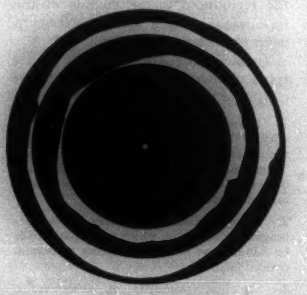


Figure 4. X and Y potential tracks used in reproducing figure 3

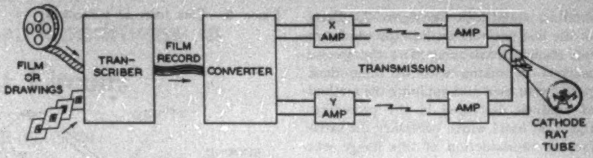


Figure 5. Schematic diagram of complete apparatus

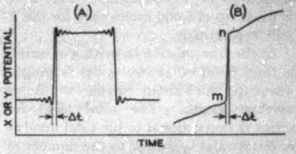


Figure 6. Typical wave forms generated in turning the spot on or off

Suppose we pick out the smallest detail, in the form of a sharp bend or kink in the curve of a potential function and attempt to determine the minimum frequency needed to reproduce it in approximate form. A little experience in harmonic analysis of typical forms will enable one to determine this frequency by inspection. Generally speaking it will be that frequency the sine wave of which can be made to fit most closely the general shape of the detail. For instance, for a sharp corner, the shape of the wave will determine the curvature that the corner will have when it is reproduced. If the band width extends up to and includes this frequency the detail in the image which gave rise to that in the potential curve will, in general, be reproduced in approximate form. The band width determined in this manner may, therefore, be taken as that necessary for satisfactory reproduction.

A rough check was obtained on the adequacy of this approximate method of analysis by equivalently narrowing the band width of the apparatus used in the reproduction of figure 2. The total band width determined as above for this figure was 2,000 cycles (1,000 cycles for each band). The bands passed by the amplifiers were equivalently reduced to 1,300 cycles each by speeding up the converter and no marked change in the image occurred.

Dark Paths

For all but the simplest of figures it will be necessary for the spot to traverse paths in the field along which the spot must not be visible. There are two methods of accomplishing this: (1) the beam can simply be cut off by a negative potential applied on the modulating cylinder as the spot passes over such dark paths or (2) it may be made to traverse them so quickly that the visibility of the trace is negligible.

For the first method an additional signal channel is needed and if its band width is made equal to that of the X or Y potential channels, the spot may be turned on or off in an amount of time somewhat less than that required to trace out the finest detail. The wave form would have the general form shown in figure 6*a* and the highest frequency f_n would be given by

$$f_n = \frac{1}{2\,\Delta t} \quad (2)$$

For the second method, that is, that of speeding up the spot over dark paths, the wave form required will be in the simplest case similar to that shown between m and n in figure 6*b*. The sharpness of the corners at m and n is determined by the upper frequency limit of the system.

Actually this method requires that either or both the X and Y potentials suddenly change in value so that the spot will abruptly jump across the dark path at such a speed that it is not visible. In figure 2, the velocity of the spot was deliberately increased by a factor of 5 in tracing the line across the neck. A velocity at least twice as great as this is required to make the trace effectively invisible.

Thus it appears that this latter method is to be preferred in general since it requires no increase in band width and no extra channel with its extra facilities.

A cartoon of Walt Disney's popular figure was chosen for determining the frequency band required to reproduce a sketch of considerable complexity. This was resolved into one continuous line such as would be followed by the cathode-ray spot for reproduction. This reproduction would look like figure 7. Parts of the line are over dark paths which are not shown. Assuming that the second method of taking care of these dark paths were used and applying the method of analysis described in the preceding, the total band width necessary for satisfactory reproduction of this image was judged to be 10,000 cycles. This total is made up of 5,000 cycles each for the X and Y potentials.

In the transmission of writing or script, a total band of 10,000 cycles is judged adequate for about seven words of average length. The total band for script is proportional to the total number of letters and spaces or to the number of words of average length.

The Centering Coefficient

The first term of the series of equation 1 is of importance in centering the image in the field. It is

$$f_1 \int_0^{\frac{1}{f_1}} E\,dt$$

This is simply the total area of the curve over one cycle of period $1/f_1$ divided by the base line $1/f_1$. It thus represents the mean distance of the curve from the zero axis. If eliminated, the shape of the curve and hence also of the image would not be altered, but the centering of the image would vary as the figures moved around in the field. As an example of the effect of this, suppose that the image consisted of a central figure of some complexity and a very simple background. Then if the figure started to walk from the center to the side of the field, the absence of the transmission of this component would hold the figure near the center and the background would slide away in the opposite direction.

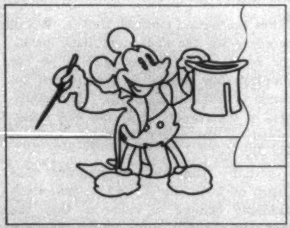
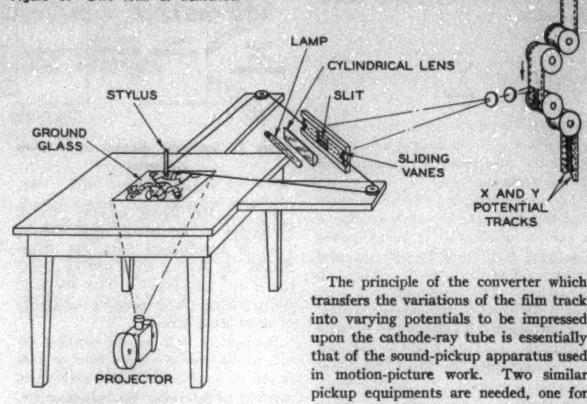


Figure 7. Total band needed for transmission of this image is 10,000 cycles wide. The spot will move one-half as fast over the figure as it does over the background

Figure 8. One form of transcriber



Ordinarily this component would be eliminated by the apparatus since it gives rise mainly to a direct current. It presents a similar problem in television where it determines the general brightness of the background. Several schemes have been devised to reintroduce or to compensate for it in television and some of these might be used here also. If the image frequencies are used to modulate a carrier, the band may be made to extend to zero on the low-frequency end and this component will then be included in the transmission. The particular method used will depend on the transmission system.

The Transcription Process

In order to get the X and Y potential recordings, some means of transcribing the original drawings or images into these potential recordings is needed. The simplest method at present seems to be one in which a stylus is manually made to follow the lines of the drawing. This stylus would be coupled up mechanically or electrically with suitable apparatus whose function would be to record simultaneously on film or other media, two tracks, the amplitudes of which would be proportional, respectively, to the X and Y co-ordinates of the various positions of the stylus. Such a machine is shown in simplified form in figure 8. It is arranged to transcribe directly from the film of an animated cartoon. The projector throws onto the ground glass one frame of the film at a time, and the stylus is moved by the operator over the lines of the image.

The principle of the converter which transfers the variations of the film track into varying potentials to be impressed upon the cathode-ray tube is essentially that of the sound-pickup apparatus used in motion-picture work. Two similar pickup equipments are needed, one for the X and one for the Y potential track. If there is a sound track to accompany the cartoon, a third equipment will be needed for it. The standard speed of 90 feet per minute for the film is adequate since with the usual types of pickup this allows a band width of about 6,000 cycles.

Applications

In urban districts the 10,000 cycles needed to transmit a figure of the complexity of figure 7 could be handled over a single special telephone line. Some equalization would probably be required for the longer circuits which could either be applied to the line facilities or incorporated in the reproducing equipment. For long-distance transmission two circuits of at least program-transmission grade, which are now equalized to 5,000 cycles, would be required, one for the X and one for the Y potentials. Depending upon the degree of definition desired in the reproduced image, circuits of this type might require some supplemental delay equalization.

The following are exemplary of the types of image transmission which are capable of transmission by this method:

1. Drawings, diagrams, and maps either with or without animation, animated cartoons, etc., of purely commercial value. These might include animated drawings depicting the working of new equipment, fashion sketches, etc.
2. Script, including signatures and foreign-language characters, which could not readily be handled by ordinary existing communication facilities.
3. Sketches, primarily of news value. These would include drawings of famous people, places, ceremonies, fashions, and

Similitude of Critical Conditions in Ferroresonant Circuits

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RECENTLY a number of articles dealing with the critical conditions in ferroresonant circuits has appeared. However, no simple generalization is possible from these previous methods. The generalization presented in this paper offers certain information pertaining to similitudes of circuits in a surprisingly simple manner. Knowing the critical stable conditions for one reactor, the principle of similitude enables the predetermination of the critical stable conditions for any other reactor using the same grade of iron.

Equation for Critical Conditions

The circuit referred to in this investigation is one containing a resistance, a condenser, and a saturable-core reactor as shown in figure 1. Assuming that the applied voltage can be resolved into the in-phase and quadrature components, the following vector relation holds:

$$E^2 = E_X^2 + E_R^2 \tag{1}$$

where

E = applied voltage
E_X = $(E_{LX} - E_C)$ = reactive voltage
E_R = $(E_{LR} + E_{RO})$ = in-phase voltage
E_{LX} = reactive component of the reactor voltage
E_{LR} = in-phase component of the reactor voltage

The critical conditions are defined as points on the volt-ampere curve where $dE/di = 0$. Since $dE/d\left(\frac{ni}{l}\right)$ also equals zero for these points,

$$2E\frac{dE}{d\left(\frac{ni}{l}\right)} = 2E_X\frac{dE_X}{d\left(\frac{ni}{l}\right)} + 2E_R\frac{dE_R}{d\left(\frac{ni}{l}\right)}$$

or

$$E_X\frac{dE_X}{d\left(\frac{ni}{l}\right)} = -E_R\frac{dE_R}{d\left(\frac{ni}{l}\right)} \tag{2}$$

For the unstable condition the above relation is satisfied by two values of current or ampere-turns per inch, while for the critical stable condition the two values of ampere-turns per inch approach a single value.

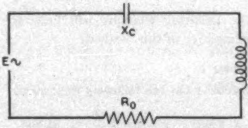

Figure 1. Ferroresonant series circuit

The Generalized Equations

It now becomes necessary to express E_X and E_R in general terms. For simplicity the following assumptions will be made:

$$E_{LR} = \frac{W}{i_1} \tag{3}$$

$$E_{LX} = \sqrt{E_L^2 - \left(\frac{W}{i_1}\right)^2} \tag{4}$$

where

W = iron losses of the reactor with sine wave of impressed voltage
i_1 = root-mean-square value of the fundamental current.

Since the reactive component of the reactor voltage is proportional to the maximum flux density, which in turn is a function of the ampere-turns per inch,

$$\begin{aligned} E_{LX} &= 4\gamma fnAB_{max} \times 10^{-8} \\ &= nA\beta \end{aligned} \tag{5}$$

where

β = $4\gamma fB_{max} \times 10^{-8}$ = a function of $\left(\frac{ni}{l}\right)$
n = number of turns on the reactor
A = cross section area of the core
γ = form factor

The in-phase component of the reactor voltage is generalized as follows.

$$\begin{aligned} E_{LR} &= nA\left\{\left(\frac{\#}{Al}\right)\frac{\left(\frac{W}{\#}\right)}{K\left(\frac{ni}{l}\right)}\right\} \\ &= nA\left\{\rho\frac{\left(\frac{W}{\#}\right)}{K\left(\frac{ni}{l}\right)}\right\} \\ &= nA\alpha \end{aligned} \tag{6}$$

where

ρ = density of iron used
$\left(\frac{W}{\#}\right)$ = watts per pound = a function of B_{max} therefore a function of $\left(\frac{ni}{l}\right)$
K = $\frac{i_1}{i}$ = ratio of the fundamental to effective current

Since β and α are single curves for any given iron when plotted against the ampere-turns per inch, the two component voltages of the reactor are completely generalized. The values of β and α may be obtained experimentally by dividing E_{LX} and E_{LR} as found from (3) and (4) by nA of the reactor; however, these curves are not necessary for the solution of the critical stable condition.

It is now possible to express E_X and E_R as follows:

$$E_X = nA\beta - \left(\frac{ni}{l}\right)\frac{X_C}{\left(\frac{n}{l}\right)} \tag{7}$$

$$E_R = nA\alpha + \left(\frac{ni}{l}\right)\frac{R_O}{\left(\frac{n}{l}\right)} \tag{8}$$

where R_O = series resistance of the cir-

Paper number 38-104, recommended by the AIEE committee on basic sciences and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted April 15, 1938; made available for preprinting July 7, 1938.

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Research work underlying this paper was conducted at the University of California under the university fellowship 1936–37.

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sketches made by an artist at the location of a news event such as a trial, football game, etc. This last would require a transcriber somewhat different from those described above in that it would have to convert the motion of the artist's stylus immediately into the proper pulsating potentials for sending the image simultaneously with the drawing of it. There are several possible means of doing this.

cuit. Substituting into equation (2)

$$\left\{nA\beta - \left(\frac{ni}{l}\right)\frac{X_C}{\left(\frac{n}{l}\right)}\right\} \times \left\{nA\frac{d\beta}{d\left(\frac{ni}{l}\right)} - \frac{X_C}{\left(\frac{n}{l}\right)}\right\} = -\left\{nA\alpha + \left(\frac{ni}{l}\right)\frac{R_O}{\left(\frac{n}{l}\right)}\right\} \times \left\{An\frac{d\alpha}{d\left(\frac{ni}{l}\right)} + \frac{R_O}{\left(\frac{n}{l}\right)}\right\}$$

Dividing through by $(nA)^2$

$$\left\{\beta - \left(\frac{ni}{l}\right)\left(\frac{X_C}{\frac{n^2A}{l}}\right)\right\} \times \left\{\frac{d\beta}{d\left(\frac{ni}{l}\right)} - \left(\frac{X_C}{\frac{n^2A}{l}}\right)\right\} = -\left\{\alpha + \left(\frac{ni}{l}\right)\left(\frac{R_O}{\frac{n^2A}{l}}\right)\right\} \times \left\{\frac{d\alpha}{d\left(\frac{ni}{l}\right)} + \left(\frac{R_O}{\frac{n^2A}{l}}\right)\right\} \quad (9)$$

Equation (9) is the generalized equation for the critical conditions of the circuit. In this β, α, $d\beta/d\left(\frac{ni}{l}\right)$ and $d\alpha/d\left(\frac{ni}{l}\right)$ have a definite value for any given value of $\left(\frac{ni}{l}\right)$, regardless of the reactor used; therefore the solution of equation (9) will depend only on the values of $X_C\Big/\frac{n^2A}{l}$ and $R_O\Big/\frac{n^2A}{l}$.

By substituting (7) and (8) in equation (1), the generalized expression for the voltage becomes

$$\left(\frac{E}{nA}\right)^2 = \left\{\beta - \left(\frac{ni}{l}\right)\left(\frac{X_C}{\frac{n^2A}{l}}\right)\right\}^2 + \left\{\alpha + \left(\frac{ni}{l}\right)\left(\frac{R_O}{\frac{n^2A}{l}}\right)\right\}^2 \quad (10)$$

The Principle of Similitude of Circuits

From equations (9) and (10) it is possible to draw certain conclusions. The following apply to the critical stable condition:

1. Two different circuits with equal values of $X_C\Big/\frac{n^2A}{l}$ will have equal values of $R_O\Big/\frac{n^2A}{l}$, and the ampere-turns per inch for the critical stable condition will be equal.

2. For the case above the values of E/nA for the two circuits will be equal.

3. Since the values of n, A, and l are generally known for any reactor, it is possible to predetermine the critical stable values of X_C, R_O, i, and E for any reactor by a very simple process.

4. The critical values of $R_O\Big/\frac{n^2A}{l}$, E/nA, and $\frac{ni}{l}$ when plotted against $X_C\Big/\frac{n^2A}{l}$ are a function only of the type of iron used. (See figure 4.)

5. From the four generalized terms above it is seen that the vector voltage triangles for two similar circuits will be similar; therefore with equal ampere-turns per inch as found under similar conditions the wave shapes of the voltage across the reactors as well as that of the current will be equal. It is then possible to state that under similar conditions in two reactors with the same grade of iron, each unit volume of iron goes through the same cycle wave shape and all.

Illustrative Example

The following example will illustrate the simplicity of this method:

PROBLEM 1

Reactor 1 has the following dimensions:

$n = 520$ $\qquad \frac{n^2A}{l} = 45{,}700$

$A = 2.62$ square inches $\qquad nA = 1{,}360$

$l = 15.5$ inches $\qquad \frac{n}{l} = 33.5$

When operated in a series circuit with $X_C = 272$ ohms, it gave the following values for the critical stable condition (see figure 2):

$R_O = 145$ ohms
$E = 224$
$i = 0.52$

What will be the corresponding critical stable values for reactor 2 with the following dimensions?

$n = 352$ $\qquad \frac{n^2A}{l} = 17{,}400$

$A = 1.72$ $\qquad nA = 605$

$l = 12.25$ $\qquad \frac{n}{l} = 28.7$

SOLUTION

$$\left(\frac{X_C}{\frac{n^2A}{l}}\right)_1 = \left(\frac{X_C}{\frac{n^2A}{l}}\right)_2$$

$$\frac{272}{45{,}700} = \frac{X_{C2}}{17{,}400} \qquad \therefore X_{C2} = 104 \text{ ohms}$$

$$\left(\frac{R_O}{\frac{n_2A}{l}}\right)_1 = \left(\frac{R_O}{\frac{n^2A}{l}}\right)_2$$

$$\frac{145}{45{,}700} = \frac{R_{O2}}{17{,}400} \qquad \therefore R_{O2} = 55.1 \text{ ohms}$$
Exp. value = 53 ohms

$$\left(\frac{E}{nA}\right)_1 = \left(\frac{E}{nA}\right)_2$$

$$\frac{224}{1{,}360} = \frac{E_2}{605} \qquad \therefore E_2 = 99.5 \text{ volts}$$
Exp. value = 100 volts

$$\left(\frac{ni}{l}\right)_1 = \left(\frac{ni}{l}\right)_2$$

$$33.5 \times 0.52 = 28.7 \times i_2$$

$$\therefore i_2 = 0.607 \text{ ampere}$$
Exp. value = 0.61 ampere

This procedure is repeated for other values of X_C. The dotted curves of figure 3 shows the critical stable values for reactor 2 predetermined from the experimental values of reactor 1. The discrepancies are due mainly to differences in the flux leakages of the two reactors.

The Generalized Curves

The critical stable condition for any series circuit can be generalized in terms of $X_C\Big/\frac{n^2A}{l}$, $R_O\Big/\frac{n^2A}{l}$, E/nA, and $\frac{ni}{l}$. Figure 4 calculated from four different reactors indicates good agreement with the theory. The tabulated values for the curves are given in table I.

From such a curve it is possible to determine the critical stable values for any reactor almost immediately. To illustrate its use a typical problem will be taken.

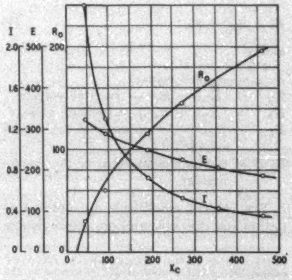

Figure 2. Critical stable values for reactor 1

PROBLEM 2

It is desired to design a voltage-sensitive circuit with a critical stable voltage of 200 volts and a critical stable current of 0.6 ampere.

This may be accomplished in a number of different ways. However, from the practical viewpoint the problem is fairly well limited. Starting with a certain value of $X_C\Big/\frac{n^2A}{l}$ the reactor dimensions and the condenser size can be determined. If these values are un-

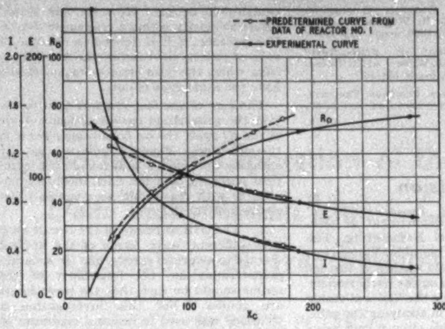

Figure 3. Critical stable values for reactor 2

practical, another value of $X_C\Big/\frac{n^2A}{l}$ is chosen and the process repeated.

Starting with $X_C\Big/\frac{n^2A}{l} = 0.010$

$$\frac{E}{nA} = 0.135 \therefore nA = \frac{200}{0.135} = 1{,}470$$

$$\frac{ni}{l} = 12 \qquad \frac{n}{l} = \frac{12}{0.6} = 20$$

Volume $= Al = \frac{1{,}470}{20} = 73.5$ cubic inches

This would result in a bulky reactor; therefore, start with a smaller value of $X_c\Big/\frac{n^2A}{l}$.

For

$$\frac{X_C}{\frac{n^2A}{l}} = 0.004$$

$$\frac{E}{nA} = 0.187 \quad \therefore nA = \frac{200}{0.187} = 1{,}070$$

$$\frac{ni}{l} = 26.5 \qquad \frac{n}{l} = \frac{26.5}{0.6} = 44.2$$

$$Al = \frac{1{,}070}{44.2} = 24.2 \text{ cubic inches}$$

$$\frac{n^2A}{l} = 44.2 \times 1{,}070 = 47{,}300$$

$X_C = 0.004 \times 47{,}300 = 190$ ohms
$C = 14$ microfarads
$R_O = 0.0023 \times 47{,}300 = 109$ ohms

If

$l = 12$ inches
$A = 2.02$ square inches
$n = 530$ turns

These values are within the practical range. If a still smaller reactor is desired, a smaller value of $X_C\Big/\frac{n^2A}{l}$ should be chosen, the size of the core being limited by the space necessary for the coils.

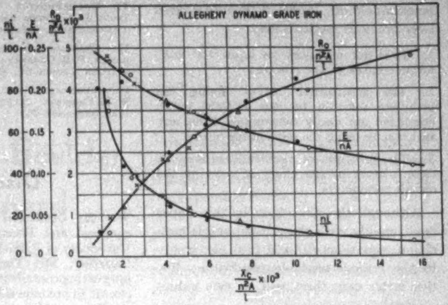

Figure 4. Generalized curves for the critical stable condition

Table I. Generalization of the Critical Stable Values

		Experimental Values X_C	R_O	E	i	$\frac{X_C}{\frac{n^2A}{l}}$	$\frac{R_O}{\frac{n^2A}{l}}$	$\frac{E}{nA}$	$\frac{ni}{l}$	
Reactor 1		43	30	323	2.4	0.00094	0.000656	0.237	80.5	
$n = 520$		93	60	284	1.3	0.00204	0.00131	0.209	43.5	$\frac{n^2A}{l} = 45{,}700$
$A = 2.62$	●	188	114	248	0.72	0.00412	0.00250	0.182	24.1	$nA = 1{,}360$
$l = 15.5$		272	145	224	0.52	0.00595	0.00317	0.165	17.4	$\frac{n}{l} = 33.5$
		355	170	205	0.43	0.00778	0.00372	0.151	14.4	
		460	195	186	0.36	0.0101	0.00426	0.137	12.1	
Reactor 2		25	10	142	2.4	0.00144	0.000575	0.234	69.0	$\frac{n^2A}{l} = 17{,}400$
$n = 352$		43	26	132	1.3	0.00247	0.00149	0.218	37.3	
$A = 1.72$	⊙	93	50	104	0.68	0.00535	0.00287	0.172	19.5	$nA = 605$
$l = 12.25$		187	68	78	0.40	0.0107	0.00390	0.129	11.5	$\frac{n}{l} = 28.7$
		272	75	66	0.25	0.0156	0.00430	0.109	7.2	
Reactor 3		93	63	290	1.3	0.00134	0.000905	0.240	74.6	$\frac{n^2A}{l} = 69{,}700$
$n = 704$	×	187	120	252	0.68	0.00268	0.00173	0.208	39.0	$nA = 1{,}210$
$A = 1.72$		272	164	230	0.50	0.0039	0.00235	0.190	28.7	
$l = 12.25$		355	182	209	0.40	0.0051	0.00261	0.173	23.0	$\frac{n}{l} = 57.4$
Reactor 4		93	56	252	1.13	0.00202	0.00122	0.223	46.1	$\frac{n^2A}{l} = 46{,}000$
$n = 500$		187	107	212	0.62	0.00406	0.00232	0.188	25.3	$nA = 1{,}125$
$A = 2.25$	△	272	139	188	0.48	0.00590	0.00302	0.167	19.6	
$l = 12.25$		339	158	172	0.40	0.00738	0.00344	0.153	16.3	$\frac{n}{l} = 40.8$

Equivalent Series Circuits

The generalized treatment so far has dealt only with the series circuit. However, the same principle can be used for other circuits by reducing them to the equivalent series circuit.

One practical application of such a scheme is found in the design of the resonant relay circuits. By shunting a high-impedance relay across the condenser of the ferroresonant circuit, relays which normally release at half the closing voltage can be made to open and close at the same voltage when the circuit is tuned for the critical stable condition.

In order to make use of the generalized chart it is now necessary to use the equivalent series reactance and resistance of the circuit. For a relay across the condenser the equivalent series values reduce to a capacity reactance of

$$X_C' = X_C\left\{\frac{r^2 + X_R(X_R - X_C)}{r^2 + (X_R - X_C)^2}\right\} \qquad (11)$$

and a resistance of

$$R' = r\left\{\frac{X_C^2}{r^2 + (X_R - X_C)^2}\right\} \qquad (12)$$

where

X_C = reactance of the condenser used
r = resistance of the relay
X_R = average reactance of the relay

Conclusion

The principle of similitude offers the simplest and the most practical solution for the critical conditions of the ferroresonant circuit. Some of the advantages over previous methods are:

1. It is not necessary to know the volt-ampere curve of the reactor.

2. The generalized curves giving the complete solution for the critical stable condition are easily determined from any test reactor using the same grade of iron.

3. The principle of similitude presents a clear picture of the effect of varying the circuit parameters.

Although the discussion in this paper dealt mainly with the critical stable condition, equations (9) and (10) also apply to the critical unstable condition. For this latter case there will be two values of $\left(\frac{ni}{l}\right)$, and they will be completely specified by the values of $X_C \Big/ \frac{n^2A}{l}$ and $R_0 \Big/ \frac{n^2A}{l}$.

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Discussion

W. B. Coulthard (Department of Mechanical and Electrical Engineering, The University of British Columbia, Vancouver, Canada): Mr. Thomson makes a further interesting contribution on the ferroresonant circuit in predetermining the limits of stable conditions. However, in applying the principle of similitude to this circuit, care must be exercised.

My criticisms of this method are as follows:

1. The dimensions of the coils and cores must ensure proportionality between the leakage and iron flux.

2. The impedance of the source of supply must be considered. It is well known that different points of instability are obtained when the voltage is varied by controlling the excitation of the alternator and when it is varied by induction regulator control. So to apply the principle of similitude, identical supply circuits must be specified.

3. When reactance preponderates in the supply circuit, due to the phase shift the point of instability does not occur at the peak and dip of the *EI* characteristic but at some points beyond. Instability is now no longer defined by $\frac{dE}{dI} = 0$ but by $\frac{dE}{dI}$ = a negative quantity. Under critical stable conditions it appears that the *EI* characteristic is no longer parallel to the current axis, but there is a slight bend between what are defined as the low- and high-circuit regions. In these circumstances $\frac{dE}{dI} = 0$ does not hold.

W. T. Thomson: (1) In order to apply the principle of similitude to any physical problem, it is obvious that the two systems to be compared must be proportional in every way. The accuracy of predetermination will therefore depend on the degree to which this requirement is satisfied.

In ferroresonant circuits it would be impracticable to attempt perfect proportionality in design. However, one should insure similarity within a reasonable degree. For instance, if it is desired to use the *EI* type of core in the reactor, the test reactor from which the data must be taken, should have the same type of core.

The core of the four reactors used to obtain the generalized curves of figure 4 are of the *EI* type, the a-c coils being placed on the outer legs. Figure 4 indicates that the accuracy obtained is sufficient for all practical purposes, and the data should be applicable with reasonable accuracy to other reactors of the *EI* type.

2. The critical points of the circuit vary with different wave forms of the applied voltage to a certain extent, and for accurate predetermination, the impedance of the source should be such that the wave forms are similar. For this investigation a "Variac" was used to insure a minimum of wave distortion.

3. The critical stable condition was defined as the limiting condition of the equation $\frac{dE}{dI} = 0$, and $\frac{dE}{dI}$ equal to a negative quantity does not come under the case of critical stable operation.

For the unstable condition $\frac{dE}{dI} = 0$ will still be points of major interest regardless of whether they represent points of instability or not. With a supply of good regulation $\frac{dE}{dI} = 0$, will represent limiting points of instability.

In general by using reasonable care in insuring proportionality, the accuracy obtained will be sufficient for all practical purposes. It should also be pointed out that up to the present there has been no publication of any other method by which it is possible to predetermine accurately the critical points of ferroresonant circuits in a generalized manner. In articles dealing with ferroresonance, the problem has been turned around backwards, e.g., to calculate the critical points for a given circuit, while the information most desired is of the reverse order; i.e., to be able to start from some specified critical point and design the circuit to give this performance. The information enabling this predetermination must necessarily be of the generalized order such as the one discussed in this article.

Symposium on Operation of the Boulder Dam Transmission Line—General Operation of Transmission Line

By WM. S. PETERSON
MEMBER AIEE

Synopsis: The successful operating experience had during the first $1^1/_2$ years with the Boulder Dam-Los Angeles transmission lines of the Department of Water and Power is reviewed in a symposium of five papers. Two unusually severe winter seasons and two lightning seasons, one of which was largely during the construction period, have served to test the line, so that an accurate judgment of its future performance can be made. An historical summary of the use of the line during permanent installation and adjustment of auxiliary equipment and through the period of normal operation to the present time is presented. The manner in which the electrical and mechanical performance has proved the design is developed and the unforeseen problems requiring solution are described. In addition to this general discussion, the excellent performance of the conductor from the standpoint of corona, the outstanding freedom from lightning flashover, are shown in two separate papers. Two additional papers describe the carrier-current supervisory and communication systems and the relay protection system as well as fully discussing the experience had in placing this equipment in adjustment and having successful operation.

THE Boulder Dam transmission line of the Department of Water and Power of the City of Los Angeles, has been in service since October 1936. By virtue of the vagaries of climate, the line has been called upon to withstand lightning, wind, ice, and floods of unusual severity, subjecting the line to many of the extreme conditions for which it was designed, without having to wait the usual long term of years for such conditions to occur.

This circumstance makes it particularly appropriate to review the performance of the line under such operating conditions, for a satisfactory judgment on its long-time performance can thus be made. To present this material together with any special or new problems that have required solution is the purpose of this symposium. It has been found advisable to present this material under several appropriate headings dealing with the mechanical and electrical characteristics of the line, corona, insulation and lightning protection, carrier-current equipment, and relay protection. Where the original description of certain features has not been given heretofore, as for carrier equipment and relays, they are included in these papers.

This line, operating at 287.5 kv, consists of two circuits, each 266.5 miles long, carried on two single-circuit steel-tower lines, except for 40.5 miles carried on double-circuit towers near Los Angeles. It includes such unusual features as 1.4-inch-diameter hollow copper conductor of the segmental type; special suspension clamps; high level of insulation provided by suspension insulator strings consisting of 24 ten-inch-diameter by five-inch-pitch porcelain units; two overhead ground wires, 30 to 40 feet above the conductors; two continuous buried ground wires or counterpoise wires per tower line, with interconnections between tower lines where single-circuit construction is used. Two intermediate sectionalizing switching stations are located at one-third points known as Silver Lake and Victorville stations. Operating as part of the receiving end line sections are two banks of autotransformers reducing the voltage from 275 to 132 kv at Century receiving station (station *B*).

The complete general description of the line and discussion of its various engineering features have been given in a paper entitled "Engineering Features of the Boulder Dam Transmission Line" by E. F. Scattergood,[1] in which paper, as well as in a companion paper,[2] there was indicated the unusual and urgent necessity for this line to be built so as to achieve the utmost reliability. With this object in view, liberal safety factors were employed, in its mechanical and electrical design, and a new step in high voltage was taken, together with the use of the two intermediate sectionalizing stations so that it could carry its normal rated load of 240,000 kw delivered, with unimpaired stability when subjected to two phase to ground faults, removed by dropping a section of circuit. This operating capacity was taken as 80 per cent of the theoretical limit, which was cal-

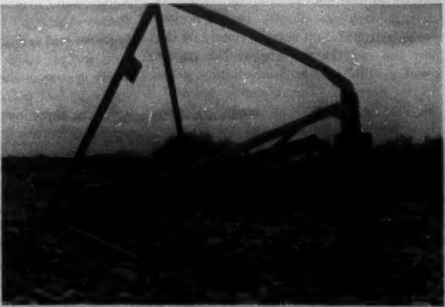

Figure 1. Remains of double-circuit tower after flood

Paper number 38-109, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 14, 1938; made available for preprinting July 8, 1938.

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1. For all numbered references, see list at end of paper.

culated without including all of the system of the Department and is therefore also conservative. Factors in obtaining this performance were generators of liberal characteristics, and exceedingly rapid relays and circuit breakers, which would clear faults in approximately 0.1 second or less. The line has an emergency capacity of 300,000 kw delivered, a limit imposed by generator capacity.

The reliability that was built into the system was of outstanding importance when the system went into service. The necessity of making a frequency change cut-over from 50 to 60 cycles at the same time as the line was inaugurated, made the demand for its use so great that essentially the only test the new Boulder system received was the test of service. The very instant that the first Boulder power house generator was available, the transmission system went into service and has continued to give the type of performance anticipated, despite the extreme mechanical loading imposed by a winter remarkable for the worst cold, snow, sleet, and wind recorded during the history of the local weather bureau; followed, a year later, by the thorough test on line location and structural strength, given by a winter having the most severe floods within over 50 years.

Preliminary Use of Line

The first application of normal voltage to a section of the line occurred on September 20, 1936, when a 90-mile section of one circuit from Los Angeles Century receiving station to Victorville switching station, was excited from the receiving end. This use of the line was made in connection with testing out and developing a neon-tube tower light obtaining its power from capacitive coupling with the line conductors.

The characteristics of the available equipment and transmission lines included in the circuit, and the resultant charging-current effects, made it impossible to build up voltage slowly on the new line, so that on closing the circuit breaker, the voltage immediately came up to normal. The charging kilovolt-amperes was 28,600 as predicted from the calculation of line and transformer constants. No difficulty was experienced with the line or the various circuit breakers and disconnects which were tested at the same time.

Frequency-Change Considerations

A consideration of consumer's interests had indicated the desirability of changing the frequency on the Bureau of Power and Light system from 50 cycles to the 60-cycle standard used throughout the country. The bringing in of a relatively large new source of power made it desirable and economical to make the change-over when such power was available. Early estimates of generator availability had indicated that the frequency change could start in April 1936, and an organization was built up on that basis. Delays from that date were costly but necessary.

As the first Boulder generator unit did not finally become reliably available until October 22 and the second generator not until November 14, it becomes evident that the transmission system had to go into effective operation immediately as the generators became available.

Operation for Inauguration

In accordance with anticipated availability of generator units, appropriate ceremonies pertaining to the arrival of Boulder Dam power in Los Angeles were set for October 9, 1936. Broadcasting and other program arrangements required adherence to this date. From the engineering standpoint this inaugural use of the line was important because it was the original tryout. Due to the co-operation of the government and the manufacturer, a generator was made available for that event, although it was not in condition to be released for regular service until nearly two weeks later.

Temporary arrangements of relay equipment and carrier telephone equipment were installed. Both power circuits were available but only one was used in order to avoid self-excitation of the generator.

The program included the use of a large arc to announce the arrival of Boulder power. Line and system arrangements were such that power from Boulder Dam would come over the south circuit, and thence through various system connections and series reactance to a 2,300-volt feeder to a downtown location, where arrangements were provided at the top of 70-foot poles for producing an arc between three-inch-diameter electrodes, arranged vertically, and separated approximately three feet. At the arc the voltage was approximately 1,000 volts, the current was 500 amperes, and the power consumed was 500 kw.

At 2:08$^1/_2$ p.m. October 9, the line was energized to the receiving station at low voltage and reduced frequency. No synchronous condensers were in use. Within three-quarters of an hour, the speed had been brought up to normal and the voltage on the generators was 75 per cent of normal in order to produce normal voltage at the receiving end. The charging kilovolt-amperes was approximately 75,000 which was quite close to the calculated value for lines and transformers.

This was the first full-speed operation of the unit, and a generator bearing began to run hot, so it was necessary to run at reduced speed and voltage from 3 p.m. until required to bring conditions back to normal in advance of striking the large arc at 7:41 p.m.

Other than the difficulty with the generator bearing, no other trouble was experienced in this first test and use of the complete line.

Further Installation and Test Work

During the following two weeks the relay and carrier telephone installation work was completed and initial adjustments made. Changes in the generator bearing to increase the flow of oil, overspeed tests, and installation and initial adjustments of the generator and transformer differential relay system and other auxiliaries were made. The installation of carrier-current supervisory-control system was under way, but was not completed until a later date.

During the relay installation, the line was charged on several instances for adjustment of potential devices used in connection with the relay system. No difficulties were encountered. It was anticipated that further test and observation and adjustment of the relay system would have to be made during operation. Final adjustments of the telephone system could best be made with the line in operation, so that suitable adjustments of input with respect to the noise level could be made.

Commercial Operation

Actual use of the line for regular power service began at 7:05 a.m., October 22, 1936, using the south circuit and one generator. Low head in the reservoir limited the generator output to approximately 50,000 kw at full gate. With loads as low as this, it was possible to take the line out of service between midnight and morning whenever desired for adjustment or installation of auxiliary equipment. At the switching stations such work involved permanent installation and adjustment of relay equipment and carrier-current telephone equipment.

Adjustment of pallet switches and other small elements of control were made. The installation work pertained principally to the supervisory control equipment. The line patrolmen took advantage of such opportunities to take out line sections to replace some insulator units slightly damaged by bullets or in course of construction work, and make any other corrections to hardware revealed as necessary by the careful inspection which was being made.

The second generator, unit *N*-4, became available December 28. Both units were put into service immediately at full output with the available head. The bringing in of these units at this time was particularly important in avoiding delay in the program of frequency changing under way. The Boulder Dam system was depended on, and used as a fully dependable system during this exacting time, when keeping adequate reserve for two frequencies was a major problem.

It may be said that these early months of operation constituted the test period for all of this relay, supervisory control, and communication equipment. Observations and tests have been continued to serve as a guide in the proper adjustment of the carrier current equipment and point the way to minor changes of design that may be necessary in any of the elements of the equipment, to meet the requirements, set by the Department, to operate under certain defined unusual circumstances.

By the end of the year 1936 the frequency-change work had been essentially completed, and the system was proceeding on a normal basis. The fourth generator became available March 18, 1937. In general the plant has been loaded up to the limitations imposed by low head until after the runoff of 1937. Since then, the maximum load has been essentially limited by the defined rated operating capacity of the transmission system. Since the Department acquired the system of the Los Angeles Gas and Electric Corporation on February 1, 1937, it became desirable from the standpoint of economy to load the Boulder system as much as possible and use the acquired steam plants for standby. The rapid growth of load on the Boulder system is indicated in table I, showing the kilowatt-hours per month supplied to the low-voltage side of the step-up transformers, the monthly peak output of the main generators in kilowatts, and load factor. In addition the average monthly efficiency of transmission is given. This figure includes losses in the step-up transformers, line, autotransformers, and synchronous condensers. One other transformation from 132 kv to the 33-kv load bus is not included.

During this period of operation, the personnel have had to acquaint themselves; with new types of intricate control equipment, a large and complicated generating plant, a new line arrangement at switching stations, and a line of unusual characteristics and length, and continue operation while extensive installation and test work was going on with such equipment. Under the conditions it is remarkable and the operating group are to be commended on the fact, that a large number of interruptions did not occur from the human element. The operation of the line has been exceedingly reliable and satisfactory. There was a period of ten months operation absolutely free from interruption due to any cause.

Of the five interruptions that have occurred, four have involved the human element as well as the control equipment, so that it cannot be stated beyond all doubt wherein the failure occurred. Such interruptions occurred during operating procedure. The other interruption was caused by a flood undermining a tower and causing relay action which took place correctly. A brief description of each of these cases will be given.

Interruptions

The first interruption was of very minor importance and occurred at 6:21 a.m., December 15, 1936, during the process of putting the north line back in service after being out on a clearance. The north line was energized for the complete length and had been placed in parallel at Silver Lake and at Boulder. While closing the parallel at Victorville, some faulty action of undetermined origin caused the south line to relay open and interrupt service. The line was carrying 50,000 kw.

The second interruption occurred at 9:11 p.m., January 6, 1937, when the line was delivering 130,000 kw. Sleet had been reported as forming on the conductors at Mountain Pass between Boulder and the Silver Lake switching station. The patrol road was becoming impassable, so it was decided to alternate the load on the north and south line sections between these stations. As will be indicated in the paper on relaying, transients during switching operations had been such as sometimes cause relay action to occur on the incoming line. The north line section had been energized from Boulder and in the process of placing it in parallel with the south line it relayed. Then either the supervisory control indication did not come in correctly, or the Boulder operator did not notice, or else did not wait long enough for the supervisory signal to come in before tripping the south line and interrupting service. Due to the line being charged from the receiving end high voltage existed for one or two seconds until the operator opened the lines so Boulder could get the transmission line ready for resynchronizing.

The third case of interruption did not occur for nearly a year and happened at 10:25 a.m., November 8, 1937, while the relay maintenance crew were making their routine tests on the performance of the relay equipment. In clearing the balanced line, to put them back in serv-

Table I

Year	Month	Energy to Step-up Transformers (Millions of Kilowatt-Hours)	Generator Peak Kilowatts	Load Factor (Per Cent)	Efficiency of Transmission (Per Cent)
1936	October*	6.20	55,000	48.0	86.9
	November	46.70	108,000	60.1	90.4
	December	67.20	162,000	55.8	91.3
1937	January	80.50	156,000	69.4	93.3
	February	71.50	152,000	70.1	92.5
	March	74.20	153,000	65.3	92.0
	April]	76.40	171,000	62.1	92.3
	May	76.60	167,000	61.7	94.7
	June	79.30	182,000	60.5	95.0
	July	81.60	166,000	66.1	94.9
	August	85.60	216,000	53.3	94.1
	September	99.00	250,000	55.0	93.1
	October	119.10	276,000	58.0	93.6
	November	117.70	277,000	59.0	93.8
	December	123.33	277,000	59.9	93.7
1938	January	120.40	281,000	57.7	93.8
	February	116.60	273,000	63.5	93.8
	March	111.97	267,000	56.4	93.6
	April	111.06			93.8

* 235 hours.

ice, some jumpers were inadvertently left so as to involve a complication of several circuits on a relay test panel, in such a way as to have the effect of connecting the secondary of the potential transformer through the residual current coil of the carrier pilot relays. Also due to the short circuit on the potential transformer, voltage on the directional element was such as to cause the relays to indicate a fault on both lines. When the relay test switch was turned to put the relays in operation both lines were tripped.

At the time of this interruption Boulder power plant was carrying 217,000 kw. It is estimated that the maximum receiving-end voltage of the lines was approximately 385 kv at 4.5 seconds after the interruption and that the maximum frequency was 65 cycles per second 5 seconds after the lines opened. Such values are not considered excessive for dropping this much load. These conditions caused no flashovers of line insulation nor difficulty in the autotransformers connected to the lines.

Figure 2. First tower east of washed-out tower

The fourth interruption occurred at 6:56 p.m., March 2, 1938, as a result of the undermining of a double-circuit tower by the worst flood of 50 years record in the Los Angeles area. The actual failure began several minutes earlier. At 6:50 p.m., the north circuit relayed out, by balanced line relays, due to a single-phase-to-ground fault on the *A* phase (top conductor). Since the overhead ground wire broke free from the tower on this side, it is believed that this first relay action was caused by the ground wire sagging down on the conductor below. The complete relay and circuit-breaker action required only 3.5 cycles or 0.06 seconds. This is as designed and the circuit-breaker action was without distress. Boulder was carrying 170,000 kw at the time of fault. System voltage drop was scarcely noticeable, and very small power swing took place, the maximum at Boulder being 177,000 kw. Since the operator knew what was taking place, load was then reduced on the Boulder circuit.

Six minutes later the tower completed its process of falling and the bottom conductor of the south line (also *A* phase) contacted ground and was relayed by carrier pilot relays, in $5^1/_2$ cycles at the receiving end and $10^1/_2$ cycles at the Victorville end. The load being carried was 130,000 kw.

This second fault was less severe than the previous one, as very considerable less ground current flowed indicating considerable resistance at the point of fault. The relay time was compatible with the ground currents involved.

The generators speeded up to about 63 cycles 4.6 seconds after the fault. The sending-end voltage did not rise appreciably.

The last interruption occurred at 10:16 a.m., March 29, 1938 It was an illustration of what can happen due to complexity of circuits in a modern power house, where doing some simple but unusual operation brings into action other equipment, possibly out of mind at the time. In this case, while gathering nameplate data for inventory purposes, the circuit breakers for supplying d-c control power over two feeders, were being alternately taken out of service. In this process, the second breaker was opened before the first was closed, removing the control power. Then all normally energized control relays dropped out, establishing a condition which energized the governor shut-down solenoids when the bus was re-energized. This caused all turbine gates to move to the full closed position, where all but one unit latched shut. On this one unit the gates came back to the full open position, but as all of its output was used up in driving the other units and churning water, no power reached Los Angeles, and the Boulder circuits were opened by hand to permit Boulder to clear up its trouble and get the line ready for synchronizing. The load on the machines at Boulder at the time was 230,000 kw.

The Boulder speed dropped to 45 cycles about 30 seconds after separation. The regulators were set to maintain 17,000 volts at the generator. The units were then put on hand control, and gates opened to bring the speed up. Due to no load, they came up more rapidly in speed than the regulator could adjust voltage, considering the heavy field current and the effects of line-charging current with increasing speeds. The leading current also acted on the cross compensation of the voltage regulators, causing them to regulate seven per cent higher than their setting. The maximum voltage at Boulder was approximately 385 kv. The receiving-end voltage was not appreciably different because of the very large lagging exciting current taken by the autotransformers at the receiving end, with high voltage.

Electrical Loading and Performance

Due to the continuous use that has been made of this line there has never been opportunity to conduct any program of accurate testing to determine the line characteristics. However, with open-circuit conditions and normal voltage at the receiving end, switchboard readings were taken of current and voltage on the high-voltage side at Boulder. A comparison of these results with values determined by transmission system constants calculated with slide rule, for the same complete circuit, revealed that the calculated sending-end voltage was 238,000 while the test average was 239,000 and the calculated current was 200 amperes while the test average was 194. Such constants were determined on the simple isolated circuit basis, without refinements including ground wire effects, or height above the ground, etc. This is sufficient check to give assurance that the circuit is operating about as anticipated. The general performance under load is also as expected from calculation of performance.

In the design work, the assumption was made that the generator terminal voltage would in general be held constant at the normal value, with a fixed setting of the voltage regulator. In actual operation a slightly different policy has been followed to date. The regulator setting is changed with the load, ranging from 90 per cent voltage at very light loads up to 105 per cent at the higher loads. This gives operation in accordance with the criterion for minimum line loss, and also permits the synchronous condensers to operate at lower outputs.

Figure 3. Towers west of washed-out tower

The high efficiency of the line is indicated in table I.

Operation of the line has as yet developed no conditions which provide a full check on the stability limit. However, some verification of design can be obtained from the clearing of the first circuit. During the trouble of March 2, 1938, when a tower was taken out by flood at a time when the load was 170,000 kw, the relay and circuit-breaker action was so prompt that only a single-phase-to-ground fault resulted. However had slower circuit breakers been used the fault would presumably have had time to develop into a two-phase-to-ground or three-phase fault. This proved to be the case when a very similar tower failure took place on the Department's 110-kv double-circuit line, where an *A*-phase-to-ground fault developed into a two-phase-to-ground fault within 3 cycles and in 13 additional cycles became a three-phase fault. For a three-phase fault at this load the more permissible duration of fault to avoid instability is of the order of one-third second. If the customary speeds of fault removal, such as 20 to 30 cycles had been used, this fault would probably have produced instability. As it happened, there was hardly an observable flicker of voltage and hardly any power swinging. Operation was perfect until six minutes later when the tower had fallen so as to involve the second circuit.

Mechanical Characteristics and Loading

CONDUCTOR

All of the line carried on single-circuit towers is designed to carry one-half inch of radial ice loading with a wind loading of eight pounds per square foot of projected area, with a safety factor of $2\frac{1}{2}$ on the ultimate strength. The nearest approach the line will probably have to these loadings occurred during the winter of 1936–37.

The first evidence of sleet formation on the line occurred during December 1936 when a few spans at the top of Cajon Pass were observed to have one-half to three-fourths inch of ice measured radially in the vertical plane, but having very little thickness on the sides. At this time the wind had a velocity of approximately 30 miles per hour. Similar conditions existed at another mountain pass between Boulder and Silver Lake stations. The conductor completely recovered to its original normal sag after the loading was removed.

The sleet formation in Cajon Pass was watched with considerable interest as an effort was made in locating the line to choose proper elevations, and the appropriate slope of the mountains, so that sleet difficulties would be minimized. The experience of some other lines in this area had been that sleet and snow has formed up to diameters in excess of eight or ten inches and broken down conductors and structures. In the storm under discussion some lines had such difficulties. It is now believed that the route selected is probably a good choice.

During the period of construction and operation, several periods of relatively high wind velocity, probably up to approximately 50 miles per hour, have been experienced without giving any trouble. This is considerably below velocities that would give mechanical trouble. The design of clearances is based on a wind velocity of 70 miles per hour without ice so there was no real danger of flashover at lower velocities.

During the year beginning September 1935, while construction work was in progress a survey of line vibration was carried on, in which 71 locations likely to be favorable to producing conductor vibration were investigated, by using a recorder employing an inertia arm. If any appreciable vibration was indicated by this instrument, a smoked-chart recorder was put in place to measure amplitude. Such were installed in 44 of the above locations.

Approximately 18 miles in scattered locations or seven per cent of the total line, gave evidence of vibration in excess of one-half inch double amplitude at a point six feet out from the clamp, and in excess of one per cent of the time during which wind was favorable for vibration. In only one case was this amplitude maintained for as long as five per cent of the time that the wind was favorable for vibration. At this same location the amplitude reached the maximum of any place on the line, which was only $1\frac{1}{8}$ inches, double amplitude. This highest amplitude only prevailed for 0.01 per cent of the time that wind was favorable for vibration.

Based on the recorded data, none of the locations had vibration of sufficient extent to lead to fatigue failure within a 50-year period. This knowledge is based on fatigue studies on the conductor used. However, in order to be conservative and make allowances for differences that might exist due to the relatively short time of survey at each location, it was decided to equip the 18 miles mentioned above with dampers.

The studies by the Department have served to indicate that the conductor being used has a high degree of self-damping, and is entirely satisfactory in its performance in resisting fatigue failure.

HARDWARE

The free-center-type of suspension clamp used on this line has operated very successfully. Although the cable lies in pivoted saddles without clamping, there has been no creep. The knife-edge pivots used have shown no wear where examined, and no wear was indicated for a clamp carrying normal load in a vibration fatigue test on the cable. During the recent tower failure due to the flood, the center wedge grip engaged the body of the clamp and held dead end strains on the cable as anticipated.

The wedge-grip strain clamps and conductor connectors have proved entirely satisfactory. One refinement however was necessary and became evident at the start of the stringing operations. Conductor stringing was started with one crew in the Kingston Valley section of the line on March 15, 1935, and this was followed by a crew in the Silver Lake section on March 21, 1935. The first span of cable erected at Silver Lake pulled out of the dead-end fittings during the erection process, and this was followed by a similar occurrence at Kingston a few days later. We had previously broken, in laboratory tension tests, several hundred pieces of this cable when dead-ended in these same fittings and in every case the dead-end fittings functioned perfectly. However, the cause of the trouble was found to be in the design of the inner wedge or chuck of the dead-end fittings. The chuck was not flexible enough to accommodate certain combinations of manufacturing dimensional tolerances of cable and dead-end fittings. All chucks were additionally slotted to make them more flexible and no additional trouble was experienced.

In connection with the discussion of hardware it is of interest to call attention to a special unforeseen condition that exists along about seven miles of line. At this point the line is proceeding in a westerly direction after going through Cajon Pass. Here the line is running in the foothills parallel to the axis of the mountains, but there are frequent small canyons at right angles to the direction of the line. The prevailing winds, which are frequent and of moderately high velocity, are from the north and flow down these little canyons with varying velocity in adjacent canyons. The result is that adjacent spans of the line have unequal and continually shifting wind loading, which produces considerable longitudinal motion as well as the normally anticipated transverse swing of the insulator strings.

Due to this action, the commonly used bent-rod type of hanger bracket, which has a hooked end carried by holes in clip angles attached to the cross arms, is causing some wear at the points of support. An improved type of hanger, devised so as to give rolling action rather than sliding action, is being designed for use at this location. For the remainder of the line this hanger has given no such difficulty.

It was also observed with this type of hanger, that in comparison with structural-steel or plate type of hangers, that there was more tendency for the clevis pins to move endways and eventually bring the cotter key against the clevis where it could wear. The exact cause of the action is not known, but may be due to the large eye and the curved surface on which the pin rests, coupled with the larger motion with the wind that comes with the use of the hollow conductor of light weight. To eliminate any worry of failure due to this cause the pins were replaced with bolts and slotted nuts with cotter key to lock the nut.

The seven-mile section here referred to had the same type of longitudinal motion take place on the overhead ground wire, causing the clamp to swing 45 degrees. The result was excess wear on the trunnion pivots of the suspension clamps at such locations. At the present time, designs are being made for special clamps, to withstand this kind of action. This type of wear is not found elsewhere on the line.

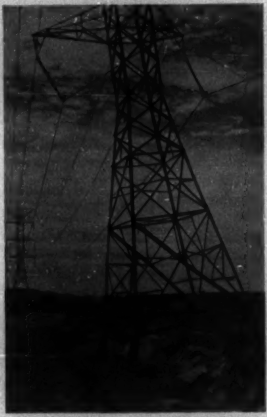

Figure 4. Tower after two footings were lowered five feet by washout

The special ground-wire strain clamp designed for this line has been satisfactory throughout the line.

TOWERS AND FOOTINGS

The satisfactory design of the towers and footings can best be indicated by a discussion of how these structures withstood the flood conditions that prevailed on March 2, 1938. Although one tower was lost, as has been indicated, there were many more at other locations that were in jeopardy and which with less liberal design, especially of footings, would have failed.

Floods in this area involve many short streams, normally carrying little or no water, which originate in the mountains, come down steep canyons, and in the case of the larger streams carry probably 50,000 cubic feet per second. The velocities even through the coastal plain are exceedingly high and very destructive. This flood was the worst in over 50 years. It was exceedingly widespread in its damage, affecting disastrously a large number of power lines, communication lines, railroads, highways, pipe lines, bridges, and many other important structures.

The tower that was undermined was in the wash area, but to one side of the established channel. The destructive power of the water can be judged from figure 1 showing a picture of the one remaining footing, with the tower visible approximately 200 feet downstream where it was carried by the force of the water. Obviously a failure of this kind put tremendous strains on the conductors and the adjacent suspension towers. In this failure the north ground-wire clamp broke free from the tower and the bottom conductor on the north side broke free at the bottom insulator. The other conductors were broken, by the pulls imposed by the falling tower.

The tower shown in figure 2, is immediately to the east of the tower that went out and shows that all arms and insulators remained intact. The insulators have a strength just under the broken wire pull that the cross arms can withstand. This tower received some help from a dead-end tower just beyond. On the west end, however, all the towers were suspension towers. The resulting strains on the conductor were such as to break insulator strings from one to three towers back depending on the conditions of pull. This is shown in figure 3. The towers were not damaged although they had to meet a duty approaching that on a strain tower. They are designed to withstand one broken conductor and one broken ground wire under maximum ice and wind conditions.

At another point in a rather indistinct water channel, a tower had all four footings completely exposed, and remained standing, due to the dead weight of the concrete footings. In another case, an angle tower, which was a considerable distance back from a water channel, had two of its footings undermined by the progressive cutting back of the bank. This tower is shown in figure 4. The footings were lowered about five feet but the weight of the footings prevented the tower from being pulled over by the transverse pull due to the angle.

These cases are representative of several others, where the sturdiness of the towers and the weight of the footings served to limit our outage to one location on the Boulder lines.

However, although this flood with its previously referred to widespread damage, did create a few points of danger and the actual loss of one tower, nevertheless to a very large degree the correctness of the location of the line and the placing of the structures is verified by the tremendous percentage of the line that escaped without damage.

Conclusion

The performance of other elements of the system will be discussed in appropriate papers associated in this symposium.

In conclusion it can be stated that the performance of the transmission line and its associated equipment has been satisfactory. The few difficulties that have been discussed are not to be regarded as materially detracting from the success of the line as a whole, but are mentioned so that the electrical industry might profit from a knowledge of their existence.

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Discussion

For discussion see page 156.

Symposium on Operation of the Boulder Dam Transmission Line—Corona Experience on Transmission Line

By BRADLEY COZZENS
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THE original basis for the selection of 1.4 inches as the diameter of the conductor for the Boulder Dam transmission line was provided principally by test data taken at the Ryan High Voltage Laboratory, at Stanford University, by the Department of Water and Power in cooperation with Doctor Joseph S. Carroll, on conductors ranging from 1.125 inches to 2 inches in diameter.[1] Also reference was made to other previous corona work at the same place.[2,3,4] An analysis and use of the data,[5,6] taking into account temperature and altitude effect and making some allowance for unknown factors affecting corona laws that had appeared in many of the tests, led to the final recommendation of 1.4 inches as the diameter for the conductor for 287.5 kv.

Cost curves for the line in terms of conductor diameter indicated that in the vicinity of minimum cost the curves were exceedingly flat, with about one per cent change from the minimum cost representing a diameter difference of 0.1 inch. Due to the peculiar shape of the corona loss curve, such economic choices would occur at the knee of the curve. However, the test work at Stanford had revealed that there were variations in corona loss that could not be accounted for by known temperature or barometric variations or other partially known factors such as humidity or surface conditions. The general indication was that after stable surface conditions had developed such factors would not cause a shift in the loss curve of more than ten per cent in voltage for the same loss. Realizing that if a conductor was operating near the knee of the curve and that this much shift in the loss curve of the conductor might run the losses tremendously and uneconomically high, it was decided to be somewhat liberal in selecting the conductor diameter. The excess diameter over that apparently required was taken as approximately 0.1 inch.

Tests on 1.65-inch-diameter hollow copper conductor of the segmental type, referred to as type *HH*, had indicated somewhat lower losses for that form of cable as compared with what would be expected from conductors with a stranded surface. It was believed, however, that a stranded cable of the diameter selected would be satisfactory for use on the line. Due to the small knowledge at the time on this subject of surface contour, no distinction was made for the difference in corona loss for these cables, in preparing specifications.

Later tests on various 1.4-inch-diameter cables, which were submitted by manufacturers in response to specifications, demonstrated the lower losses to be obtained with the smooth surface segmental type,[7] which was the conductor used on the line. This conductor also had economic advantage in that it could be made with the exact amount of copper necessary to carry the mechanical and electrical loading involved.

From the corona standpoint, the tests on 1.4-inch-diameter conductors indicated that any of them should be satisfactory, and that the 1.4-inch-diameter segmental-type conductor would be liberal as the losses were quite small, providing the surface conditions existing in the laboratory tests would be maintained in the field, by care in erection.

In addition to selecting a conductor size that would give low corona losses, a special effort was also made to design line hardware so as practically to eliminate unshielded corners or small radii that would be conducive to the formation of corona. The suspension clamp is essentially a box-shaped shell of smooth exterior with very liberal radii of curvature for all edges and corners. The strain clamps and connectors are designed so as to have rounded edges and dimensions that will make them corona free. In addition the use of $1^1/_2$-inch pipe for arcing horn structures at the high voltage end of insulator strings gives adequate shielding to hardware parts and the lower insulators to avoid corona formation at such points, as well as eliminating corona from the horn itself. In addition the long insulator strings used further reduced the duty on insulators to a point where corona formation at the insulators is practically eliminated unless disturbed by dirt and fog or other contamination.

Noise Observations

Dry Weather Corona

After a line is in operation, a great deal of the judgment, concerning the performance of conductors with respect to corona, must be based on noise observation. This comes from the fact that, in a long line with low corona losses, it is very difficult to make any test arrangement whereby corona losses can be accurately determined. Such work involves not only the problems of accurate measurement of power at low power factor and high voltage, but involves subtraction of copper losses, affected by tem-

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Paper number 38-110, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 18, 1938; made available for preprinting June 27, 1938.

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[1] For all numbered references, see list at end of paper.

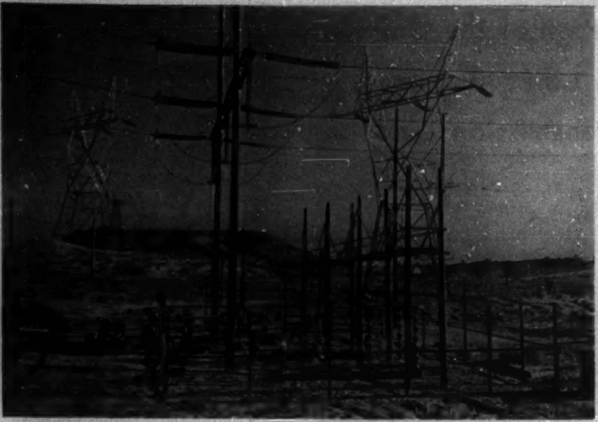

Figure 1. The test line and high-voltage wattmeter arrangements for Victorville corona tests

The steel towers in the background are part of the main transmission lines

perature at various points and the variation in the charging current along the line. In order to maintain proper voltages at each end of the line, in accordance with those existing during normal operation, synchronous condenser operation is necessary and determination of such losses must also be made. The interpretation of the results is also difficult due to the variation in elevation and temperature along the line.

When 90 miles of the line was first energized from the Los Angeles end, observations taken near the receiving station indicated a corona noise of moderate intensity that could be heard for a distance of approximately 400 feet. This statement means that such noise is at the threshold of hearing at that distance when observed by a person accustomed to listening to corona noise and who is concentrating his attention on its observation. Newly energized circuits generally have a higher noise level than found on the same circuit after weathering. After this conductor had weathered for the month or so until normal operation was in effect, and then further weathered for about two months with voltage applied, the noise as observed for dry weather was very much reduced and became essentially inaudible when observed from the ground when standing directly under the line midspan. In general, in the vicinity of Los Angeles, it is only under exceptional conditions that the dry weather corona noise becomes audible when so observed. Under such conditions noise can develop very suddenly and apparently with no observable change in temperature or humidity or known factors affecting corona loss. It would be of an intensity that becomes inaudible at about 200 feet, and is not of long duration and is less noisy than tire noise from an automobile traveling on smooth pavement.

In the foothill region east of Los Angeles where the line is at elevations of approximately 2,000 feet, the dry-weather noise is not much different from that which has been discussed. It can probably best be described by saying that an occasional corona brush manifests itself with an intermittent buzzing sound.

When observations are made in the mountains and desert, inconsistencies begin to appear. The highest noise levels are not found at the high elevations as would naturally be expected from the variation of the air density factor. Observations at elevations in excess of 4,500 feet are usually inaudible if the location is such that the observer cannot be close to the midspan because of a gulley or canyon. The audibility at the tower due to approximately 100 feet elevation of the conductor is quite low. At other points of high elevation where the contour of the ground permits observation at closer distances, a mild corona noise is heard.

One of the few sections producing the most noise is in the vicinity of mile 175 from Boulder at an elevation of 3,200 feet. When at its highest level the noise at this point has the crackling sound associated with a small grass fire and also a low humming sound similar in quality to transformer hum, is sometimes heard. When the noise is at its maximum, at this location, it becomes inaudible at a distance of 1,200 to 1,500 feet. This noisy location exists for a distance of about three miles on both circuits. There is considerable variation in the noise from day to day, over and above that due to temperature changes. A high degree of noise has been observed on cool evenings and very quiet operation has been observed during warmer daytime periods, which is contrary to normal expectations. Sometimes in the course of a few minutes a conductor will exhibit alternations of noise and quietness.

At night, no uniform envelope of corona is visible, as the discharge is in the form of scattered and intermittent brushes from two to eight feet apart, which are visible at midspan where the conductor is about 30 feet above the ground.

There has been occasion to notice about four other similar areas where such corona conditions exist but probably to a lesser degree.

During wind storms, in the desert, other lower voltage lines had been observed to have a glow similar to corona, probably due to charging or discharging dust particles or other atmospheric charge phenomena. On the assumption that atmospheric charges might have some influence on corona loss and noise, an observation station was set up in the vicinity of mile 175. At two locations, about 800 feet and $1^1/_2$ miles from the line, antennas about 50 feet long were carried on masts 30 to 40 feet above the ground on hilltop locations. An electroscope was used to measure the potential of the antennas to ground. Arrangements were also made adjacent to the line to measure radio interference noise, and to record audible corona. By taking observations simultaneously at both places, it was thought it might be possible to obtain some correlation between atmospheric charges and corona noise. Voltages up to approximately 2,000 volts were measured by means of the electroscope, which is not uncommon for antennas in the desert. Very little if any correlation was observed between the accumulation of charges on the antennas and noise on the transmission line.

For the great part of the line length, corona noise is at a comparatively low level. At elevations of the order of 2,300 to 2,700 feet, which is about the average elevation of the line, and on a normal dry day with the temperature a little above average, the noise is at the threshold of audibility at a point midway between the two single circuits, which have a separation of 265 feet. This general low level of corona noise is very satisfactory and indicates that the conductor design has been adequate.

WET WEATHER CORONA

As soon as rain begins to fall on the conductor, corona noise becomes evident at any location. The noise is so directly coincident with the beginning of the falling of the drops, that the cause of the loss and noise is either the exchange of charges between drops and the conductor or the mechanical pattern and shape of conducting water caused by the rain drops striking the conductor. The motion of the drops in the alternating electrostatic field may also contribute to the humming sound quite evident during rain. The resulting noise is moderate.

At an elevation of 1,000 feet, where adequate darkness prevails, it is just barely possible to see rather intermittently the bluish corona glow during rain.

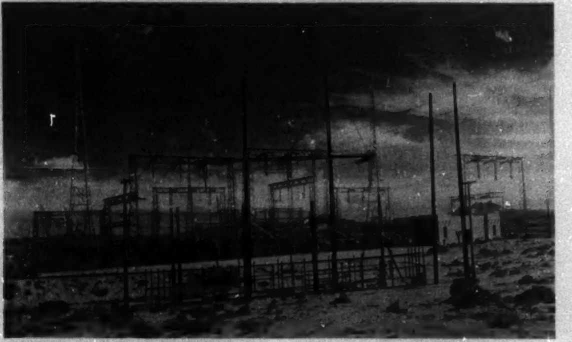

Figure 2. High-voltage wattmeter equipment for the Victorville corona tests

The steel structure in the background is the Victorville switching station

At elevations in excess of 2,000 or 2,500 feet, and similar darkness, the faint blue glow of corona is visible for about two spans. The glow frequently has an intermittency that gives the effect of longitudinal motion along the conductor. It is believed that this effect may be due to rain drops hanging along the bottom of the conductor, when it is vibrating slightly. Then the drops are either shaken loose or elongated by the motion, which develops glow either by changing the gradient, or by carrying small charging currents to the falling drops.

Desert Loss Measurements

For the past several months consideration has been given to the design of a third 287.5-kv Boulder circuit. Due to the fact that no high-voltage source was easily available for testing in the actual line location heretofore, the work had all been done at the Ryan laboratory. On the basis of such tests, 1.4-inch-diameter stranded conductor was considered to be satisfactory for 287.5-kv operation and 1.4-inch-diameter hollow copper conductor of the segmental type showed appreciable liberality. These facts together with a general excellence of performance of the 1.4-inch conductor being used, brought about the suggestion that the possibility of using a smaller diameter of conductor be investigated. However, observations of noise at some special locations on the desert, as previously mentioned, indicated some departure from the laboratory conditions and made it desirable to have some loss tests made actually under desert operating conditions.

Since high voltage was now available from the Boulder circuit, a test set up could be made at the Victorville switching station. By isolating the portion of one circuit from Boulder to Victorville and supplying it from one generator, voltage could be controlled from any desired low value up to about 360,000 volts at Victorville. High-voltage wattmeter equipment similar to that used for some of the tests at the Ryan laboratory,[8] and a suitable test line, were erected. General views of these test arrangements and their location with respect to the switching station are given in figure 1 and figure 2.

At this location, tests are in progress on 1.25-inch-diameter and 1.4-inch-diameter hollow copper type *HH* conductor and also on some 1.4-inch stranded aluminum conductor steel reinforced that had been tested previously at the Ryan laboratory. At the conclusion of this work, the details will be reported.

One important result of these tests was that, after due allowances had been made for barometric pressure and temperature, it was found that the losses at Victorville were appreciably higher than those obtained at the Ryan laboratory. This is true for all conductors tested to date, including stranded as well as those of the segmental type. The aluminum cable is the identical sample tested at Stanford and its surface was carefully protected so as not to be roughened or damaged in shipment or erection. The 1.4-inch type *HH* cable was a fair sample of that cable, not noticeably different to any that had been heretofore tested or used on the line. The 1.25-inch diameter type *HH* cable was a new size manufactured for the test, but the equivalent of its losses at Stanford can be judged from interpolations made from tests on 1.1-inch and 1.4-inch-diameter cable.

The increased corona losses measured at the Victorville test location are sufficiently higher than those obtained at the Ryan laboratory, so that the difference cannot be explained on the basis of any reasonable altitude and temperature corrections. The losses are also sufficiently high so as to make unattractive the use of 1.25-inch-diameter hollow copper conductor of the segmental type or even 1.4-inch-diameter conductors with a stranded cable surface. As far as can be judged by measurements to date, which are not fully completed, 1.4-inch-diameter hollow conductor of the segmental type as used on the line will have losses sufficiently low as to confirm the design and size of this conductor as being satisfactory for 287.5-kv lines.

Such higher losses have been found on clear days and under such conditions that wind, humidity, temperature, and barometric pressure do not offer sufficient explanation for the results. The conductors have been tested as soon after washing as was possible, without demonstrating that the accumulated contamination of the conductors was increasing the losses. Among the intangible items that have been mentioned as possible causes are such things as atmospheric charges, sun-spot phenomena, invisible charged dust particles, the presence of radioactive materials in the vicinity or as dust on the conductor, or the presence on the conductor of any

Symposium on Operation of the Boulder Dam Transmission Line—Insulation and Lightning Protection

By BRADLEY COZZENS
MEMBER AIEE

Synopsis: This paper covers a résumé of the fundamental principles of insulation and lightning design for the Boulder Dam transmission line, including the fog problems encountered in the coastal regions and the lightning problems of the desert section. The operation in both areas is covered, with particular reference to the lightning encountered during two years of construction and one year of operation. A description of the lightning recording equipment with current values and frequency of strokes is reported. From these values the adequacy of the design is definitely indicated.

THE Boulder Dam-Los Angeles transmission system of the Bureau of Power and Light traverses country presenting a wide variation in geological and climatological conditions. Starting from practically sea-level conditions at the Los Angeles terminus, the line passes through low-lying alluvial valleys, with their accompanying fogs, through the foothill sections and across the gravelly and rocky talus slopes at the base of the San Gabriel Mountains. Passing over Cajon Pass at an elevation of 4,415 feet, the line drops gradually into the Mojave Desert area for the remainder of the 190 miles to the Boulder power plant. For those unfamiliar with this territory, it is a mingling of mountain ranges, large sloping valleys of a generally rocky character with occasional sinks or dry lakes that are flooded at two- to four-year intervals by desert storms. In this section the line varies in elevation from 800 to 4,862 feet with the general trend being between 2,000 and 3,000 feet. The average rainfall in this territory is between one and six inches per year which usually comes in one or two storms of cloudburst proportions.

In the area between the Cajon Pass and the coast the line is subject to long dry periods during the summer months, at which time the insulators collect considerable dust aggravated by field cultivation. This period is followed by the heavy fogs, in the fall of the year, which fogs come without much warning and seriously impair the effectiveness of any insulation. Although in the desert area there is appreciable dust, there is almost complete freedom from fogs and its attendant problems. The rains come as heavy downpours that serve primarily to wash insulation rather than producing any insulation problem from this standpoint.

The familiar isokeraunic charts presented by the United States Weather Bureau[1] show Los Angeles having less than 5 lightning storm days per year; the mountainous section between Los Angeles and the desert, approximately 7; with the frequency gradually increasing to approximately 30 in the vicinity of Boulder Dam. Prior to the design of the line, these values were partially substantiated by a survey of the territory and of the operating records of the few transmission and telephone lines that traverse it.

Thus two definite insulation problems were presented; namely, insulation for the lightning voltages of the desert and insulation to be free from flashover due to dust and the fogs of the coastal region. Since the system was to be a major source of supply for the City of Los Angeles, justifying the high degree of reliability specified by the management, the design contemplated practical immunity from flashover from either lightning or insulator contamination and fog.

Fog Design

Insulation that will withstand contamination and wetting of fog has been a baffling problem on the Pacific Coast for many years. Studies made in connection with the design of the Boulder Dam line and other lines of the Department gave a good understanding of the action of insulators under these conditions and also led to the redesign of lightning arresters[2] to operate under such conditions. Under fog conditions the leakage current over insulators is 20 to 200 times the normal capacitance current. The voltage distribution is thus determined by surface resistance alone.

Paper number 38-111, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted June 3, 1938; made available for preprinting July 16, 1938.

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1. For all numbered references, see list at end of paper.

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chemicals that may increase the emission of electrons. A new field of corona investigation is thus opened up.

As a final check on corona loss on the Boulder transmission line, reference should be made to the over-all monthly efficiencies that are given in table I, of the companion paper on general operation of the transmission lines. These losses agree so closely with calculated resistance and equipment losses, that the corona loss is negligible.

Conclusion

The experience obtained to date in the operation of the line indicates a general low level of audible corona noise and conservative corona losses thus substantiating the original design.

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Discussion

For discussion see page 156.

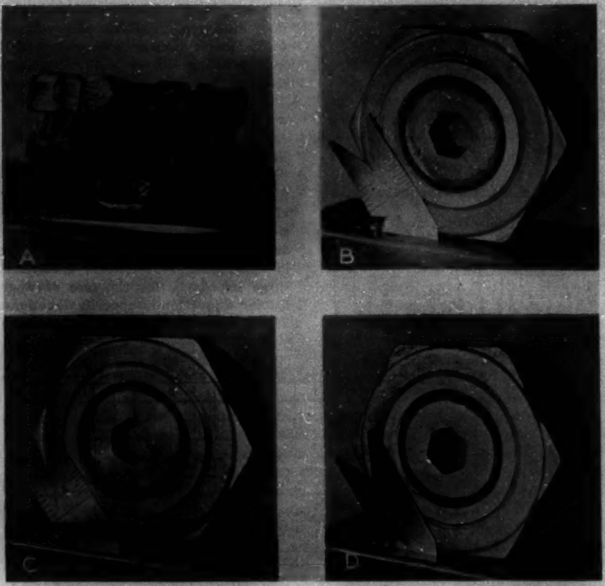


Figure 1. Gaps used to fasten counterpoise to tower leg and to break the electrolytic circuit between copper counterpoise wire and zinc-steel tower legs

A—Section through gap, stud cut off
B—Gap flashed by natural lightning
C—Gap flashed in laboratory, 75,000 amperes
D—Gap flashed by natural lightning

Since the drying of the surface film increases its resistance and consequently the voltage duty and power dissipated in the dry areas, these drier sections will continue to heat and dry carrying more voltage duty while the other areas become wetter and take less and less voltage duty. The caps of these drier units may operate in excess of 35 degrees Fahrenheit above the ambient air temperature, while the high-resistance area adjacent to the pin may be much hotter.

The results of the fog tests on practically all commercial and many special insulator shapes showed the old-type 12-inch flat disk to have the lowest leakage current and ability to insulate in fog. Though the smooth surface and poor electrostatic stress distribution of this type unit inhibits dust deposition, the same conditions exaggerate the sparking and arcing of the wet units eliminating its use in populated areas. However, this unit has approximately 20 per cent lower impulse flashover than the conventional-type insulators which eliminated its use in other locations. Little, if any, improvement was found for any of the so-called fog-type insulators available for these tests. To facilitate cleaning and yet obtain the greatest leakage in a given string length, the ten-inch-diameter five-inch-spaced standard suspension disk was selected for this type of service. In accordance with tests and the successful operation of 20 such units on 220 kv in northern California it was felt that the use of 24 units at the higher voltage but somewhat less severe fog condition on the Boulder Dam lines would preclude the possibility of flashover for a number of years. To prevent disturbance in the residential section through which the line passes a program of washing the insulators once or twice a year

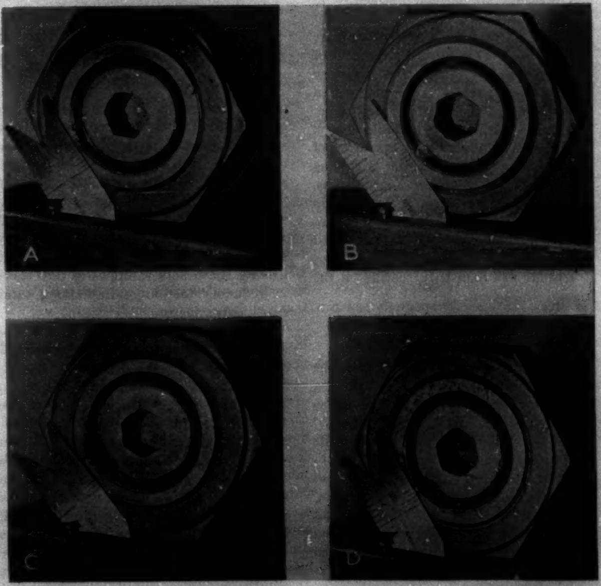
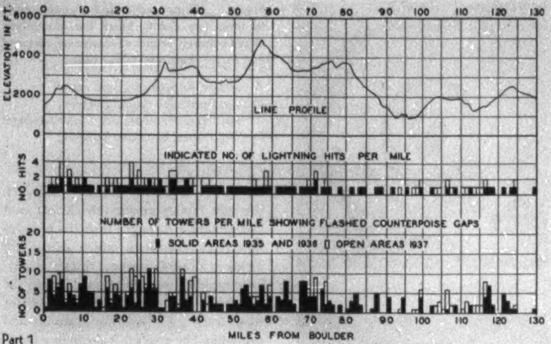
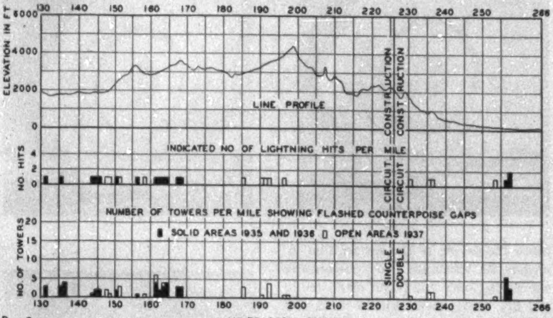


Figure 2. Gaps used to fasten counterpoise to tower leg

A—Gap from same tower as gap of figure 1 *D*. Note multiplicity of burns
B—Gap fused across by natural lightning
C and *D*—These gaps resemble power-frequency burns and appear to be the result of many strokes. Gaps burned before line was energized



Part 1



Part 2

Figure 3. Parts 1 and 2, graphical presentation of lightning strokes per mile and number of towers affected per mile as indicated by flashed counterpoise gaps with accompanying profile of tower footing elevations

with high-pressure jets is being used. A section of the line approximately five miles long has been washed twice to date in this manner.

At Victorville switching station, less than a mile from a large cement plant, the station insulation, switch supports, and housings are washed frequently to prevent setting of the cement dust. Jet pressures up to 800 pounds per square inch are used. The line for a distance of about ten miles through this territory is washed once a year. It is contemplated that cleaning the insulators on the remainder of the line will be unnecessary.

During the 20 months of operation of the Boulder Dam circuits involving practically two complete fog seasons, there has been no danger of flashover in the fog areas, and with the washing program being followed, the noise created by the insulators under fog conditions is actually less than that existing on the lower-voltage lines.

Lightning Design

At the time the lightning design of the Boulder Dam line was started, lightning theory had not been developed nearly so far as at present. Direct hit theory[3,4] had a few proponents and had had its first substantiation by reasonable methods of calculation.[4,5] Counterpoise had been used successfully in one location[6] and before the erection of the line had been installed on other transmission systems.[7,8] Many of the factors used were not contemplated in the mathematical theory of lightning phenomena at that time; such as, change in stroke surge impedance with voltage, change in coupling factor due to excess voltage and corona, reduction in kilovolts per foot values for long distance flashovers, the concentration of charge on the ground surface rather than at the equivalent ground plane depth, the effect of current leaving the towers in opposite directions in reducing the surge impedance, and many other factors.

In general, classical wave theory as applied to transmission lines[4,5,9] was used as the main basis of design. The voltages encountered were based on 16 to 20 million volts in the streamer at the tower top. The streamer surge impedance used was the conventional 200-ohm value, though studies were made with other values as modified by size of corona sheath and height above ground. This variation in streamer impedance as well as the impedance of the feeder streamers or collecting paths within the cloud are considered extremely important factors in determining the current in the lightning discharge. The flashover voltages for the longer distances were extrapolated from existing curves with the slope slightly decreased in the upper regions. The theoretical corona sheath diameter was used for the capacitance element in computing coupling factors and surge impedance while the metallic conductor diameter was used for the inductive component. In the case of towers, due to the already large dimensions, physical dimensions only were used for both the inductance and capacitance elements. There is appreciable reduction in theoretical surge impedance for the case of wires leaving a junction in opposite directions. Though this effect decreases rapidly with distance, it is believed that this effect combined with the fact that the charges are located on or near the ground surface accounts for the improved performance of the counterpoise ground over the vertical ground well, even though the latter may have the lower measured resistance.

As contemplated, ground resistance measured on stakes driven to approximate counterpoise depth showed resistance values as high as 3,000 ohms, though the large concrete tower footings at their greater depth showed values from as low as one ohm up to 150 ohms, with the majority of the footings measured showing less than 10 ohms. It was realized that to attempt to terminate counterpoise wires in this high-resistance top soil layer would produce severe voltage reflections and render the counterpoise ineffective. In addition, with the equipment developed for laying the counterpoise, it was more economical in most cases to use the additional length of wire in laying a continuous counterpoise than it would have been to attempt to cut and ground radial wires at the end of shorter runs. The continuous counterpoise was used, each wire leaving the tower radially until 135 feet apart, then turning to run parallel to the center line of the transmission circuit, thus gaining the advantage of low surge impedance of the radial counterpoise without the disadvantages of terminal reflections. In the desert section where the two single-circuit tower lines are used, there is a cross tie between the adjacent counterpoise systems of the individual circuits. This gave a theoretically lower surge impedance than would have resulted from the addition of a single wire, but with approximately a one-microsecond time delay. Arrangement of the counterpoise was given in a previous paper.[10]

The evaluation of the counterpoise surge impedance is still a debatable matter. In this design attempt was made to duplicate by calculation values that had been recorded in tests.[11] It was necessary to use correction factors to get agreement between these data and computed values, though some writers later obtained agreement by the use of two-wave theory.[12] The correction was primarily the use of a slight corona sheath or high-conductivity area in the ground and surrounding the counterpoise. That this condition exists can be indicated by surging a conductor in water of high conductivity and observing the corona formation on the conductor under the water. The other factor, primarily not a correction, was the use of a dielectric constant greater than unity for the material outside of this corona envelope. With these corrections, close agreement between theory and measured values was obtained.

Realizing the possible inaccuracy of the assumptions upon which lightning theory is based, a slight safety factor was considered desirable. The curve of flashover with ten-microsecond time lag lies approximately 12 per cent below the same curve for two-microsecond time lag. To introduce a safety factor, the curves for the ten-microsecond time lag were used for insulators and gaps, rather than the two-microsecond values as computed. Thus if the computed values were rigidly correct, the use of the ten-microsecond curves would give a 12-per cent overinsulation for the maximum lightning voltages expected, which percentage is no more than the erraticness of flashover voltage.

On the basis of these studies a 16- to 20-million-volt lightning stroke to the tower would require approximately ten feet of insulation to prevent flashover. To balance this insulation for the case of a stroke to the ground wire at the center of a 1,000- to 1,200-foot span would require a separation of 40 feet between ground wire and conductor at mid span. This balance of insulation between the supports at the tower and the separation between conductor and ground wire at mid span has been substantiated by recent literature.[13]

Insulators

A lightning generator of the cage type was erected at the Harris J. Ryan High Voltage Laboratory, Stanford University, and with the co-operation of Doctor J. S. Carroll, studies of the effect on impulse and 60-cycle flashover of insulator separation, diameter, and shape were made on practically all commercial shapes available at the time. Assemblies up to ten feet in length were tested. Other data[14] published by eastern laboratories substantiated these measurements and furnished additional data upon which to base cost studies. This study included, in addition to the cost of insulators, the cost of added tower height and cross-arm length necessary to maintain the same flashover voltage. On this basis, the 10-inch-diameter 5-inch-spaced unit showed a decided economic advantage, while the 12-inch-diameter conventionally shaped unit with a separation of $5^3/_4$ inches showed the second lowest cost based on insulator and steel prices at the time of the study. Because of these studies, the 10-inch-diameter 5-inch-spaced insulator was selected for the desert or lightning section as well as fog section of the Boulder Dam line.

Grading Rings

Initial studies were made on a relatively large oval grading ring for the lower end of the insulator strings with a circular ring for the top end of the assembly, and their proper dimensioning and location was verified by impulse tests in the laboratory of one of the eastern manufacturers. These shields proved too costly for the improvement offered; so it was decided to use a relatively large arcing horn at the lower end of the string with a strap-type upper horn. The basis of design and positioning of these arcing horns was such that they should prevent cascading for all string flashovers occurring in three microseconds or less, and be free from corona at a voltage 20 per cent above the operating voltage. This basis was followed as an insulation standard for all of the equipment on the Boulder Dam lines. The insulator assembly has been pictured in a previous paper.[10]

Operation and Measurements

One of the circuits was first energized for its entire length from Boulder power plant on October 9, 1936, and went into permanent operation on October 26, 1936, as described in a companion paper.[15] Thus the energized line was subjected to a few storms at the end of the 1936 lightning season and all of the 1937 season under normal operating voltage.

The line erection was started in the latter part of 1934, and approximately 27 per cent of the overhead ground wire and a somewhat larger percentage of the counterpoise had been installed by August of that year. During the 1935 lightning season, 73 per cent of the ground wire and 79 per cent of the counterpoise were installed with conductor stringing progressing to about these same values. Thus in the collection of data, the 1934 and 1935 season are considered as one year and called 100 per cent line length. The line was 100 per cent complete during practically all of the 1936 lightning season.

The counterpoise leaving the base of the tower is attached with a gap connection for the purpose of preventing electrolytic corrosion between the copper counterpoise and the zinc and steel of the tower leg. External from the tower, however, the counterpoise is continuous with the loop between the two wires of each circuit and between adjacent circuits closed at each tower to prevent building up excessive induced potentials in long isolated leads. A cross section of one of these gaps with the counterpoise wire in place is shown in *A* of figure 1. The gap is of cast bronze with molded bakelite insulation between the two elements and bolts directly to the tower leg about seven inches above the footing. The gap between the concentric cylindrical elements is $^1/_{16}$ inch, which offers no barrier to lightning voltages.

In addition to providing the insulation necessary to break the zinc-copper circuit, the counterpoise gaps serve to indicate the location of lightning strokes. The first indication of outstanding importance from these gaps was an observed direct hit which involved the gaps on seven towers of the two adjacent circuits. From the nature of the burns a code was developed primarily involving burn size and location and the nature of the burn. The diameter or size of the burn was given as a number indicating twentieths of an inch; thus, a half-inch burn is a number 10 burn. Early in 1937 the gaps on the first 200 miles from Boulder were surveyed. This survey, covering the two years of erection with a few months of operation, revealed a wide variety of burns ranging from a small bead of copper about 0.02 inches in diameter to burned areas covering over one-half inch of the circumference of the gap. Other discharges fused the gap together in diameters in excess of $^{1}/_{8}$ inch. The code that had been developed was inadequate and had to be revised, though the measurement of the burn diameter in twentieths of an inch was retained.

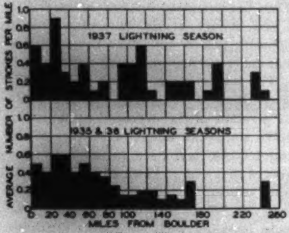


Figure 4. Average number of lightning strokes per mile per year. Averages obtained for each ten mile section

At about this time published data indicated that burn size could be used as an indication of the magnitude of crest current.[12,16] The sponsor of this idea checked this effect on the counterpoise gaps using an oscillatory wave and currents from 25,000 to 100,000 amperes. The burn diameter was found to be proportional to current with approximately 0.625 inches of burn per 100,000 amperes. These burns had the character of molten metal that had been blown violently. The burn as produced by test with 75,000 amperes is shown in figure 1 *C* and is about $^{9}/_{20}$ inch in diameter. The burns produced by actual lightning are equal or greater in diameter than those obtained in the laboratory. Figure 1 *B* shows a burn from natural lightning of the same character as the laboratory burn that is between $^{13}/_{20}$ and $^{14}/_{20}$ inch in diameter. This would indicate a current in excess of 100,000 amperes. Figure 1 *D* shows another burn of the same nature between $^{4}/_{20}$ and $^{5}/_{20}$ inch in diameter that was obtained from a tower leg on which the magnetic-link-type surge-crest ammeter had recorded 21,000 amperes with 18.5 per cent oscillation. The magnetic links on the north leg of the same tower indicated a similar current value, but the gap showed three or more burned areas indicating the multiplicity of the strokes. The diameter of the individual burns was approximately $^{3}/_{20}$ inch while the laboratory data would have indicated 21,000 amperes should have burned an area between $^{2}/_{20}$ and $^{3}/_{20}$ inch.

Complete fusing of the gap occurred in only about five cases. Figure 2 *B* shows a typical case in which the diameter of the fused section is nearly $^{3}/_{20}$ inch. This condition is considered to be the result of many low-current strokes of a multiple discharge, or the result of a low-current long-time discharge. Figures 2 *C* and 2 *D* show two burns of a similar nature, yet much more extensive. These are assumed to be the result of multiple strokes or many strokes in the same location. These latter burns have the nature of power-frequency burns, but there have been no flashovers since the line was energized, so they are known to have been the result of lightning.

Though there is close agreement between the laboratory and field data in many cases, others show little similarity. Extreme care and considerable more data will be necessary before using the counterpoise gaps as an accurate current-measuring device, primarily because of the multiple nature of the lightning discharges with overlapping burns, the oscillation between the streamer and the lightning-protection system or within the latter, and the great variation in the time of discharge, though at present duration of discharge is considered to have little effect on diameter. Further calibration data are being obtained and it is hoped that future reports will include current values as indicated by the counterpoise gaps.

The location of the counterpoise gaps flashed by lightning is given diagrammatically in figure 3. The line profile is included in the charts. The data from the survey of May 1937 include all of the gaps that were flashed during the 1935 and 1936 seasons except those changed by construction crews and are indicated by the solid areas. The data from the survey of May 1938, covering the 1937 lightning season, are indicated by the open areas of the chart.

The upper section of the chart shows a great many more gaps flashed in the first 130 miles from Boulder Dam than in the lower section of the chart which covers the remainder of the line. This substantiates the data given by the Weather Bureau's isokeraunic charts.[1] There was a total of 110 strokes indicated during the 1935 and 1936 seasons, or an average of 55 per year, and 63 during the 1937 season. In general, less towers were involved per stroke during the 1937 season than during the previous seasons, due possibly to inaccuracies of the earlier survey or overlapping of the two seasons. With but few exceptions, the lightning is relatively evenly distributed in the first 80 miles from Boulder Dam. The lack of hits at the high point of the line in mile 32 is due to the fact that the line runs through a pass and is protected by adjacent mountains. This is also true to a lesser degree of the ridge in mile 39 and 40. For the next 30 miles, from mile 40 to 70, the 1935 and 1936 seasons showed little decrease in intensity, but the hits during the 1937 season were very scattered, being limited to sections adjacent to the high points. It was this summit at mile 57 that showed the heavy power-frequency type burns of figures 2 *C* and 2 *D* during the 1936 season. The section between mile 80 and 170 shows a decidedly lower lightning level with a still lower lightning level between mile 170 and the Los Angeles terminal of the line.

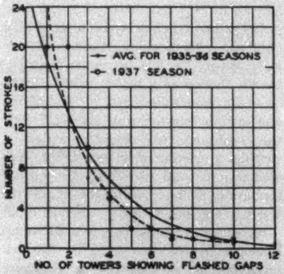


Figure 5. Variation in lightning stroke intensity as indicated by the number of strokes affecting various numbers of towers—indication obtained from flashed counterpoise gaps

The group of hits near mile 257 indicated as a 1936 hit occurred in December 1936 after the line was energized. This was in the section of double-circuit construction where the lightning protection is less effective than in the single-circuit area, due to less counterpoise and greater separation between the ground wire and lower phase wires. There was no flashover, yet the counterpoise gap burns on two adjacent towers indicated in excess of 100,000 amperes per tower.

Figure 4 gives a condensed indication of the lightning stroke intensity throughout the line. The values plotted are the average strokes per mile per year for the ten-mile section included. The lower section using two years data shows a general trend with the number of strokes decreasing between the dam and Los Angeles. The upper section for 1937 only is very erratic in nature. There are high sections between miles 90 and 120, 140 and 170, 180 and 200, and again between miles 230 and 250. These blocks were not shown clearly by the larger plot of figure 3.

In the lower section of figure 3 is shown the number of towers per mile that showed flashed counterpoise gaps, which follows the same trend as the number of hits per mile. The duplication of hits in mile 24 produced an unusual quantity of flashed gaps yet the whole section of the line up to mile 80 showed a large number of flashed gaps. In addition to showing the lightning frequency decreasing west of mile 80, figure 3 also indicates that the intensity may be decreasing also due to the smaller number of flashed gaps for each hit. There are many more hits in this section where only one or two towers are affected.

As an indication of the distribution of stroke intensity, figure 5 shows the number of strokes that flashed counterpoise gaps on various numbers of towers from 1 to 12. The plotted dots and solid-line curve give the approximate average per year for the 1935 and 1936 seasons combined, while the plotted circles and dash-line curve cover the 1937 season. The two average curves are surprisingly close together though the individual points are erratic. From the curves it is seen that between 12 and 20 lightning strokes per year will involve but 1 or 2 towers; 4 per year, 4 towers; 2 per year, 6 and 7 towers; and a stroke every other year, or half a stroke per year, may involve 12 towers. There are, of course, those discharges that will not permanently mark the counterpoise gaps. Artificial lightning discharges of approximately 4,000 amperes show only slight discoloration with no pitting of the metal. This type discharge soon oxidizes and any record from it is lost; so that many more gaps are flashed than are actually found in the gap survey.

The number of strokes indicated may be in slight error due to some hits that were multiple in character, terminating successively on both towers yet recorded as one hit only. This increases the number of gaps per stroke, for some of the gaps flashed on the second circuit were probably due to cross currents from the preceding stroke on the other circuit and vice versa. These are located in some cases by the displacement of the center of disturbance on the two lines, showing how easily both circuits may be involved in open country even though 265 feet apart.

Magnetic-link-type surge-crest ammeters are installed on 23 miles of the Boulder north circuit including miles 1 to 12 and 22 to 32. Recorders are installed also on all diverter towers at the Boulder switch rack and Silver Lake and Victorville switching stations. The normal location of the supporting bracket is seven feet above the top of the concrete footing which is three feet below the first panel point. Where unequal leg extensions or irregular tower construction is involved, the supporting brackets are placed at a symmetrical point in the tower. In the normal installation there are two brace angles, $2^{1}/_{2}$ by 2 by $^{5}/_{16}$ inches that terminate seven feet below the link brackets. The leg members are 5 by 5 by $^{5}/_{8}$ inches so it is anticipated that the major portion of the current is carried by these leg members. From the inspection of the counterpoise gaps prior to the installation of the magnetic links stroke currents of over 200,000 amperes per tower were indicated. Since with the insulation level on these lines low-current strokes could not cause flashover, and to record more accurately the high current strokes, the magnetic links were located 6 and 18 inches from the center of gravity of the angle rather than the 3 and 9 inches as in the normal installation.

Table I. Magnetic-Link-Surge-Crest-Ammeter Data for 1937 Lightning Season

Tower Number	Stroke Number	Leg	Current	Per Cent Oscillation	Cloud Polarity
25-*N*-3	37–1	N	*2,800+	over 50	–
		E	8,400	50	–
		S	4,400	–	–
		W	2,000	–	–
			17,600		
25-*N*-4	37–1	N	21,150	18	–
		E	20,600	18.5	–
		S	23,300	19.5	–
		W	19,150	21.5	–
			84,200		
25-*N*-5	37–1	N	*3,000 ±	over 50	–
		E	Trace	–	–
		S	5,000	–	+
		W	3,000+	over 50	–
			6,000		–
			5,000		+
26-*N*-1	37–1	N	Trace		–
		E	Trace		–
		S	*2,000 ±	over 50	–
		W	Trace		–
26-*N*-5	37–2	N	8,200	50 ±	–
		E	9,800	50 ±	–
		S	3,800	– ±	–
		W	3,600	– ±	–
			25,400		
4-*N*-3	37–3	N	10,600	50 ±	–
		E	9,000	50 ±	–
		S	5,300	50 ±	–
		W	10,400	50 ±	–
			35,300		
22-*N*-6	37–4	N	*2,000	–	–
		E	1,600	–	–
		S	2,000	–	–
		W	2,400	–	–
			8,000		
23-*N*-1	37–4	N	13,400	32	–
		E	15,400	37	–
		S	16,800	29	–
		W	17,100	28	–
			62,700		
24-*N*-5	37–5	N	15,000	35	–
		E	14,000	44.5	–
		S	13,200	42	–
		W	13,200	47	–
			55,400		
25-*N*-1	37–5	N	*5,000	over 50	–
		E	4,000	over 50	–
		S	4,000	over 50	–
		W	2,000	over 50	–
			15,000		

* Approximate.

In addition to the above mentioned installations, magnetic-link type and paper-film type surge-crest ammeters are installed on the 12 lightning-arrester phase units at the Boulder end and the six phase units at the Los Angeles end of the circuits. To date there have been no discharges recorded by the links on the arresters, though the paper film gaps on two of the arresters at Boulder showed very small punctures, possibly due to overvoltages combined with switching surges.

Five strokes have occurred of sufficient magnitude to record accurately on the magnetic links as located. One of these strokes showed indication on four towers while the remainder registered on only one or two towers. Many other towers showed traces of magnetism too small to be of value. Table I gives the tower number, leg location, crest current, degree of oscillation, and the polarity of the recorded currents. The largest stroke recorded was in mile 25 and showed 84,000 amperes on the tower hit with 17,000 amperes in the tower adjacent to the east. On the tower to the west three of the links showed the same polarity current: namely negative, while the fourth link on the south leg showed a positive current. This is one of the locations where the adjacent circuit was struck by one of the discharges in the same stroke or by another stroke. The counterpoise cross-tie between the two circuits first contacts the south leg of the north circuit which could account for the reversed polarity on this leg and the resultant lower values of current than those found on the tower to the east of the one hit. The stroke current of this discharge was at least 100,000 amperes, and if the above analysis is correct was in excess of 120,000 amperes. The oscillation on the tower struck was 18 to 20 per cent while on the adjacent towers, 50 per cent oscillation was indicated, possibly due to the effect of strokes on the adjacent line or the difference in the speed of travel of the surge currents through the overhead ground wires and through the counterpoise system.

The next stroke, in mile 26, was relatively light showing only 25,000 amperes with approximately 50 per cent oscillation. The third stroke in mile four again showed oscillation of 50 per cent or greater with 35,000 amperes in the tower and only a trace on adjacent towers. The fourth stroke centered on tower 2-*N*-1 with 62,000 amperes in the tower and oscillation between 28 and 37 per cent. The tower to the east showed 8,000 amperes with high oscillation. The stroke current was at least 70,000 amperes. The last stroke of any magnitude terminated on tower 24-*N*-5 with 55,400 amperes in the tower and oscillation between 35 and 47 per cent. The adjacent tower to the west showed 15,000 amperes with high oscillation. The total stroke current was again in excess of 70,000 amperes.

From the records obtained with the magnetic links it is evident that the low-resistance counterpoise system allows appreciable oscillation, particularly on the towers adjacent to the tower struck. This may be true oscillation, or may be reversal of current flow due to the difference in speed of propagation of the waves over the different paths. The values of lightning stroke current recorded, to date are between 35,000 and 120,000 amperes, indicating that the lightning encountered in the western part of the United States is approaching the same intensity as that encountered in the East, and since observations of the counterpoise gaps indicate that values practically double these may be encountered at times, places the lightning currents well within the range of those encountered elsewhere.

To further the study of lightning current phenomena, a lightning rod assembly is being erected on eighty transmission structures and diverter towers. This assembly is pictured in figure 6. The sharp point on the top of the rod is graduated in tenths of an inch so that the amount burned off by various lightning strokes can be determined. The information gained from this should be of value in determining the size of strand necessary in overhead ground wire to prevent complete destruction by strokes terminating on the wires.

As a measure of the total current-time values of the lightning stroke, the fuse link assembly is added below the rods. Adjacent to the rod is mounted the bracket for supporting the magnetic-link-type surge-crest ammeter while the paper-film-gap type is mounted on the supporting steel member at the base. A device for counting the number of discharges in multiple strokes is in the process of development to fill the vacant space between the last fuse-link clip and the film-type crest ammeter. It is hoped that this equipment will add some further information as to the effect of burning of lightning strokes as well as some data on the multiple stroke currents.

Conclusions

The insulation design selected for the fog section of the transmission line gives satisfactory performance, practically free from noise and perfectly free from outage if washed at occasional intervals as contemplated.

To date there have been 173 strokes to the Boulder Dam transmission line, none of which is known to have flashed an insulator assembly. Approximately 70 of these hits occurred since the line has been energized.

The counterpoise gaps used to break the electrolytic circuit between the tower steel and the counterpoise give very desirable indications of the towers hit by lightning and serve to give an approximate indication of the magnitude of the stroke.

Magnetic-link-type surge-crest-ammeter records have showed during one lightning season, currents as high as 84,000 amperes down a single tower and in excess of 120,000 amperes in stroke current.

The adequacy of the lightning-protection scheme has been largely substantiated by the past year's experience, and future performance will determine the exact factor of safety that was built into the lines.

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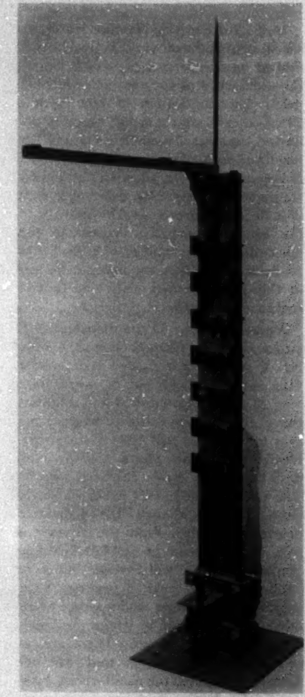

Figure 6. Lightning rods for tower tops, points to indicate degree of stroke burning, fuse links to indicate total current times time, with magnetic link crest ammeter at top and paper film crest ammeter at base

Symposium on Operation of the Boulder Dam Transmission Line—Carrier-Current Equipment

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CARRIER-CURRENT equipment is being employed for three distinct services on the Boulder Dam transmission line: first, for pilot channels for automatic protective line relays; second, for a transmission channel for supervisory control equipment which provides control from Boulder power plant of the two switching stations between Boulder and Los Angeles; and third, for telephone communication between Los Angeles, Boulder, and the two intermediate stations, and to transmit orders to line patrolmen who may be working at any point along the line. These three functions may be performed simultaneously without interference with each other, and are not affected by power conditions or normal switching conditions on the line.

This paper deals with the supervisory control equipment, the telephone equipment, and certain equipment common to all services. The relay installation is discussed in a companion paper in this symposium entitled "Transmission Line Relay Protection."

Important savings in operating time and in investment have been achieved by the use of carrier-current equipment for these functions. As Boulder power plant is the major source of power for the City of Los Angeles, and in order to increase the stable power capability of the line, it is necessary that faults be cleared with a minimum disturbance to the system, and that outages be reduced to a minimum. The use of carrier-current pilot relays provides the high-speed relay protection for individual line sections essential to maintain stability on the system during fault conditions. The carrier-current supervisory control provides a fast means of performing switching operations on the line and reduces dispatching time by providing continuous indication at both the system load dispatcher's office and Boulder power plant of the position of all switching equipment on the line. Due to the high mechanical reliability of the line and its lightning protection, the carrier-current telephone provides a communication channel more reliable and safer than could be obtained over a telephone line built through the country traversed, and at a fraction of the cost. It also provides a quick means of issuing orders to line patrolmen and thus reduces the time required to locate and repair any faults that may occur on the line.

The term "carrier current" is used here to denote currents of high frequencies that are transmitted over the metallic circuits of the power lines to perform special functions. Since the frequencies used are of the order of those used in space transmission most of the equipment used is radio-type equipment which is directly connected to the transmission line through coupling capacitors.

The carrier-current channels all operate over the 287,000-volt conductors between Boulder switch yard and Century receiving station at Los Angeles. The relay channels stop at these stations. However, from Boulder switch yard to the power plant the supervisory and telephone carrier channels operate through coaxial cables. From Century station to the load dispatcher's office in Los Angeles the supervisory and telephone operate over direct-wire channels.

The equipment required for carrier-current transmission consists first, of radio-type transmitters that generate the carrier frequencies and are controlled by signaling devices which may be relays, if it is desired to transmit codes, or microphones for voice transmission; second, tuned coupling devices for connecting the carrier sets to the transmission line; third, the transmission line and, fourth, radio-type receivers that rectify or demodulate the messages received. There are also auxiliary devices for blocking the carrier out of stations and bypassing station equipment.

All carrier-current equipment installed on the Boulder transmission line is designed to operate at any frequency in the wave band from 50 kilocycles to 150 kilocycles, and is designed to operate at 10 kilocycles separation between adjacent-channels without interference. This band permits the use of eleven channels at this spacing, of which there are now eight channels in use and more channels will be added later. The supervisory control equipment is operating at 60 kilocycles, the telephone at 90 kilocycles, and the relay circuits at 110, 117, 130, 135, 140, and 150 kilocycles.

Each relay carrier channel operates over one line section only, and since transmission is required only when there is no fault condition on the line, one conductor and ground provide a satisfactory circuit. For the supervisory and telephone services it is necessary to maintain carrier transmission during abnormal conditions. For each of these services, one conductor in each line of a different phase is used with the midpoint between the line tuning units grounded so that if one conductor of these circuits becomes open, transmission is maintained through the other conductor and ground. Phase *A* of each line section is used exclusively for the pilot relaying carrier channels. Phase *B* of each section of the north line and phase *C* of the south line are used exclusively for the supervisory control carrier channel. Phase *C* of the north line and phase *B* of the south line are used exclusively for the telephone carrier channel.

The line coupling devices used to connect the carrier sets to the transmission line consist of a capacitor connected to the line conductor and to ground through a variable tuning inductance and one wind-

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Paper number 38-112, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 9, 1938; made available for preprinting June 27, 1938.

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Discussion

For discussion see page 156.

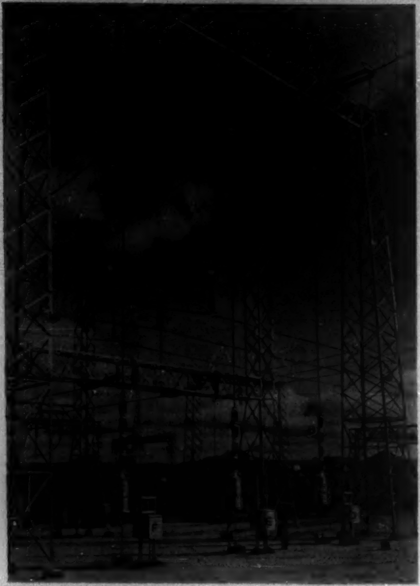

Figure 1. Line coupling equipment

A—Wave traps
B—Coupling capacitor
C—Potential device
D—Supervisory line tuning unit
E—Relay carrier set
F—Telephone line tuning unit

ing of a repeating coil. There is a coupling device connected to each conductor entering the station, and a wave trap between each coupling device and the station (figures 1 and 2).

Each coupling capacitor consists of four porcelain-housed units in series, each consisting of 40 series units, using aluminum foil plates, insulated with kraft paper, giving a capacity of 0.00075 microfarad for the complete unit. The porcelain housings are evacuated, then filled with insulating oil to within several inches of the top of the metal cap. This top space is filled with an inert gas and hermetically sealed to provide a cushion to prevent excessive pressures due to expansion in hot weather.

At the lower end of the capacitor the circuit divides, one branch being a carrier-frequency circuit and the other a power-frequency circuit. A 0.03-microfarad condenser, in the carrier-frequency circuit, blocks the passage of power-frequency currents and a 0.24-henry choke coil, in the power-frequency circuit, blocks carrier-frequency currents. From the small condenser the carrier-frequency circuit goes to the line tuning units. The tuning units for the three services are slightly different in construction, but each consists of a variable inductance by means of which the circuit from line conductor through capacitor, tuning unit, and repeating coil can be tuned to resonate at any frequency between 50 kilocycles and 150 kilocycles. In order to bypass station equipment, the tuning units at the two ends of the station for the supervisory carrier are connected together with underground coaxial cables; the telephone tuning units are similarly connected. The carrier sets are connected in the circuit between the tuning units (figure 3).

The 60-cycle circuit from the radio-frequency choke is completed to ground through the primary of a potential device having a capacity of 150 watts at 115 volts to provide energy for relay potential coils. The secondary of this device is connected to an adjusting unit by means of which phase angle and ratio adjustments are made.

The wave traps in the line between the coupling capacitors and the station are adjustable to block any single frequency within the carrier band from entering the station equipment and becoming dissipated in losses in the equipment or in closed shorting and grounding switches. For the supervisory and telephone services they also force the carrier currents to go through station bypasses, and thus provide approximately the same circuit characteristics regardless of the position of station equipment. The traps used for the three services are essentially the same; they consist of a large coil shunted by adjustable groups of small fixed condensers of various sizes in series with a variometer. The inductance coil in the traps used in the relay circuits consists of 31 turns of 750,000-circular-mil cable wound on a 15-inch-diameter porcelain shell. Those used in the traps for supervisory and telephone circuits consist of 36 turns of $^{7}/_{8}$-inch diameter solid hard drawn copper rod wound in two layers of 15-inch and 19-inch diameters supported on a bakelite frame. A protective gap and lightning arrester are connected across the trap to protect the capacitors in case of high voltages due to surges on the line.

All the carrier transmitters and receivers used on the Boulder transmission line operate from 115-volt single-phase 60-cycle power supplies. To insure the best reliability for their power supply, there are, in each station, two d-c motor-driven alternators obtaining power from the station control batteries and connected through automatic transfer equipment to a carrier equipment bus. Either alternator can be connected to the bus but not both at the same time. In case the operating machine fails and the bus potential drops, the second motor is automatically started and its alternator is connected to the bus. At the same time, the motor switch of the other set is opened. In case neither set starts, the bus is connected to the station lighting supply as an emergency standby. The station lighting supply is not used normally because its regulation is not considered good enough for this service. Automatic voltage regulators on the alternators hold the carrier equipment bus voltage to within one volt. The frequency varies slightly but this is not objectionable as all of the carrier equipment operates satisfactorily at any frequency between 50 and 60 cycles per second.

Carrier-Current Supervisory Control

CONTROL SCHEME

The carrier-current supervisory control equipment used on the Boulder transmission line provides a centralized control at Boulder power plant of the 287,000-volt switching equipment on the line, and also operates a system diagram board in the system load dispatcher's office at Los Angeles.

The operator at Boulder can perform the following operations at both Silver

Lake and Victorville switching stations: Open and close four oil circuit breakers, open and close eight disconnect switches, open and close four ground switches. There is continuous visual indication by means of colored lamps on the control board at Boulder of the positions of all this equipment and potential indications for each line section. There are also indications for the following equipment connected to the Boulder line and located in Los Angeles over which the Boulder operator has no control: four oil circuit breakers, four disconnect switches, four ground switches, and potential on two busses. On the same benchboard with the supervisory control equipment is a panel for the control of the equipment in the Boulder switch yard. This equipment is controlled by direct wire using the same type of control switches and indicating lamps that are used on the supervisory panels. From this panel are controlled four oil circuit breakers, eight disconnect switches, and four ground switches.

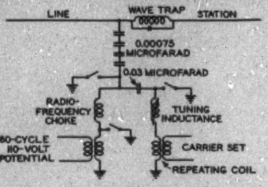

Figure 2. Carrier-current-coupling-equipment wiring

On the system diagram in the Los Angeles load dispatcher's office continuous lamp indication is provided by the supervisory equipment of the positions of all oil circuit breakers, disconnect switches, ground switches, and potential on lines and busses at Boulder switch yard, Silver Lake switching station, Victorville switching station, and the equipment at Century receiving station, Los Angeles, that is connected to the Boulder lines.

The Boulder operators are responsible for keeping the lines energized to Los Angeles. In case of trouble on the line, whereby Boulder becomes separated from Los Angeles, the bus breakers at Los Angeles are opened either automatically or by the local operator, depending on the nature of the trouble. The Boulder operator then energizes the line and it is synchronized with the system by the operator at Los Angeles. The control of all line circuit breakers at the two switching stations, in such a case, is by carrier-current equipment. Normally all switching in such a case would be done without telephone communication except orders for speed adjustment for synchronizing, and this order is over carrier-current equipment.

The specifications for the carrier-current supervisory control equipment require that the carrier-current equipment shall transmit and receive signals so as successfully to operate the supervisory control equipment: (*a*) under normal transmission line conditions, (*b*) without regard to the position or operation of any air-break switch, grounding switch, or circuit breaker at any of the stations, (*c*) with any one section of the transmission line solidly grounded at any point between stations, (*d*) with one phase wire to which coupling is made broken in any one section and either or both ends of the phase wire either grounded or ungrounded at the point of failure.

These requirements cover any operating conditions that are expected to occur on the line.

SUPERVISORY CARRIER EQUIPMENT

The carrier-current equipment for supervisory control operates at a single unmodulated frequency of 60 kilocycles, transmitting codes similar to radiotelegraph signals. The carrier equipment at the four stations are duplicates, using all industrial-type tubes. The transmitter circuit consists of a Colpitts oscillator, followed by one stage of class-*A* amplification and one stage of push-pull class-*B* amplification. Keying is done in the oscillator grid circuit. The keying relay is of the type used in high-speed radio telegraphy and operates from the 48-volt supervisory-control battery and is controlled by the supervisory relays. By selection of taps on the transmitter power transformer and by the use of either two or four tubes in the output amplifier, the sets are adjustable to operate at outputs ranging from 50 to 400 watts.

The best signal levels at all stations have been obtained with the transmitters at Boulder and Los Angeles operating at 400 watts output, and those at Silver Lake and Victorville at 200 watts output.

The receivers employ a special self-regulating heterodyne circuit. The receiver output operates a receiver relay which in turn controls the supervisory relays.

Test telephone sets are provided which can be plugged into the transmitter and receiver circuits for temporary communication.

The Boulder control board is a steel benchboard with four panels, one for Boulder switch yard, one for Silver Lake switching station, one for Victorville switching station, and one for Century receiving station at Los Angeles. On the benchboard there is provided, in a mimic bus system, an individual control key and indicating-lamp escutcheon for each piece of apparatus remotely controlled or supervised. Associated with these lamps and keys, and controlled by the operations of the keys, are groups of small multicontact relays mounted on relay panels. A similar installation of multicontact relays is made at each remote station. At the remote station, however, the relays are controlled by the operation of those at Boulder by carrier impulses transmitted over the line and are connected through their contacts to interposing relays which, in turn, cause the devices in the remote station to function in response to the operations initiated by the Boulder operator.

Separate independent codes are used for the selection of each device. For common functions such as close, trip, supervisions, Los Angeles check and release, a group of codes common to all points is used. Guard circuits are incorporated in the equipment that prevent the completion of a selection sequence if the selection code is not received correctly.

When an individual selection key is pulled on the benchboard, the Boulder transmitting relays set up and place on the line the predetermined selection code for that point. This code is transmitted from Boulder to all other stations. The function of this code is to select for operation the switch corresponding to the escutcheon on which the operator pulls out the selection key, and to select the corresponding point on the Los Angeles dispatcher's board. When the point selection code is completed, check codes are transmitted from the selected station and from Los Angeles, which cause the individual point selection lamp to be lighted on the escutcheon on the Boulder board corresponding to the equipment selected.

To close the breaker the operator now sets the individual twist type control key in the close position and then depresses the master control key. This causes the Boulder transmitting relay to send out a close code which is registered by the selected station's receiving relays and they, in turn, cause the "close" interposing relay to be energized, thus causing the power circuits to close the breaker. The closing of the breaker changes the position of its auxiliary switch, which, in turn, changes the position of the indication relay. This

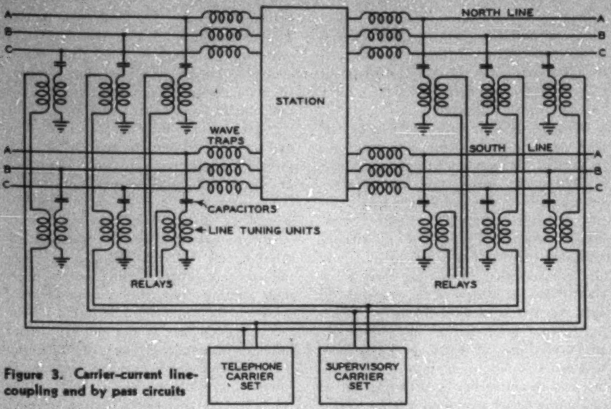

Figure 3. Carrier-current line-coupling and by pass circuits

relay operates to send a code which changes the position of the lamp relays for the selected breaker, thus causing the indicating lamps to change from green to red at Boulder and Los Angeles and completing the entire operation. The elapsed time from pressing the master control key until the lamps change is two seconds. Pressing the selection key resets the equipment to normal.

The operation of tripping a breaker is the same except that a trip code is transmitted to energize the "trip" interposing relay.

In case of an operation of any switching equipment by other means than by supervisory control a similar sequence occurs except that the selection code is transmitted by the equipment in the station where the operation occurred. This code is received at Boulder and Los Angeles and causes the indicating lamps to change.

Sequence of point selections is so arranged that line indications are on the first points, oil circuit breakers next, then disconnect switches and ground switches. If a line section relays due to a fault, five indications are transmitted, dead line and the opening of four oil circuit breakers, all in less than 25 seconds. If an automatic operation occurs during the sending of a code, the signal to indicate that operation is automatically withheld until the first signal is completed, and is then immediately transmitted. If a code originating in an outlying station is not correctly received at Boulder due to possible interference with the carrier or other external causes, the code is repeated until it is correctly checked or is automatically stopped after several attempts.

Interlock circuits are incorporated in the equipment that prevent the closing of a ground switch at either end of a line section if a disconnect switch is closed at either end of the section and also prevent closing a disconnect switch if a ground switch is closed at either end of the section. Circuits also prevent the operation of a disconnect switch unless its associated oil circuit breaker is open.

The equipment is so designed that trouble in any one station will not interfere with the operation of the balance of the system. Circuits are provided to sound alarms in case of a failure of the supervisory equipment. Telegraph sounders are located at the Boulder office and Los Angeles, connected in the line circuit so the operators may readily know when the supervisory is in motion.

In addition to the supervisory control all stations are provided with conventional-type remote-control switchboards and all switching equipment can also be operated manually. In case any equipment is operated from the local control boards or manually the operations are recorded by lamp changes on the supervisory boards at Boulder and Los Angeles.

Operating Experience

After the carrier-current supervisory-control equipment was put into service it was found that extraneous signals occurred on the line with sufficient 60-kilocycle components to operate the receiver relays. These signals were generated by lightning and other discharges during rain storms, arcs that occurred when disconnecting switches were opened or closed, and transients that occurred when line sections were energized or de-energized. Any of these signals occurring during the transmission of a supervisory code would interfere with the supervisory relays registering the code properly and cause the equipment to lock out or fail to complete the operation intended. The intensity of the signals caused by rain and lightning is relatively low and could be tuned out by desensitizing the receivers; however, the signals generated by disconnect arcs and switching transients are of higher levels than the code signals during some of the abnormal line conditions under which it is required that the equipment operate. These interference signals contain other frequencies in the carrier band in addition to the 60-kilocycle component. A circuit was designed and added to the receivers that will detect these multiple-frequency signals and increase the bias on the 60-kilocycle detector tube in proportion to their intensity. This circuit reduces the rectified current due to interference signals in some cases from approximately 12 milliamperes to 0.5 milliampere which is an unobjectionable value and permits weak 60-kilocycle code signals of the order of 6 milliamperes to be rectified with no decrease in value. This circuit has eliminated one of the major troubles that has been encountered in operating this equipment.

With all sections of the line energized for normal operation the carrier transmission loss is 32 decibels. This loss is increased as sections are de-energized and grounded for maintenance work. At present there are some unusual line switching combinations that increase the carrier losses to an extent that failure of the supervisory equipment occurs but it is expected that these failures will be eliminated in the near future by increasing the transmission efficiency during abnormal line conditions.

Carrier-Current Telephone

Communication System

The carrier-current telephone equipment provides complete intercommunication between Boulder, Silver Lake, Victorville, and Los Angeles, on a single carrier frequency of 90 kilocycles. This telephone channel is used for load dispatching, general communication, and for giving orders and instructions to transmission-line patrolmen cruising in automobiles in the vicinity of the transmission line.

The specification requirements for abnormal operating conditions are similar to those for the supervisory control. During normal operating conditions on the line this equipment provides excellent telephone service from the standpoint of speech quality, and provides good quality and continuity of service during abnormal line conditions. On the occasion when a double-circuit tower fell during a flood, five of the six conductors were broken, the conductor that remained in the air was one to which the telephone channel was connected and communication was maintained through this condition until a repair crew grounded this conductor.

This equipment uses the single-side-band carrier-suppressed principle of transmission. All the transmitted energy is concentrated into one side band which contains all the frequencies that are essential to reproduce the original speech. This equipment operates on a single-frequency channel for transmission in both directions and employs voice-actuated vacuum-tube circuits to start the transmitter and block the receiver when transmitting and reverses this operation when receiving. Normally the transmitter is held inoperative by the introduction of a high negative bias on the grids of the modulator tubes and the receiver is normally set to receive signals. Speech currents introduced into the transmitting circuits are at the same instant introduced into the duplex control circuit. The speech currents in the duplex control circuit act to unblock the modulator and to block the demodulator. This allows the transmitter to become operative after the receiver is blocked, thus permitting the modulated speech to be introduced into the power amplifier and thence through the coupling equipment to the transmission line. The design of the duplex circuit is such that the unblocking of the transmitter and blocking of the receiver is almost instantaneous when talking is started. A certain amount of delay is provided in the release of this circuit in order to prevent the transmitter from blocking and the receiver from unblocking between syllables or between words at average talking speeds. To prevent received speech from actuating the transmitter at the receiving terminal, the received speech operates on the duplex control circuits and prevents it from actuating the transmitter.

This equipment operates similar to a party line with selective ringing. Any station can hear all conversations on the line. Selective ringing is provided between all terminals. This is accomplished by employing an audio-frequency tone of 1,615 cycles which is controlled by conventional telephone dials. The code of audio-frequency pulses is transmitted from the calling terminal and received at all other terminals. These pulses actuate sequence switches of a type used in automatic telephone exchanges. At the receiving terminal corresponding to the code dialed, relays are operated which cause a signal bell to ring. Ring-back facility is provided so that when a bell at a station is being called a 1,615-cycle tone is transmitted back to the calling station.

A two-wire extension line is run from Century station to the main office *PBX* board in Los Angeles where telephones in the main office can be connected to the carrier system. The system load dispatcher has the right of way over all other terminals and can break into any conversation and ask for the use of the line.

Power required for the operation of this equipment is supplied from a 30-volt d-c source and from a 115-volt 60-cycle source. At one location a storage battery is used to supply the direct current and at all others copper-oxide-type rectifiers supplied from the 115-volt service. As the equipment must be ready for instant service it is always kept energized. Each equipment transmitter has a rated output of 125 watts.

Patrol cars along the transmission line are equipped with radio receivers tuned to the telephone frequency. When it is desired to issue instructions to patrolmen, the dispatcher impresses a special tone on the carrier which is received by all cars. When the patrolmen hear this tone they turn up their receivers and get whatever message is on the line. These car receivers will pick up messages at any point along the line patrol road, and the performance has been very satisfactory.

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Discussion

For discussion see page 156.

Symposium on Operation of the Boulder Dam Transmission Line—Transmission Line Relay Protection

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WHEN the study of these lines for the application of protective relays was started the predetermination of fault currents made it necessary to calculate all of the phase-sequence constants of the lines since none of these constants for conductors of this size and spacing was available.

The elements entering into the calculation of the positive-phase-sequence characteristics were easily determined and the proper method for their consideration has been fairly well fixed by previous practice. This was not the case for the zero-phase-sequence characteristics since the equivalent depth of the ground-return circuit could not be easily determined and there was no previous experience with a continuous buried counterpoise such as was to be used with this line. In making these calculations the equivalent depth of the ground return circuit was taken as 8,800 feet and the counterpoise was considered as though it did not make contact with the earth, or in other words as though it was part of the overhead ground wire system. The depth of the ground-return circuit used which corresponded to

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Paper number **38-113**, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 9, 1938; made available for preprinting June 30, 1938.

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1. For all numbered references, see list at end of paper.

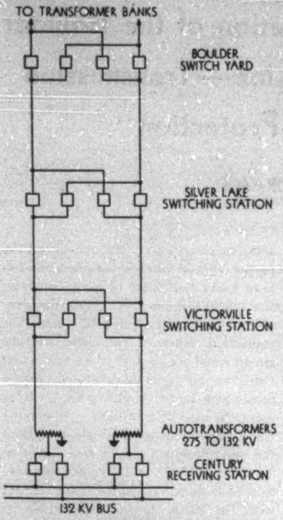
TO TRANSFORMER BANKS
BOULDER SWITCH YARD
SILVER LAKE SWITCHING STATION
VICTORVILLE SWITCHING STATION
AUTOTRANSFORMERS 275 TO 132 KV
CENTURY RECEIVING STATION
132 KV BUS

Figure 1. One-line diagram of Boulder Dam transmission line

dry earth was probably somewhat small for the territory over which this line was to be built and would consequently give values of impedance that were low; on the other hand, considering the counterpoise as not being in contact with the earth should give values of impedance which were high. Because of the manner in which these two factors entered into the calculation of the zero-phase-sequence impedance of the line it was believed that the final results would, if anything, be slightly high, and since they were to be used in determining the minimum current conditions for protective relays this was far more desirable than impedance values which might be low.

The line constants were first calculated for 50 cycles and a complete study of the short-circuit currents and voltages for all types of faults at each bussing point on the line was made. From this study it was apparent that if the shunt capacitance of the line was neglected the errors in current and potential, while quite large at some locations, were small at the locations under consideration in this study, and were entirely negligible at the locations which would be most difficult to protect.

When it was definitely decided that the system frequency would be changed from 50 cycles to 60 cycles it was a comparatively simple matter to recalculate the line characteristics, since a great deal of the work done during the 50-cycle calculation was not changed, and in view of the negligible errors introduced by neglecting shunt capacitance during the 50-cycle short-circuit study, it was decided to neglect this capacity in the 60-cycle study. The results of the 60-cycle study for three-phase and single-phase-to-ground faults have since been checked, using the distributed constants of the line and the above decision was entirely justified by these checks.

The results of the short-circuit study indicated that, under minimum fault conditions, the current available for operation of the protective relays would be less than the maximum load current the line would be required to carry, that the most difficult fault to clear would be a phase-to-ground fault on the 132-kv side of the autotransformers at Century receiving station and that with a phase-to-ground fault at this location when the parallel circuit was out of service the relays at Victorville could not be expected to function unless the delta connection of the autotransformer tertiary winding was opened.

Trials of Equipment

Carrier-current pilot protection seemed most desirable for these lines; however, it was felt that some operating experience with this type of protection should be had before a definite decision was made. Equipment of this type was purchased and installed on two parallel 132-kv transmission lines, together with a scheme of balanced current protection.

Two different types of carrier pilot protection were used; one line was equipped with an intermittent carrier type in which carrier current is only transmitted when a blocking impulse is desired, because of external fault; the other line was equipped with a continuous carrier type in which carrier current is transmitted continuously, and only stopped at both ends when an internal fault occurs on the line. Both types performed quite satisfactorily during faults on the system, both internal and external, but the continuous carrier type had the disadvantages that it caused interference on nearby broadcast receivers, and that the tubes in this type were required to carry plate current continuously.

The broadcast interference was due to the fact that it was impractical to operate the transmitters at the two ends of the line at exactly the same frequency and therefore an audible beat frequency equal to the difference between the two transmitter frequencies was produced. This audible beat frequency was heard in nearby broadcast receivers whenever these receivers were tuned near some multiple of either of the transmitter frequencies.

While no trouble of a serious nature was encountered with either of these equipments, other than that described above, the experience gained proved very valuable in preparing specifications for the Boulder line relays and in adjusting that equipment when it was installed.

Specifications

In addition to all of the ordinary features that would be required of any scheme of line protection the following special requirements were considered highly desirable in the Boulder line protection: (1) all faults on these lines should be cleared in not to exceed 0.15 second from the inception of the fault, until the last breaker was open, and (2) the protection should be immune to operation on an out-of-step or surging condition. The only types of protection that would possibly fulfill both of these special requirements are high-speed current balance protection and carrier-current pilot protection.

The maximum time required by the oil circuit breaker on these lines is three cycles or 0.05 second, and at the time the specifications were written it was uncertain if operating time shorter than 0.05 second could be obtained with carrier-current relays. This would mean that the settings of the carrier-current relays would have to be sufficiently sensitive to secure simultaneous operation of the relays at the two ends of a line section, or the total time would be extended beyond 0.15 second. Such low settings were undesirable and could be eliminated by the installation of high-speed current balance relays which would operate in 0.025 second or less so that even with full cascade operation of the relays and breakers at the two ends of a line section the total clearing time would not exceed 0.15 second.

The specifications were prepared on the basis of using both carrier-current pilot protection and high-speed current balance protection and the following performance requirements written in: The current balance protection shall operate simultaneously at both ends of a faulty circuit for any fault occurring within the center 70 per cent of the electrical length of the circuit but may operate sequentially for faults occurring within 15 per cent of the electrical length of the line from either end. The time required by the relays in either case shall not exceed 0.025 second. With one circuit of a section carrying 300,000 kva and the other circuit open, the circuit carrying the load shall not be tripped when the other circuit is closed at either end.

The carrier-current pilot protection as a whole shall:

1. With minimum system condition and both circuits of the section in service energize the oil circuit breaker trip circuits at both ends of a faulty circuit in not to exceed 0.05 second for all faults occurring within the center 70 per cent of the electrical length of the circuit.

2. With minimum system condition and both circuits of the section in service energize the oil circuit breaker trip circuits at the end of a faulty circuit adjacent to the fault in not to exceed 0.05 second for all faults occurring within 15 per cent of the electrical length of the line from that end, and shall energize the oil circuit breaker trip circuits at the end remote from the fault in not to exceed 0.05 second after the circuit is opened at the end adjacent to the fault. The above time requirement may be exceeded if all voltages are less than five per cent of normal and no secondary current is greater than ten amperes.

3. Not cause tripping for instability or external faults under any condition.

4. Clear ground faults occurring while the system is unstable without any purposely introduced time delay.

5. Clear phase-to-phase and three-phase faults occurring while the system is unstable with the minimum practical delay consistent with the requirement that instability shall not cause tripping.

6. Remain inoperative with a load of 300,000 kva on any one circuit and no fault on the system.

7. Not delay tripping for internal faults that occur immediately after any external fault except a three-phase external fault.

8. Be entirely self-contained and shall not depend upon the operation of any piece of apparatus not directly associated with the line protected.

Other requirements of a special nature were an automatic complete carrier transmission and reception check in both directions once every hour, and a pair of portable telephones that could be plugged into the relay carrier sets at the two ends of a circuit for temporary communication during adjustments.

Description of Equipment

Four high-speed current balance relays are used for each end of a pair of circuits, three for phase faults and one for ground faults. Each relay has two balance beam units and each unit is equipped with four coils and circuit-closing contacts for tripping two breakers. The coils are a current operating coil, a current restraint coil, a voltage restraint coil, and a d-c holding coil. The current operating coil of one unit is connected in series with the current restraint coil of the other unit, thus forming two separate current circuits for the relay which are connected one in the same phase-current-transformer secondary circuit of each line. The units trip the two oil circuit breakers associated with the line supplying their operating current.

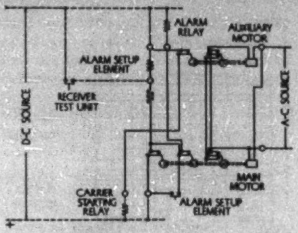

Figure 2. Diagram of automatic carrier test relay showing internal and external connections

The voltage restraint coils of the phase relays are supplied with phase-to-phase voltage, which leads the current used on the relay by 30 degrees at unity power factor. The voltage restraint coil on the ground relay is not ordinarily used.

A carrier-current pilot relay is used in which all of the necessary relay elements for the complete protection of a line terminal are incorporated in one case. This relay and the scheme of protection used is the same as described by Traver and Bancker,[1] except that two ground-fault detector elements are used and the functions of the ground-fault detector in the relay described are divided between these two in the same manner as they are with the phase fault detectors. In addition a normally open contact on the *A* ground-fault detector shunts the contacts of the block cut-off and block continuing units in the master oscillator grid circuit to prevent any delay in tripping ground faults occurring during instability.

An automatic carrier test relay and two auxiliary interposing relays in separate cases are used for each line circuit terminal in addition to the carrier pilot relay. The interposing relays are used one between the automatic carrier test relay and the master oscillator grid circuit, and the other between automatic test relay and the alarm circuits. A schematic circuit diagram of the automatic carrier test is shown in figure 2. The automatic carrier test relay contains a main and an auxiliary sychronous-motor-driven cam shaft, and an alarm setup relay element. The motors receive energy from the capacitor potential devices connected directly to the line and the main motor operates continuously, driving its cam shaft at one revolution per hour and operating three separate sets of contacts at 20-minute intervals. The auxiliary motor drives its cam shaft at the rate of four revolutions per minute but it is controlled by one set of contacts on the main cam shaft in such a manner that it only travels one revolution, in two steps of approximately one-half revolution each, for each revolution of the main cam shaft. A set of contacts actuated by the auxiliary cam shaft causes the carrier transmitter to operate for a period of 0.5 second and the only function of the auxiliary motor and cam shaft is to obtain this very short operation of the carrier transmitter, which could not be reliably obtained with a contact actuated directly by the slower-moving main cam shaft. Twenty minutes after the operation of the first set of contacts on the main cam shaft the second set of contacts picks up the alarm setup relay element which locks itself in, then 20 minutes later the third set of contacts on the main cam shaft completes the alarm circuit unless the alarm setup relay element has been released in the meantime by operation of the receiver test unit in the carrier pilot relay.

The main cam shafts on the automatic test relays at the two ends of a line circuit are set 180 degrees or 30 minutes apart and always remain in the same relative positions since they are always energized and de-energized together, thus carrier is transmitted from the remote end of the circuit midway between the operation of the second and third set of local contacts and no alarm will be given unless the carrier signal is not received locally. The receiver test unit has a pickup setting that is 25 per cent greater than the receiver blocking unit so that the gradual reduction of the received carrier signal which might be caused by the falling off in emission of a tube will give an alarm before actual failure has taken place.

The portable telephones can be plugged in either at the relays or at the carrier sets and a transfer circuit is arranged so that the portable telephones can be set up at the carrier sets on one line but the carrier sets on the other line used for communication. The circuits are so arranged that the operation of the carrier test, either manual or automatic, or the use of the portable telephones will not in any way interfere with the correct operation of the relays should a fault occur while any of these were in operation.

The carrier sets are contained in double-walled thermal-insulated metal cabinets which are mounted on the switch rack near the coupling capacitors. These sets consist of a line tuning and safety panel, a transmitter-receiver shelf, and a power rectifier shelf. The transmitter is a single-tube master oscillator with a push-pull type amplifier and has an output of approximately 25 watts. The receiver uses a single three-element tube of the same type used in the oscillator and amplifier. All tubes are biased to cut off when no signal is being transmitted or received. Grid bias voltage is obtained directly from the station control battery and the filament and plate supply is obtained from motor generators which are driven from the control battery.

Copper-oxide rectifiers are used as valves in the tripping circuits of all oil circuit breakers, as shown in figure 3, to prevent false tripping in the event these trip circuits should be tied together through the contacts of some of the protective relays which are blocked either automatically or manually. This is believed to be the first time copper-oxide rectifiers were ever used for this purpose, and saves the time of auxiliary tripping relays or the complication of a pallet switch interlock scheme which might be used for the same purpose.

Potential for the relays is obtained from three capacitor potential devices connected directly to each line end at all points except Century receiving station where potential for all the relays except the automatic test relay is obtained from either of the two 132-kv bus potential transformer banks through a manually operated transfer relay. The auto-test relays at this station are supplied from a single capacitor potential device connected directly to each line.

Current is supplied from cascade-type current transformers in the impulse-type oil circuit breakers at all locations except Boulder, where the conventional bushing-type current transformers on the oil circuit breaker bushings are used.

The relay carrier sets are coupled between *A* phase and ground on all circuits using the same coupling capacitors as are used for the potential devices. A wave trap which can be tuned to any frequency from 50 to 150 kilocycles is connected into the line at each end between the coupling capacitor and the station.

A single-pole oil circuit breaker is installed in the delta connection of the tertiary windings on each of the autotransformer banks at Century receiving station. This breaker is for the sole purpose of opening this delta connection in the event of a phase-to-ground fault between the bank and the 132-kv breakers, when the parallel line circuit is out of service, so that sufficient residual current will flow at Victorville to operate the relays at that point. This single-pole breaker is tripped by an inverse-time excess-current relay operated by circulating current in the delta connection. A minimum time setting of approximately 0.3 second will prevent this relay from operating under conditions when it is not necessary and will allow it to operate when necessary in a sufficiently short time to prevent serious damage by the fault. System stability will not be affected by the time of operation of this relay because, under the condition when it is necessary for this relay to operate, the system will have been separated from Boulder power plant and the fault by the carrier-current protection at Century receiving station.

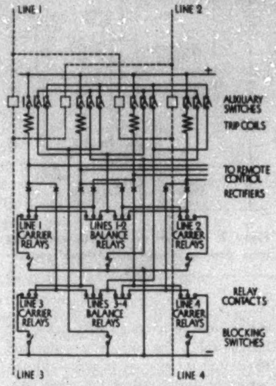

Figure 3. Schematic diagram of tripping circuits showing use of copper-oxide rectifiers

High-voltage leading-current protection is installed on these lines at Century receiving station that will automatically open both lines in the event the lines should be opened at Silver Lake switching station or at Boulder and the leading kilovolt-amperes of the lines should cause high voltage on the receiving-station bus.

Tests

The high-speed current balance relays and the carrier-current pilot relays were all tested at the factory, using two test tables as described by Traver and Bancker[1] to simulate the actual line conditions under which these relays would be required to operate in service. Two of the carrier sets which were to be used with these relays on the line were used in the tests and coupled together through an artificial transmission line. All types of faults, both internal and external as well as out-of-step and surging conditions in which the electrical center of the system fell within and outside of the protected line were tried. Oscillograms were taken of each test and the operations of the relays were entirely satisfactory. As a matter of fact the tripping time of the carrier-current relays was considerably shorter than was required by the specifications due to the development of the so-called one-cycle scheme of carrier-current protection between the time of writing the specifications and the start of manufacture of the equipment.

Installation

Upon receipt of the equipment at Los Angeles each element of the relays was checked to make sure that the adjustments had not been disturbed during transportation. Carrier frequencies between 110 and 150 kilocycles were assigned to the various circuits, an attempt being made to keep as wide a separation as possible between the frequencies assigned to parallel circuits and to circuits terminating in the same stations. The wave traps for each section were tuned before installation and marked for the station, line, and phase in which they were to be installed.

The carrier sets were installed and ready to operate before work was completed on the lines themselves and to save time it was decided to adjust these sets without interfering in any way with the work on the line and to check these adjustments when the line was completed, making any readjustments necessary at that time. To make these adjustments a ground was placed on the line side of the coupling capacitors and a noninductive resistor connected in series with the carrier lead-in wire to limit the output of the transmitters. A resonant-type wave meter was used to set the oscillators to their assigned frequencies and then, using the transmitters as a source, the line coupling and the receiver primary and secondary were tuned to resonance. The coupling of the oscillator transformer and the receiver transformer were adjusted at the same time. After all sets had been adjusted by this method and the lines had been completed a check was made between the two ends of each line circuit, which revealed that operation would have been entirely satisfactory on all circuits without any further adjustments and the only improvement that could be made was a readjustment of the receiver primary tuning, which increased the received signal by a very slight amount. The usual wiring check and over-all tripping tests were made before placing the relays in service.

Operating Experience

The amount of trouble experienced with the protective equipment on these lines is considered remarkably small in view of the size and extent of this project. What trouble has been experienced can be classified under four different headings, (1) vacuum tubes, (2) rectifiers, (3) wave traps, and (4) switching transients.

(1) VACUUM TUBES

This trouble is hardly worthy of mention in that out of a total of 60 vacuum tubes used in the relay carrier sets, one rectifier tube has failed in a year of continuous operation.

(2) RECTIFIERS

Potential restraint for the phase-fault detector elements of the carrier-current relays is secured from the line potential devices through full-wave copper-oxide rectifiers. This is done to secure pickup characteristics of these elements which are practically independent of phase angle and to reduce the potential burden of the relay. Shortly after the line was placed in service these rectifiers failed in one of the relays. The failure occurred at a desert switching station which was the highest voltage point of the line under the operating condition at that time and where the ambient temperature was high, although not unusually so for that location. Upon investigation it was found that these rectifiers at all locations were operating at temperatures higher than were desirable and additional ventilation was provided by portable electric fans as a temporary remedy and no further trouble was experienced. New rectifiers of the same type but with 28 per cent more plates were installed and a resistor, which was the principal source of heat in the relay case where the rectifiers are mounted, was replaced by an external resistor. Since this change was made the equipment has been in operation through a summer season without undue heating of the rectifiers and no further trouble has been experienced.

(3) WAVE TRAPS

The line wave traps used with the carrier relays were tuned to resonance at the assigned frequency by a group of fixed condensers and a variable condenser which was used to obtain capacities in between those that could be obtained by different grouping of the fixed condensers. Shortly after the beginning of the lightning season along the line failure of carrier was indicated on one circuit by the operation of the automatic carrier test alarm. Upon investigation the trouble was located in one of the line wave traps. Flashover had occurred between the plates of the variable condenser and had eventually built up a globule of the plate material which shorted the condenser and detuned the trap. This same trouble occurred on several other traps at various times and in each case was revealed by the automatic test equipment within a few minutes after it had occurred. As a temporary expedient the short was removed with a file in each case and the trap returned to service. As a permanent cure the variable condensers were replaced by a small variable inductance in series with a group of fixed condensers and this has apparently completely eliminated the trouble.

(4) SWITCHING TRANSIENTS

When switching operations are carried out on these lines quite large transient currents flow. The only time these transient currents have caused trouble with the relays is when a line circuit is picked up at normal voltage or when an unloaded circuit is paralleled with a circuit that is carrying load. All trouble experienced has been with the ground relays. Numerous oscillograms have been taken which reveal that these transients have frequencies from 120 cycles to upward of 700 cycles per second with or without a d-c component. The d-c component appears when an unloaded circuit is paralleled with a loaded circuit and also when a circuit containing the autotransformers is energized. The high-frequency transient appears in practically all cases but is apparently suppressed when the d-c component is present.

The minimum pickup of the balance current ground relays was increased from one ampere to two amperes at all locations and in addition at Century receiving station these relays were further desensitized for a short period of time after paralleling the two circuits by energizing the potential restraint coils from the control battery when one or both circuits are open, and removing this potential restraint automatically approximately two seconds after the lines are paralleled. This method has apparently cured the trouble in so far as the current balance relays are concerned, and since it introduces no objectionable restrictions to their operation on faults is considered as permanent.

The minimum pickup of the ground-fault detector elements in the carrier-current pilot protection was increased from 1 ampere to 3 amperes for the starting, or *A* detectors, and from $1^1/_4$ amperes to 4 amperes for the tripping, or *B* detectors. No trouble has been experienced since these settings were increased and they are still sufficiently low to clear all ground faults; however, this method is objectionable because under some minimum conditions the time of clearing will be extended longer than is desirable.

A transient filter has been developed for use with the ground tripping fault detector that will effectively block all frequencies of 120 cycles and above, as well as d-c transients, from the fault-detector coil without materially affecting the pickup characteristic of the detector on frequencies from 35 cycles to 85 cycles. These filters are being manufactured at the present time and when installed will permit resetting the ground-fault detectors to their original pickup values. No filter will be used with the starting-fault detector since there is no objection to its operation by the transient currents.

During the extremely severe flood conditions of March 2, 1938, one of the twin-circuit towers on the Victorville-Century section of the lines was washed out, causing a fault first on the north line and six minutes later on the south line. The north line was cleared by the balanced current protection at both Victorville and Century in a total time of $3^1/_2$ cycles. The south line was cleared by the carrier-current protection in $5^1/_2$ cycles at Century and $10^1/_2$ cycles at Victorville.

The fault on each line was single-phase-to-ground and the longer time required at Victorville was due to the ground current being only slightly above the pickup settings of the ground-fault detector elements. If the transient filters described above had been installed at this time and the pickup settings of the ground-fault detectors restored to their original values, the time required for clearing the south line at Victorville would have been considerably reduced and would probably have been of the same order as the time at Century.

These faults, as well as numerous other faults which have occurred on the system at various times, have demonstrated the ability of the carrier-current protection properly to block tripping on through fault conditions.

During the oscillographic investigations of the switching transients simultaneous records at the two ends of a 90-mile circuit were obtained in the following manner: Load was being carried over one circuit between Boulder and Victorville and records of the terminal conditions of the loaded circuit at both Boulder and Silver Lake and the Silver Lake terminal conditions of the dead Silver Lake-Victorville circuit were desired at the instant of energizing the Silver Lake-Victorville circuit at Silver Lake. A six-element oscillograph was used at Boulder, and two oscillographs, a nine-element and a three-element, with their drums mechanically tied together, were used at Silver Lake, giving six elements for each line terminal being considered. A contact on the drum shafts at each station was used to sound a buzzer once for each revolution of the drums The control circuit for the oscillograph shutter and oil circuit breaker closing at Silver Lake were arranged so that by closing a single contact both functions would occur in the proper order to obtain a record of the change in line terminal conditions when the breaker contacts closed. The control circuit for the oscillograph shutter at Boulder was arranged in the same way so that all that was necessary was to synchronize the film drums at the two stations and close the initiating contacts in both stations at the same time. The synchronization of the film drums was accomplished by placing the buzzer at Boulder near the microphone of the carrier telephone; the operator at Silver Lake was then able to hear both buzzers and adjusted the speed of the Silver Lake film drum until both buzzers sounded at the same time.

The operation of the initiating contacts at the same time was accomplished by taking the carrier-current supervisory control equipment out of service and using the receiver relay at both Boulder and Silver Lake to close the initiating circuits. When all was in readiness the operator at Silver Lake synchronized the film drums and then pressed a button, which started the local supervisory-control carrier transmitter.

The carrier signal being received at both stations simultaneously, closed both initiating contacts at the same time. The drum speeds used gave a total record of approximately 30 cycles and not a single shot was missed because of this method.

References

1. A Faster Carrier Pilot Relay System, O. C. Traver and E. H. Bancker. AIEE Transactions, volume 55, June 1936, pages 688–96.

2. A Single Element Polyphase Directional Relay, A. J. McConnell. AIEE Transactions, volume 56, January 1937, pages 77–80 and 113.

Discussion

Abe Tilles (University of California, Berkeley): I am certain that I speak for all present in expressing thanks to the Department of Water and Power for their broadmindedness in making public these interesting details regarding the few items of trouble that have occurred.

A number of questions come to mind regarding this outstanding transmission project. I am not sure to which member of this Department of Water and Power team of experts each of these questions falls, but I believe they will be able to sort them out without difficulty.

It was mentioned that the sending-end voltage of the line is being altered as the load varies. To what degree of refinement has it been found desirable to do this? Is the voltage setting changed manually by the operator in accordance with the load schedule, or is the voltage regulator initially adjusted to give continuous compounding of the generator voltage? How long does it take to put a generator on the line from a standstill? To what extent is automatic synchronizing equipment in use at present? What is the procedure in energizing the line?

It was mentioned that control and relaying equipment for the contemplated third line will fit along side of, and be similar to, the existing equipment. Is this the case at the switching stations as well as at Boulder? How far physically will the third line be from the others? Will it be generally similar and divided into sections? With three lines in parallel complications arise in a scheme of high-speed parallel protection. How, in a general way, will the three lines be protected? If the receiving third of the third line is not directly in parallel, can the carrier-controlled relaying on that section be sped up?

It was mentioned that tube failures in the carrier relay circuit act to operate an alarm. Is my understanding correct that the worst possible condition the failure of the carrier relay equipment can produce is the tripping out of a line section on a through fault? Has this ever happened?

It would seem that a failure in the tube circuits of the supervisory control equipment at the control point, if it occurred in just the right place at just the wrong time, might cause serious operating difficulties. What are the most drastic consequences which it is supposed might on rare occasions occur? How long would it take to isolate and replace such faulty tubes, etc.?

The performance expected of this project was of such an unusually high caliber, and many of the technical features were so far developed beyond previous practice, that it is no small achievement for these gentlemen to be able to say that this transmission project is living up to their expectations. To single out one item for example, the lightning record of the line is worthy of attention. Here, at last, is a line that "can take it." Three years of lightning, including a large number and variety of recorded direct strokes have not produced a single insulation failure. This is what was expected. True. It is none the less awe-inspiring to contemplate the proved fact.

These gentlemen of the Department of Water and Power of the City of Los Angeles are, indeed, to be congratulated on the results of their labors.

K. B. McEachron (General Electric Company, Pittsfield, Mass.): Those who are concerned with the protection of transmission lines from the effects of lightning are naturally much interested in learning of the excellent results obtained with the Boulder Dam transmission line as described by Mr. Cozzens. The experience records obtained add materially to the existing knowledge of lightning. I am especially interested because most of the lightning data obtained thus far have been secured in the East, and yet the results from the Boulder Dam transmission line agree very well in general with what would be expected, based on results obtained in the eastern part of the United States. This tends to support the general conclusion that lightning is not particularly different in the western part of the country than it is in the East. If the general conclusions can be substantiated, it will make the large amount of data already obtained of greater value, since it becomes more widely applicable. There is no doubt but that the severity of lightning storms are related to meteorological conditions, which are of course different in different parts of the country, which consideration would lead one to expect to find variations with respect to localities widely separated. These variations, if they exist, may however be covered up by other variations which have not been recognized.

Mr. Cozzens used a stroke surge impedance of 200 ohms, which, at the time the line was designed, and still is, a value used frequently when making line flashover calculations. I believe that considerable doubt surrounds the use of a surge impedance for a stroke channel, but the results obtained using values ranging from 200 ohms to 400 ohms have been reasonable, and experience seems to show that the answer obtained using such surge impedances is about right.

I do not believe, however, that the mechanism of discharge is at all similar to the discharge of a charged capacitance through a conductor. In the case of outdoor lightning, the cloud consists of charged drops of water or particles of ice separated by an air dielectric. At the time a stepped leader leaves the cloud to progress toward the ground in a series of steps, charges of opposite sign to that of the base of the cloud are accumulated on the ground These charges, usually positive in sign, because the cloud seems to be usually negative, can move in toward the point to be struck with little voltage drop where conducting objects like counterpoises are available for carrying the charges. As the negative leader progresses, requiring often as long as 0.01 second to reach the earth, it produces a space charge around its own channel.

When the downcoming leader is within a few hundred feet of the ground, the increase in field intensity on the surface of the earth is often sufficient to cause a concentration of charge at the point to be struck of a magnitude which forms streamers which may extend several feet from the earth. These streamers increase in length as the downcoming leader approaches, until the leader makes contact, presumably with the longest streamer, after which the return stroke takes place between the earth and the cloud.

McEachron and McMorris ("The Lightning Stroke: Mechanism of Discharge," *General Electric Review*, October 1936) have suggested that the current builds up to a maximum during the time occupied by the step leader in traversing the last few hundred feet from the earth. The maximum current occurs at the time of contact, and as far as the current wave through the stricken object is concerned, the wave tail takes place during the return stroke. If this return stroke fails, as it sometimes does, to reach the cloud, it is possible to calculate the length of the tail, knowing the rate of propagation of the return stroke. On the other hand, if the return stroke reaches the cloud, the length of the tail will be dependent upon the conditions in the cloud. Additional charge may be available, or new regions tapped in the cloud, giving rise to a long tail or to a multiple stroke.

With this conception, the rate of rise of current is dependent upon the amount of charge in the leader and its rate of propagation. The maximum current is dependent upon the amount of charge and its availability on the ground at the point struck. The tail of the wave results from the flow of charge from the earth into the channel created by the leader.

Data are not yet available to support completely the foregoing, but nevertheless sufficient data are at hand to indicate strongly the probability of the mechanism indicated. Propagation data on a large number of strokes are available, and the existence of streamers up from the ground has been identified through photographic evidence. Much remains to be done, and work is actually progressing, which should add materially to the information available as to just what does happen at the instant of contact.

Mr. Cozzens has commented on the presence of corona in the ground around the counterpoise wires. There is no doubt that corona and sparks exist when the potential of the counterpoise is high enough. Impulse tests on ground rods of fairly high resistance earth have shown reductions of the order of 40 per cent in resistance compared to the low-voltage measurements. Corona, coupled with a series and multiple gap effect, no doubt played an important part in this reduction.

Under the heading "Operation and Measurements," Mr. Cozzens discusses the size and character of the burn on the tower gap in relation to the magnitude of the current flowing through the gap. There is no question in my mind but that considerable information can be obtained in this manner. However, I do wish to point out that there is considerably more to this picture than the current amplitude. In the studies of lightning to the Empire State Building (*EE, Dec. '38, p. 493–505*), I describe a type of stroke which I have called "continuing," which is really a d-c arc between the cloud and the ground. In one case, a current of the order of 250 amperes or more flowed for a time of 0.51 second or slightly longer than a period corresponding to 30 cycles of the usual power-frequency supply. This particular stroke had a superimposed peak of 13,000 amperes together with many smaller peaks. These results are obtained from an oscillogram of the stroke.

Laboratory tests show clearly that the type of burn to which Mr. Cozzens referred as resembling a 60-cycle burn can be caused by a continuing stroke. Burns showing beads are not as a rule associated with high-current short-time discharges but with the longer time discharges, which will, in general, be of much lower magnitude, although they may have high superimposed peaks. This situation, of which I have obtained oscillographic evidence in which the explosive effects of high-current short-time discharges are combined with the burning effects of low-current long-time discharges tends to confuse the story to be obtained from burns on gaps, at least in some cases.

Mr. Cozzens also suggests the possibility of multiple strokes moving across a distance of 265 feet from one tower line to the other. Such a possibility seems very remote to me in view of our latest data. The maximum time of discharge of which I am aware is 1.53 seconds obtained in 1937 in New York City. It would require a 118 mile wind and a stroke duration of 1.53 seconds to affect both lines from the point of view of blowing the discharge path from one circuit to the other. Such a combination of high wind at right angles to the line, and of maximum duration, is extremely doubtful. There is one other possible explanation, but it too is not likely to occur. I have photographic records of two multiple strokes which changed their paths on the ground end between successive strokes a distance of several hundred feet, apparently due to change in local ionization conditions. This phenomenon is most unusual, which might be invoked to explain the results obtained by Mr. Cozzens. It seems more likely to me that the strokes were successive in the same storm but not in the same stroke.

P. L. Bellaschi (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): One feature which distinguishes this investigation is that Mr. Cozzens has availed himself of a number of suitable and simple devices to measure the lightning currents to the line. From each, one or more characteristics of the lightning stroke are recorded and from the combined recorded effects a more complete appraisal is possible both in regard to the current amplitude, the duration, and also other characteristics of the discharge.

The writer has long advocated that advantage must be taken of all suitable means if lightning phenomena are to be investigated on a comprehensive basis. The magnetic link has shown its usefulness in recording the crest of the current—an important factor to know in lightning protection. Surface burning of metal parts, such as the tower gaps on the Boulder Dam line, and in addition fusion of metal links and other similar means contribute to determine the current amplitude and the nature of the discharge.

For example, test results obtained at Sharon during the past year have established that currents in the order of a few thousand amperes sustained several thousand microseconds produce concentrated fusion effects similar to that in figure 2*B*. As it already had been suspected a few years ago, this fusion apparently results from such current components being present in the discharge of certain lightning strokes. The concentrated arc and sustained arc drop of these current components thus produce fusion effects resembling those of the power arc.

In this connection the photographic studies during the past five years by Doctor B. F. J. Schonland and his associates ("Progressive Lightning—III," *Proceedings of the Royal Society*, September 1937) clearly show that the high current, which results from the main stroke discharging from earth into the established channel and the main branches, takes place in some 100 microseconds. This intense discharge is followed by a sequence of moderate current components extending into hundreds and even thousands of microseconds. The source of these current components seems to reside largely in the cloud and are due to a continuation of the lightning discharge in the cloud proper. While visual accuracy may be questioned, it is apparent to those who have closely observed lightning that some of the prolonged discharges are in the nature of low currents sustained in some instances in the order of one second. In analyzing the fusion effects account must furthermore be taken of multiple lightning strokes.

Tests made on aluminum sheets will illustrate the distinct character of the surface fusion produced respectively by the intense current of short duration and by the moderate current components sustained for a much longer time. Currents of 125,000 amperes having a total duration of some 400 microseconds produced surface burning concentrated in a $^{5}/_{8}$-inch diameter circle quite similar to those in figure 1. It is estimated that the metal was fused in the test about 0.005 inch below the surface. Tests made on a 0.035-inch aluminum sheet with a 330-ampere current of 108,000-microsecond duration (interrupted in $6^{1}/_{2}$ cycles) burned a $^{1}/_{8}$-inch diameter hole practically through the sheet. In addition four smaller spots were fused in the sheet.

E. H. Bancker (General Electric Company, Schenectady, N. Y.): There are several items in Mr. Draper's paper that strike me as worthy of comment. The first of these is the great amount of careful preliminary engineering which was done in connection with the transmission line problem for this project. Under the heading "Specifications" Mr. Draper gives an operating time for the protective system that was written into the specifications, without any statement of how and why these figures were reached. It seems to me that it might interest many of you to know that if a relay system having these characteristics had not been available, it would have been necessary to have constructed a third transmission line to carry the same amount of power through short circuits with maintained stability. This is probably an extreme case illustrating the economic benefits that may result from improved relaying. By the expenditure of a few thousand dollars in relay equipment, the cost of a several million dollar transmission line was deferred until a higher load condition was reached.

The second item of interest is the statement under "Specifications" that the protection should be immune to operation on an out-of-step or surging condition. Under ordinary circumstances the balanced current relaying will do most of the fault clearing, but obviously each section must have some form of single-line relaying. It may not be appreciated that in this instance, the ability to refrain from tripping is a most important requirement for the single-line relaying. If this were to trip on the power swings following the operation of the balanced relays, the tie between the Boulder Plant and Los Angeles would be lost. This requirement ruled out as back-up protection for single-line operation even such simple things as time-overcurrent relays, because even they would notch closed on successive power swings. It is rather curious, therefore, that the principal requirement of part of the protective relaying system is not that it shall trip, but that it shall refrain from tripping. This was a performance not obtainable with any other kind of relays, except pilot protection.

The chief item of interest to me in this paper is the operating experience. It is in actual operation that the little unexpected kinks and wrinkles develop and must be ironed out. The experience with the tubes is not unusual, but merely confirms that of other utilities having carrier equipment. Some of the other experiences were rather novel, particularly the operation of the ground relays when a section of line was picked up by closing an oil circuit breaker. Every one knows that the three poles of a circuit breaker do not close at exactly the same instant, but with types of relaying in service and at voltages previously used, the effect of the non-simultaneous pole closing was not noticed in any relay action. In this case the unusually high voltage and length of the line section resulted in a residual current that was big enough, lasted long enough, and was in the right direction to cause the ground relay to operate to trip. The solution of bypassing the high frequency that resulted from these switching operations around the tripping relay coil is merely another example of the high order of engineering that has gone into the design and maintenance of the equipment on the Boulder line.

The methods devised for synchronizing the oscillographs at two stations and tying them in with the closing of a circuit breaker was most ingenious and highly effective. I have seen a number of the records taken in this way and can vouch for the success in recording a large number of electrical quantities at widely separated points with a high enough oscillograph film speed so that individual cycles can be studied and accurate time records of the performance of different parts of the equipment made.

Another interesting item is the use of copper-oxide rectifiers as check valves in trip circuits. As Mr. Draper says, this is probably the first practical use that has been made of this scheme. While it may seem a little thing, again it has considerable economic importance because it permits energizing the circuit breaker trip coils without the wasted time needed to operate an auxiliary relay which would partially undo the good accomplished through the purchase of three-cycle circuit breakers.

In conclusion, I should like to acknowledge publicly the fine spirit of co-operation exhibited by Mr. Draper and his associates during the investigation and correction of the troubles mentioned under "Operating Experience." When users and manufacturers work together with such a fine mutual understanding of each other's problems an amicable and satisfactory solution is reached quickly and easily.

R. W. Sorensen (California Institute of Technology, Pasadena): These papers, together with certain other papers included in the symposium, present to the Institute membership the results of almost two years of operation of the present world's outstanding supervoltage power line. The data as presented again exemplify the mastery of electrical phenomena which competent electrical engineers have. In spite of many unusual storm conditions and new features involved in this line, the engineers who planned, designed, erected, and operated it have so completely analyzed all the problems involved and so well anticipated transient outages which have occurred as to make possible this first two years of operation without any very serious interference with the power supply for a great community of people.

When we stop to consider the fact that coupled with all the features of engineering which have been named there was also a change of frequency program involving a change from 50 cycles to 60 cycles for the entire system, we certainly would be remiss if we did not compliment the Bureau of Water and Power staff on its outstanding achievement. In reciting this well-earned tribute we must not fail to note the courage shown by these men in going to a voltage of 287.5 kv when, without additional expense or probably at even less expense, the same amount of power could have been transmitted over 230-kv lines, the construction and operation of which has become more or less standardized.

The lightning protection for the line and the way it has functioned has been so complete and satisfactory that there are few points leading to any discussion. It is interesting to note that a considerable portion of this line—although we generally consider California a nonlightning country—passes through a territory subject to lightning storms, the number of which approximates the average number occurring in many parts of the United States. Indeed the data presented indicate that the severity of these storms is rather high and it would be difficult to overestimate the thoroughness with which Peterson and Cozzens have analyzed the lightning problems and the effectiveness of the measures for guarding against lightning outages which they have included in their line design.

The corona experience on the Boulder Dam line as discussed presents some interesting features in showing a difference between corona loss values as made on sections of line crossing the desert areas and the preconstruction corona loss measurements made in the Ryan Laboratory. Without doubt both sets of measurements are correct and we have before us the striking situation of an increased corona loss in desert areas over that anticipated. I do not think any unexpected corona loss has ever been noticed on the considerable number of miles of 230-kv lines which for years have operated in similar desert areas. There is a possibility that certain sections of the 230-kv lines which traverse desert areas to some extent might show corona loss over that found in nondesert areas, which simply has not been observed because, as the authors have pointed out, the measurement of corona loss on an actual transmission line traversing many kinds of country at different altitudes is no easy matter and none of the lines—the one under discussion or the 230-kv lines which I have in mind—has any great amount of kilowatt-hour corona loss. On the other hand, it may be that the conditions which are essential for the operation of 287.5-kv lines; namely, large diameter conductor and this higher voltage, introduce factors which are not so noticeable at 230 kv and hence do not demand the attention at 230 kv which must be given them at perhaps 287.5 kv and higher—should we in the future raise our transmission voltages to some higher value.

It is interesting to comment here that 10 years elapsed between the installation of 150-kv lines and 230-kv lines—the maximum potential up to the installation of the 287.5-kv lines—and that 15 years have elapsed since the 230-kv lines went into operation; and now we have 287.5-kv lines.

To return to the unexpected excess corona loss on the desert, I will just present for our consideration a report of my observations concerning corona phenomena which may be noted any time in the high-voltage laboratory at the California Institute of Technology. We have installed in the laboratory a smooth bus $3^1/_2$ inches in diameter. With voltages approaching 300 kv applied to this bus, we note that while the complete bus does not go into corona, trees or brushes of corona and sometimes quite definite brush discharges will be noted at points along the bus. These discharges seem to start at places of very slight irregularity or dust accumulation on the bus, even though we sometimes think we have the bus all clean. The difficulty of keeping the bus free from these discharges has led us in our laboratory experiments to use for most of our conductor pieces of chain such as are used for window weights, door checks, etc. Naturally these pieces of chain go into considerable corona at voltages much lower than the initial corona on the $3^1/_2$-inch bus. The corona formed along the chain, however, soon builds up an ionized layer of air around the chain which serves as a shield around the chain and seemingly holds the total corona loss to an amount no more troublesome than the corona which forms on the large bus. In fact, the corona along the chain seems to be limited almost entirely to regular corona glow rather than the brush discharges which occur on the bus. We have never gotten sufficiently interested in the phenomena thus observed to make any measurements. These phenomena, however, in connection with the unexpected phenomena mentioned in the paper lead me to suspect that small irregularities on large conductors, particularly smooth-surfaced ones, may result under some conditions in corona phenomena quite annoying and difficult to eliminate. Whereas the 230-kv lines which have come under my observation, and which are all stranded-conductor lines, as I have stated, have never shown this phenomenon on the desert to an extent which has led us to believe there is any difference in corona loss on the desert and in the laboratory which cannot be accounted for by the usual things which affect corona; namely, humidity, temperature, barometric pressure, conductor surface irregularity, etc.

Bradley Cozzens: The discussions presented by K. B. McEachron and P. L. Bellaschi always add new or confirming facts to any paper because of the very intimate and continuing contact that these men have with lightning studies and associated phenomena. Specifically referring to Mr. McEachron's comments: we too, are pleased to have data indicating that the magnitude of current and possibly voltage encountered in western lightning is not materially different from that of eastern lightning, for it was upon much of the data gathered regarding eastern storms that the lightning design of the Boulder Dam lines was based. From what observation we have been able to make in the few years of construction and operation it appears that the number of strokes per storm may be materially less than for the eastern locations, possibly due to the higher average cloud height and also the smaller amount of actual water vapor involved in any one average storm.

As mentioned in the paper, surge impedance values can be calculated that show a wide variation in magnitude depending upon assumed conditions. We agree heartily with Mr. McEachron in his criticism of the use of a lightning stroke as being a path of definite surge impedance short-circuiting the condenser between the cloud and earth. It is indeed a much more complex circuit. The use of the 200-ohm value, however, gives a design that will cope with some of the most severe lightning strokes while the higher values would of course be more representative of the less intense discharges. Perhaps with further accumulation of information Mr. McEachron or Mr. Bewley, on whom we lean quite heavily for methods of computing lightning phenomena, may evolve methods that will take into account the resistance or impedances between the various charge masses in the cloud to account for the relatively slow build up of the current in the streamers. Such information might modify greatly the present methods of computation, but while we realize that present methods are not rigorous, they are still the only methods available to most of us.

Mr. McEachron mentions that the maximum current is dependent upon the availability of the charge on the ground. The use of the counterpoise makes this charge easily available from a much larger area than normally. It is still questionable whether an extensive counterpoise system may not some day feed such heavy currents into the lightning strokes that the voltage drop in and immediately adjacent to the tower will become a factor. This would be first evidenced on the lower voltage lines.

It is indeed gratifying to have definite confirmation of the formation of corona around the buried counterpoise wires. Our only information, other than reported reductions in measured surge impedance with the application of high surge voltages, was the observed phenomena of corona forming about a conductor in salt water when surged with high impulse voltages. This corona area around the counterpoise was used in the design computations and with Mr. McEachron's comments we are more certain of its justification.

While we had no oscillographic records of the duration of any of the lightning strokes to the Boulder Dam lines, yet mention is made in the paper that from the types of burns observed on the counterpoise gaps, some were probably of short duration while others must have been the result of long-time low-current discharges or many multiple strokes occurring in rapid succession. Mr. McEachron's recently recorded data on the long-time low-current strokes confirms the assumptions mentioned in the paper. Knowing this now to be the case, the information from the counterpoise gaps will be much more valuable for from observing many of them it is possible by careful observation to detect multiple discharges, with appreciable time interval between them by the overlapping of the burned areas. The single-discharge high-current value can be detected relatively easily, and the long-time low-current value also can be identified. Of course these are not instrument measurements and should not be treated as such, but they will add information I feel sure.

Mr. McEachron's comment on the impossibility of a multiple stroke to follow the same general path and travel the 265 feet between tower lines in his maximum observed time of 1.53 seconds is of course correct. However, the change of path for the lower portion of strokes does not seem to be quite as uncommon as indicated. A very beautiful example is given in the United States Department of Commerce Bureau of Standards publication No. 95 on protection of electrical circuits and equipment against lightning. Figures 1 and 2 show the still and moving film records of a multiple stroke in which the lower section entirely changed path for one or possibly more of the discharges. Again we have no definite proof that this was the case for the strokes involving both of the Boulder lines, but as mentioned previously, the desert storms are usually high clouds and are moving rapidly so that successive discharges usually progress with the storm except where the storm clouds are being blown over a mountain area. It is felt, therefore, that strokes to both circuits in a single area come either as this diverted multiple discharge or within a very short time interval.

We greatly appreciate Mr. McEachron's comments on this paper.

The comments of Mr. Bellaschi add further evidence by laboratory tests indicating that burns that result in appreciable fusion of the metal must be the result of long-time low- or moderate-current discharges. The few values mentioned by Mr. Bellaschi covering some of his recent tests indicate that the study of the area and volume of burned metal as a function of the current and time involved is just begun if it is to be used extensively as an indication of lightning current. It, definitely, is a rough means of indication if not interpreted too rigorously.

J. D. Laughlin: Discussions and questions as asked by Abe Tilles bring out points and information additional to that contained in the original paper, as the reader may see the subject from a different angle to that presented by the writer.

The following discussion, in answer to Mr. Tilles' questions relative to starting generators at Boulder and energizing the Boulder transmission line and his questions relative to the consequences of tube failures in the supervisory control equipment, gives some additional information pertaining to the operation of the transmission line not included in the original papers.

Starting a generator from the condition with the butterfly valve closed and all auxiliaries shut down normally requires approximately 15 minutes to put the unit on the line. Ten minutes of this time is used in opening the butterfly valve and checking the generator auxiliaries. Before starting any unit, all auxiliaries are operated manually, then shut down and set for automatic operation. Included in the auxiliary check are devices for supplying bearing oil pressure, governor oil pressure, generator cooling water and transformer cooling water, generator field breaker, overspeed devices, and position of the butterfly valve. When these checks are completed and reported to the operator in the control room, he closes the master starting switch. Within two minutes all auxiliaries have been started automatically and the generator is up to full speed. The operator then adjusts the generator voltage and selects the oil circuit breaker to be used for synchronizing. Actual synchronizing and closing of the circuit breaker is done automatically. From the time the generator is up to full speed until it is on the line requires approximately two minutes. If the butterfly valve is open and the scroll case is full of water and it is desired to get a machine on the line quickly, this can be accomplished in approximately four minutes by omitting the auxiliary inspection.

The generators are always synchronized to the lines, using oil circuit breakers on the generator busses. Automatic synchronizing equipment is used for putting all machines on the line for normal operations. Manual synchronizing is used only on special occasions, as for tests or when some equipment is out of service.

When both circuits are de-energized and it is desired to connect Boulder to the system at Los Angeles, two generators are synchronized on the low voltage bus and adjusted for one-third normal voltage. The line is then picked up, one 90-mile section at a time without any further adjustment of the generator fields. When all six sections are energized but not connected to the system, the generator voltage at Boulder is approximately 75 per cent of normal, and at the Los Angeles end of the line the voltage is 100 per cent of normal. The line is then synchronized with the system at Los Angeles. The synchronous condensers for voltage regulation at Los Angeles are connected to transformer banks fed from the 132-kv bus and may be put on the system either before or after the Boulder line.

A second method has been used for picking up the lines but it is more hazardous to equipment than the one described above. For this method, all circuit breakers on the lines are closed except those that connect the lines to the system at Los Angeles. Two generators are brought up to normal speed and connected to the lines without any field excitation. The generator fields are then closed and excitation is increased slowly until normal voltage is obtained at the Los Angeles end of the line. This method is not used due to the difficulty in regulating generator excitation so as to maintain synchronizing current between the machines and not get the line voltage too high. If one circuit is energized and one de-energized, and it is desired to put the second circuit into service, at least two generators must be on the line at Boulder. The dead line is then picked up one section at a time starting from the Boulder end.

Regarding the failure of tubes in the supervisory control equipment, the most serious result that could occur from a tube failure would be a failure of a nature that would overload and damage other equipment in the carrier-current circuits, such as condensers, transformers, or wiring; however, the circuits are protected with fuses in such a manner that this condition is very improbable. Usually when a tube burns out it stops the transmission or reception of a code and prevents the completion of the intended operation. In no case could it cause a false operation. When such a condition occurs, it is immediately detected at Boulder. The operator can usually determine at which station the failure occurred by noting at what point in the code sequence the equipment stops. He then calls the attendant at the station in trouble by telephone and has him check his equipment. The replacement of a faulty tube is a matter of a few minutes after it fails. The failure of a tube in any station except at Boulder does not interfere with the operation of the balance of the supervisory control equipment. Operations can still be performed at the other stations, and indications received. In case of a tube failure at Boulder, the entire equipment is out of service, since codes from all other stations must be recorded and checked at Boulder before the intended operations can be completed.

L. L. Draper: I would like to express my appreciation for the very generous comments of Mr. Tilles and Mr. Bancker on this paper and will endeavor to answer Mr. Tilles' questions regarding the relays in a general discussion rather than trying to answer each question individually.

Each section of the third circuit will be equipped with carrier-current pilot protection like that in use on the present circuits. With this type of protection each section of each circuit is protected as an individual unit and the worst possible condition that the failure of carrier to be transmitted or received can produce is faulty tripping of that particular section upon the occurrence of a through fault condition. As mentioned in the paper the carrier-current pilot protection used is considerably faster than was anticipated at the time the specifications were written and is sufficiently fast to meet the required clearing time under all conditions without any additional speeding up.

As Mr. Tilles has stated complications arise in a scheme of high-speed balance current protection for three parallel lines. This is particularly true with the ring arrangement of six oil circuit breakers as will be used at Boulder, Silver Lake, and Victorville. For this reason and also because balance current protection cannot be applied to the section of the third circuit between Victorville and Los Angeles consideration is being given at this time to other types of protection that might be used as a second line of defense on these lines. The final decision as to the second set of protection equipment on these lines will be to use the overall scheme that will give the least probability of an undesired separation between Boulder power plant and the Los Angeles system due either to failure of the relays to trip a faulted line section or faulty operation of the relays on line sections that are not faulted.

Mr. Bancker has brought out the principle reason for the short operating time requirements of the specifications in so far as this line is concerned, however, there are other well-known benefits to be gained by fast clearing of faults one of which was very well illustrated by the system load dispatchers report of the two faults that occurred on these lines on March 2, 1938. In each case the report contained the very terse statement "No disturbance to voltage." Actually of course there was a disturbance to the system voltage, but of such short duration that from all practical standpoints the statement was absolutely correct.

Mr. Bancker attributes the high-frequency transient currents, that caused trouble with the ground relays, to the nonsimultaneous closure of the oil circuit breaker poles and while this was probably a contributing factor, I believe that the predominating factor in the production of these high-frequency currents was the point on the voltage wave at which individual poles of the breaker closed and that they would be produced on these lines, even with simultaneous closure of the breaker poles.

Wm. S. Peterson: The engineers who have been associated with the Boulder Dam transmission line development deeply appreciate the interest shown in this project and are sincerely grateful for the many fine comments that have been made in discussing this group of papers.

In reply to Mr. Tilles' question regarding the alteration of sending-end voltage as the load varies, it can be stated that the operation is very closely in accord with operating under the condition for minimum loss. The normal voltage of the generator is 16,500 volts to produce 287,500 volts at the sending end of the line. This voltage is used when delivering approximately 200,000 kw at the receiving end of the two circuits. For delivering the rated output the voltage is increased to 17,000 volts and for very light loads, under 75,000 kw, the voltage may be reduced to 15,500 volts. This change is made by manual adjustment of the regulator settings. This type of operation requires slightly less synchronous-condenser capacity than does fixed-voltage operation.

Mr. Tilles asks about the location of the third circuit. For two-thirds of the way, it is parallel to the existing circuits and is about 265 feet from such circuits. From Victorville to the Department's receiving station *E*, the line diverges from the existing line going directly west instead of southwest.

Doctor R. W. Sorensen has made the statement that the same amount of power could have been transmitted, without additional expense, at 220 kv.

In such comparisons account must be taken of the type of service, or the conditions, on which the line is rated. Two 220-kv circuits of similar length if operated within the criteria set up for a steady-state stability basis of operation would have about the same rating as do the two 287.5-kv circuits on a transient stability basis. However it was the purpose in building this line to achieve a reliability of transmission equivalent to that given by a local steam-electric generating station. It therefore had to be rated on a transient stability basis for withstanding, without loss of synchronism, a two-phase-to-ground fault at the worst point on the system, drop a section of line and still have a margin of 25 per cent as a safety factor. Calculations show, that, in order to achieve the same rating with 220 kv, it would require the use of three circuits. Three circuits of 220 kv of a given standard of construction are more expensive than two circuits of 275 kv of a similar standard. Economy in obtaining reliability dictated the choice of 275 kv.

Turning to the corona discussion by Doctor Sorensen, I am inclined to agree with his observation that it may not have been observed because measurement of loss on actual lines is difficult to make and it takes accurate measurements to determine the differences under discussion. The phenomena has been identified as having relation to locality, so it is possible that other lines are in areas not affected by the same factors as are present in some places along the Boulder lines.

In making the corona-loss measurements at Victorville the voltages for the conductors under test were carried as low as 220 kv. For these low voltages, the "voltage shifts" in the corona-loss curves for the desert area from those run at Stanford on the same conductors are of the same order of magnitude as found at the higher portion of the loss curves. These measurements were on conductors having diameters of 1.25 and 1.4 inches for the smooth surface type and 1.4-inch diameter for the stranded type. It is reasonable to suggest that if the smaller diameter conductors associated with 220-kv lines had been tested at the same location that a similar shift would have been found and that it would have given appreciable loss differences because 220 kv would have been nearer the "knee of the curve" for such conductors than those tested.

The Electric Strength of Air at High Pressure

By H. H. SKILLING
MEMBER AIEE

Synopsis: Experimental data show that the strength of compressed air as an insulator increases with pressure until a critical value is exceeded. At higher pressures the sparking voltage fails to rise, and may even drop. The maximum voltage and the critical pressure are largely dependent on the shape of the electrodes of the spark gap.

OVER thirty years ago work was done by Ryan[1,2,4] and his student, Ekern,[3] to determine the electrical strength of air at pressures of many atmospheres. It was discovered that the voltage required for sparking between points bears a roughly linear relation to the air pressure until the voltage has risen to approximately ten times that required for sparking between the same points, at the same spacing, at normal atmospheric pressure. A maximum voltage is reached at an air pressure of between 100 and 200 pounds per square inch. As the pressure is further raised, the sparking voltage does not rise correspondingly—in fact it drops somewhat if sharp points are used, and then at still higher pressure (above 400 pounds per square inch) the voltage rises slowly with pressure.

More recent work has been done by a number of scientists: some of this work is summarized by Whitehead.[11] The latest to be published is by Goldman[12,13] of the Union of Socialist Soviet Republics. Goldman's work will be mentioned again in a later paragraph.

Renewed interest in the electrical properties of air at high pressure has appeared recently. This may be traced to the present use of compressed gas as insulation in radio transmitter condensers, in electrostatic generators, and its proposed use in high-voltage power cables. The present paper is a report of an experimental study of the subject.

The chief contribution of the present paper is a study of the effect of the shape and material of electrodes. In previous investigations Ryan and Goldman worked with points, and Goldman, Amyashi, and others, worked with plane surfaces or surfaces with large radius of curvature. It is believed that no one has heretofore investigated both of these types of electrodes and also the intermediate region of electrodes of various radii of curvature. In the investigation of electrode material, electrodes of copper, iron, and zinc were used.

The present investigation also gives assurance that the results are not seriously influenced by distortion of the electric field, brought about by proximity of the walls of the enclosing chamber. Previous work has been done in pressure chambers of relatively small size.

Apparatus

The pressure chamber in the present work has an inside diameter of six inches. This permits the study of longer gaps and larger electrodes than were used by Ryan, including electrodes of spherical and cylindrical form. However, the increased diameter of the test chamber makes it advisable to use a relatively low maximum air pressure, and since the most essential and characteristic sections of Ryan's curves appeared at pressures below 300 pounds per square inch, the present apparatus was designed for that pressure. Glass and porcelain insulation is used, and voltages lie in the same range as that studied by Ryan.

The outside of the air pressure chamber is shown in figure 1. Two sections of six-inch steel pipe are separated by a sheet of glass, and the cylinder thus formed is closed with a six-inch extra-heavy cast-iron blind flange at each end. The sheet of glass is 14 inches square, with a four-inch round hole in the center. This cylinder, with the insulating sheet of glass and the two blind flanges, occupies a central position in figure 1; it is the test chamber, and air is pumped into it through a pipe inserted in the lower flange.

The cylinder is made to retain air under pressure by being clamped between a pair of eight-inch I-beams, as shown in figure 1. Just above the pressure cylinder is seen a cast-iron spacer block, and between that and the upper beam is a small steel roller for equalizing forces. By tightening screws beneath the standard suspension-insulator units at the ends of the beams, the beams are drawn together. This exerts an axial force on the cylinder, and a compressive force on the glass of about 10,000 pounds. The insulator units, together with the glass sheet, completely insulate the upper beam and the upper half of the cylinder from the lower beam and the lower half of the cylinder. Leads from a high-voltage transformer are connected to the two beams.

Within the pressure chamber is the small frame shown in figure 2. The vertical members of the rectangular frame are insulating, and electrical connection is provided from the top and bottom of the frame to the upper and lower blind flanges, respectively. Some of the electrodes used appear in the photograph. Sharp points, spheres, and cylinders were used, of different materials, and with various sizes and spacings.

Sixty-cycle alternating voltage was used in all tests. A 300-kv, 150-kva transformer was used to supply the test voltage. Power was supplied to the transformer from either the sine-wave generators of the Ryan Laboratory or the commercial supply mains; even in the latter case the wave form does not vary from a sinusoidal wave by a significant amount. Voltage was adjusted by variation of the generator field (when the generator was used) and by means of an induction voltage-regulator in the transformer primary circuit. The voltage was measured by means of a tertiary voltmeter winding, known to be correct.

The location of the spark within the chamber, and the relative intensity of the spark, could be observed through the insulating glass sheet. In this way it was known that the spark occurred between electrodes, as desired. In one test the spark took place, due to moisture, along the surface of the glass within the apparatus; this was immediately detected and corrected. It was not possible, however, to see anything of the form or nature of the discharge.

Results

Experimental results are plotted in figures 3, 4, 5, and 6. These curves indi-

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Paper 38-117, recommended by the AIEE committees on research and power transmission and distribution, and presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938. Manuscript submitted May 5, 1938; made available for preprinting July 20, 1938.

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The author gratefully acknowledges the aid given by N. E. Bradbury of the department of physics, Stanford University, in interpretation of results, and of J. S. Carroll of the Ryan High-Voltage Laboratory in the experimental work.

1. For all numbered references, see list at end of paper.

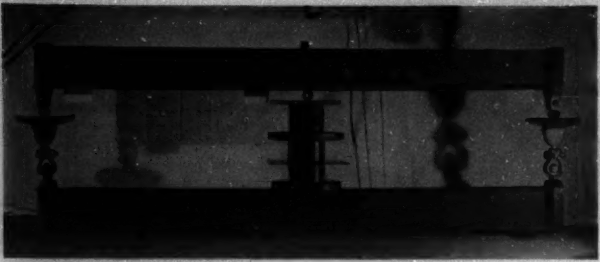

Figure 1. The test chamber for holding compressed air

cate average sparking values, each measurement being repeated five to ten times. As an indication of the consistency of results it may be well to consider a sample of the work in detail. Curve *A* of figure 5 is drawn through a number of small circles which represent the average experimental points. At each pressure used the sparking voltage was measured some five or ten times, and the average value is marked by the circle. Individual measurements of voltage were likely to vary by as much as ten per cent; for example, the point on curve *A* at 290 pounds per square inch pressure represents an average of eleven voltage readings that varied from 37.8 to 41.0 kv. Despite this variation of individual measurements, the average values are consistent and reproducible from time to time.

When the electrodes are sharp points, as in figure 3, a maximum value of voltage is evident at about 100 pounds pressure. As pressure is increased from 100 to 150 pounds, voltage for sparking declines. At higher pressure the voltage may or may not rise again, depending on sharpness of the points; at pressures above 100 pounds the voltage required for sparking is greatly increased by dullness of the points (see figure 6). These results are similar to those obtained by Ryan;[3] Goldman,[12] producing sparks between a pointed electrode and a plane, gives voltage-pressure curves of the same shape but with 30 to 50 per cent lower voltage values.

Sparking voltage values in the high-pressure range are erratic compared to sparking voltages at pressures below 100 pounds. This suggests a different mechanism of spark formation. Also, in the higher pressure range there were almost invariably, discharges between electrodes at voltages five to ten per cent below the "sparking voltage" plotted in the curves. These preliminary discharges were of momentary duration, and failed to initiate a complete breakdown of the gap. (They sometimes carried enough current to actuate a circuit-breaker in the primary circuit of the high-voltage transformer, so that it chattered but did not trip. The plotted "sparking voltage" is the voltage at which the breaker opened. The height of the curves obtained would vary a few per cent with changes of the speed and setting of the breaker. Therefore, for consistent results, the setting was kept always the same.)

In figure 4 are shown curves of sparking voltage for one-inch-diameter spherical electrodes (steel balls from a ball bearing). Considering the spacings used these are large spheres, and the electric field between them in the region of nearest approach is almost uniform. (The maximum field strength is increased three per cent by curvature of the spheres.) Since the electric field near a sharp point is highly divergent, the pointed and spherical electrodes represent opposite extremes of electrode configuration.

It will be seen from figure 4 that between spherical electrodes the sparking voltage increases with pressure in a more or less linear fashion until a pressure of about 250 pounds is reached. At higher pressures the sparking voltage remains constant, within the experimental range. The effect of pressure above 300 pounds on the sparking voltage could not be determined because of limitations of the apparatus. Other investigators[1,11,13] show similar flattening of the voltage-pressure curve, but at higher values of voltage and pressure. Although all investigators show the flattening effect, there is little agreement as to the voltage and pressure at which it occurs. There is the possibility that the flattening of the curves of figure 5 is merely a point of inflection, and that voltage would again rise with large increase of pressure. The author, interpreting sparking at high pressure as due to field current, believes that different investigations give different results because of unlike conditions of electrode surfaces. The importance of this factor will be considered later.

Figure 5 shows the gradual change of form of the voltage-pressure curve as the radius of curvature of the electrodes was reduced. All of the curves of that figure apply to spark gaps of 0.05-inch length. Curve *A* was obtained with large steel spheres ("large" in the sense that the radius was nearly ten times the gap length). The practically identical curve *B* is for cylindrical zinc surfaces with large radius of curvature. Curve *C*, for small steel spheres, with radius approximately equal to the spacing between them, is noticeably different; and curve *D*, obtained with electrodes of copper wire spaced nearly twice their radius, shows further difference of the same nature. As the radius of curvature is decreased the curves are less sharply flattened at 250 to 300 pounds pressure, and the voltage required for sparking in that pressure range is reduced. Curve *E*, for a smaller iron wire, and curve *F* for a smaller copper wire, are further altered in the same manner. Curve *G* represents two sets of data that were practically identical: they were obtained with electrodes of copper wire in one case, and with blunt steel points in the other. The radius of the copper wire was $^1/_{10}$th the gap length (0.010-inch diameter); the steel points as viewed under a microscope were rounded at the tips due to the original sharp point having been burned to a blunt end about 0.015 inch across.

The same dull points were used for one of the curves of figure 6; the spacing between them having been increased to 0.30 inch. The two other curves of that figure correspond to increased sharpness of the same points, the "sharp" points being freshly ground and showing, under the microscope, the marks of the abrasive wheel. These three curves continue, in a sense, the series of curves of figure 5, for they complete the transition from "large" spheres to sharp points. Through the entire series the voltage maximum at about 250 pounds pressure becomes less apparent, and a maximum at about 100 pounds pressure appears as characteristic of sharp points. Curve *G* of figure 5 is the first to show a pronounced hump in the 100-pound region.

The various electrodes used for obtaining the curves of figures 4 and 5 are of such simple geometrical shape that the surface electric gradient corresponding to breakdown could be computed. (The electrodes for curve *G*, however, were omitted because corona probably produces space charge about them before sparking voltage is reached.) The maximum surface gradients obtained, corresponding to the flattened parts of the curves at high air pressures, are as follows:

	Maximum Gradient for Sparking (Crest Kilovolts per Centimeter)
Gap length 0.05-inch between:	
Steel spheres, 1-inch diameter	450
Steel spheres, 0.125-inch diameter	540
Copper wires, 0.063-inch diameter	510
Iron wires, 0.041-inch diameter	470
Copper wires, 0.025-inch diameter	530
Zinc, radius of curvature 0.5 inch	450
Gap length 0.017 inch between:	
Steel spheres, 1-inch diameter	550
Gap length 0.075 inch between:	
Steel spheres, 1-inch diameter	450

Three electrode materials were used. They were copper, iron (and steel), and zinc. Zinc was used because its low work function contrasts with those of copper and iron. Steel and zinc surfaces gave almost indistinguishable voltage-pressure curves. The average of the maximum gradients for copper is a little higher than the average for iron (520 kv per centimeter, compared to 490 kv per centimeter) but this difference is probably not significant. The author's conclusion is that electrode material has little if any effect in the impure state in which it must necessarily exist during electric discharge in compressed air.

Discussion

A dash line, *H* in figure 5, is drawn to indicate the voltage that would be required for sparking if Paschen's law were valid in this pressure range. Paschen's law expresses breakdown voltage as a function of the product of air density and linear dimensions of the gap,[5] and therefore implies that ionization results from impacts of electrons (or ions) that have been accelerated in the gas.[10] It is seen that the law fails to apply at pressures of several atmospheres, and breaks down utterly at about twenty atmospheres. There can be little doubt that the reason for this anomalous behavior is that a source of ionization which is relatively ineffective at low pressure becomes predominant at high pressure. Moreover, the source of ionization that is predominant at high pressure appears to be nearly or entirely independent of pressure. It seems therefore that sparking at high pressure derives most of its ions or electrons from a source that is independent of the gas in the gap.

Field current, or cold-emission current, is quite commonly found to flow between electrodes in high vacuum when the potential gradient is of the order of magnitude of half a million volts per centimeter.[6,7] Residual gas about the electrodes takes no part in this phenomenon, for the mean free path of electrons in the gas may be many times the gap length. The gradient required for field current is greatly dependent on the surface conditions of the electrodes, the necessary gradient being raised by a factor of two or three or more when the surfaces are polished and de-gassed.

Field current can thus appear between electrodes in vacuum when there is so little gas that it plays no part in the discharge. It seems quite reasonable that field current can also appear between electrodes in high-pressure gas, the gas being so compressed that it takes no part (or only a secondary part) in the discharge. If the mean free path of electrons in the gas is so short that an electron will rarely attain ionizing velocity even though the gradient is raised to the high value required to produce field current, then field current may be expected to flow. This is the most probable cause of the electric breakdown that occurs at high pressure between large, smooth electrodes. It has been so accepted by Ryan and Goldman.

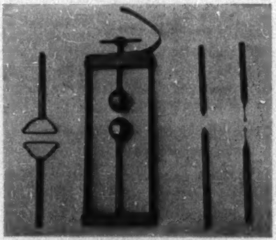

Figure 2. Some of the spark gaps tested

Ryan, in 1911, wrote,[1] "When the stress delivered from the metal electrode surface to the adjacent zone of gas is great enough some of the free electrons in the metal will de detached from the electrode and will migrate through the gas to the anode." Goldman, in 1938, agreed,[13] saying, "Starting from a certain pressure the mechanism of gas puncture is altered. Impact ionization no longer plays a predominant part. With those fields as are obtainable at high voltages, electrons may be torn out from the cathode by the action of the field."

It must be understood that there is no proof that the anomalous sparking voltages at high pressure are due to field current, but there is evidence to support such a belief. The best evidence is that in those cases for which field strength can be computed, the electric field just preceding sparking is of the right order of magnitude to give plentiful field current. Work in vacuum shows (as mentioned above) that an electric gradient of the order of magnitude of 500 kv per centimeter will draw appreciable field current from a metal surface that has not been carefully cleaned, de-gassed, or polished. Compton and Langmuir say,[6] "The past history of the surface has a very great effect on the field strength needed to draw electron currents," and Millikan and Eyring[7] give values varying from 400 to 1,100 kv per centimeter as producing appreciable electron currents from a tungsten filament. Compton and Langmuir found that surface impurities were extremely important, and after saying that "Field currents are obtained much more easily from surfaces covered with adsorbed films of electropositive materials," they report that the result of one test "suggests that traces of alkali metal escaped from the glass and are held by a monatomic oxygen film on the tungsten."

In view of the extreme sensitivity to surface condition it is not surprising that experiments in high-pressure air give breakdown at different values of gradient. It was shown in the preceding table that the gradient believed to produce field current in the present work varied from 450 to 550 kv per centimeter. Goldman obtains a value[13] for a gap of practically the same dimensions, of 730 kv per centimeter in nitrogen, and Amyashi[11] gives data indicating a maximum gradient of about 800 kv per centimeter in both nitrogen and air. All these values are well within the range of gradients determined by work in vacuum. It is evident that when a metallic surface is being subjected to electric discharge in an atmosphere of compressed air it can remain neither highly polished, chemically clean, nor de-gassed. Traces of alkali metals will no doubt always be present. Probably, therefore, the production of field current will depend far more on the presence of unavoidable impurities than on the material of the electrodes. This is consistent with experimental data.

It should be mentioned that it is only in the region of flattening of the pressure-voltage curve that different investigators disagree. When the voltage is less than half that of the flat part of the curve

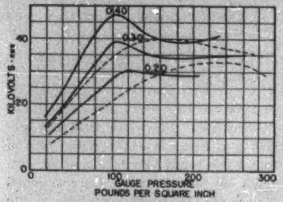

Figure 3. Sparking voltage between points at various spacings

Length of gap in inches is marked on curves. Dash lines show Ryan's results for comparison at the two shorter spacings

there is agreement between the present work and that of Goldman and of Amyashi, and between all three and Paschen's law.

The production of electron current by high surface gradient, like the production of electron current by impact ionization, commences gradually and then increases rapidly with increasing applied voltage. Field current in vacuum has been found[6] both theoretically and experimentally to be related to gradient by the equation

$$j = aE^2\epsilon^{-D/E}$$

wherein a and D are constants, j is current density, and E is electric field strength. It is evident that field current is negligible at low values of voltage and that, as current becomes appreciable, it increases tremendously with a small increase of voltage.

The electrons of field current, once released into the gas between electrodes in high-pressure experiments, will travel from cathode to anode and will multiply themselves by impact ionization as they go. Even a small supply of field-current electrons will thereby disturb the normal mechanism of sparking in gas, and this may account for the reduction of sparking voltage below that indicated by Paschen's law at intermediate pressures (see, for instance, figure 5). As pressure is increased, and the surface gradient preceding sparking becomes higher, the effect of field current on the spark becomes more pronounced, until finally it is the controlling factor and breakdown voltage is thereafter independent of pressure. The formation of a spark by means of field current probably includes intense heating of a very small spot on the cathode.

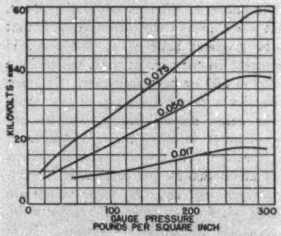

Figure 4. Sparking voltage between spheres of approximately one-inch diameter, at various spacings

Length of gap in inches is marked on curves

Shape of Electrodes

Experimental data may be considered in four classifications, with indefinite intermediate regions. The four basic categories are:

1*A*. Spark due to ionization of gas; not preceded by corona discharge.

1*B*. Spark due to field current; not preceded by corona discharge.

2*A*. Spark due to ionization of gas; preceded by profuse corona discharge.

2*B*. Spark (see below) preceded by profuse corona discharge.

These classifications correspond to the following physical conditions:

1*A*. Flat or rounded electrodes; low pressure.

1*B*. Flat or rounded electrodes, high pressure, or pointed electrodes in very high pressure.

2*A*. Sharp-pointed electrodes, not close, low pressure.

2*B*. Sharp-pointed electrodes, not close, medium pressure.

Characteristic of classification 1*A* are the curves of figure 4 at pressures below 100 pounds. No explanation is needed, for this type of sparking is that of spheres in open air. These same curves at pressures above 250 pounds illustrate action of type 1*B*: the field strength at the sphere surface rises with increasing voltage until emission of electrons produces field current in sufficient quantity to appear as a spark.

The curves of figure 3, at pressures below 90 pounds, represent sparking of the 2*A* type. This is the customary mechanism of sparking between points in free air.[8] The same curves at pressures of 120 to 150 pounds, and possibly higher, illustrate the 2*B* mechanism of sparking. This requires further consideration.

One is inclined to attribute flattening of the voltage-pressure curves for pointed electrodes, as in figure 3, to the effect of field current emitted from the point. Goldman and Wul's results,[12] however, indicate that such is not the case. Working with direct voltage applied to a gap between one pointed and one plane electrode they found that the typical shape of the a-c voltage-pressure curve (as in figure 3) is reproduced when the pointed electrode is positive, but with the point negative the sparking voltage is higher and continues to rise with pressure through the experimental range (up to 200 pounds pressure). This indicates that the action that causes sparking voltage to diminish as pressure rises must take place at or about the *positive* point, and it clearly cannot be the emission of electrons. Accepting the data of Goldman and Wul, one cannot do better than to accept also their explanation that the anomalous sparking voltage between *points* is due to high concentration of space charge, which, in turn, is the result of low ion mobility in high-pressure gas. This is the 2*B* type of action.

When pressure is further increased the extent of the corona that precedes sparking is curtailed so greatly that the action enters an intermediate region between 2*B* and 1*B*, and at the very high pressures used by Ryan[1] it appears that a

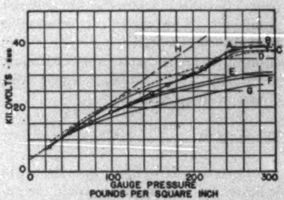

Figure 5. Sparking voltage between various electrodes, gap length in each case being 0.05 inch

Electrodes were:

A. (Heavy line with circles to indicate average experimental voltage values) Steel spheres, 0.933-inch diameter

B. (Dot-dash line, practically coincident with *A*) Zinc cylindrical surfaces, radius of curvature 0.5 inch

C. (Light line) Steel spheres, 0.125-inch diameter

D. (Dotted line) Copper wires, 0.063-inch diameter

E. Iron wires, 0.041-inch diameter

F. Copper wires, 0.025-inch diameter

G. Copper wires, 0.010-inch diameter, and also points with blunt tips about 0.015 inch across

H. Paschen's law (for plane surfaces, extrapolated from low pressure region) fully 1*B* type of action is attained even between sharp needle points.

The curves of figure 6 show varying degrees of similarity to the typical behaviors 2*A*, 2*B*, and 1*B*. With blunt points the maximum voltage at about 100 pounds pressure tends to disappear, while the maximum at about 300 pounds becomes more prominent.

Conclusions

1. Sparking voltage in air rises as air pressure is raised above atmospheric, but a maximum value is reached. As pressure is further increased, sparking voltage remains constant or becomes less. Maximum sparking voltage is greater than sparking voltage at atmospheric pressure by a factor of five to ten, depending on the electrode configuration.
2. No difference was observed between iron, copper, and zinc as electrode materials.
3. The form of electrode is very important. Electrodes that permit corona before sparking behave quite differently from those that do not.
4. If there is no corona preceding sparking, maximum voltage is reached at 18 to 20 atmospheres pressure (or higher, according to other investigators).
5. If there is corona preceding sparking a maximum appears in the sparking voltage at 7 to 10 atmospheres pressure, and another maximum appears at 20 atmospheres or higher.
6. Sparking between smooth electrodes at high pressure is probably due to field current; the result, that is, of an electric field so strong that "some of the free electrons in the metal will be detached from the electrode and will migrate through the gas to the anode."

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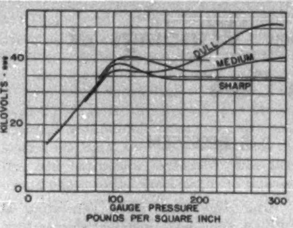

Figure 6. Sparking voltage between points of various degrees of sharpness

Gap: 0.30 inch

Dull: rounded tip, about 0.015 inch across
Medium: rounded tip, about 0.005 inch across
Sharp: freshly ground point, less than 0.001 inch across

Discussion

D. W. Ver Planck (Yale University, New Haven, Conn.): One of the important consequences of Professor Skilling's excellent paper is that it helps to establish a limit for Paschen's law. For instance in figure 5 of the paper, it can be seen that the sparking voltages for substantially uniform field begin to deviate from Paschen's law, curve *H*, at about four or five atmospheres absolute pressure.

In this connection attention should be called to the recent work of Finkelmann published in Germany (*Archiv für Elektrotechnik*, volume 31, 1937, page 282). Finkelmann working with uniform fields and about the same pressure range as Professor Skilling extended his tests to much higher spacings and voltages, and to carbon dioxide, nitrogen, and hydrogen as well as air. His results for air show that the pressure, or more correctly the density, at which deviations from Paschen's law commence increases with the gap length. Thus the limiting pressure increases gradually from Professor Skilling's five atmospheres at a gap of 0.127 centimeter to about eleven or twelve atmospheres at 0.7 centimeter. In the light of Professor Skilling's contention that the deviation is caused by removal of electrons from the cathode by direct action of the electrostatic field (so-called field current), a view also expressed by Finkelmann, it might be better to define the limit of Paschen's law in terms of the potential gradient, the quantity directly causing field current. Doing this one finds that as the gap increases from 0.1 to 0.7 centimeters, the gradient at which the deviations commence is not constant either, but increases from somewhat less than 150 to something over 300 kv maximum per centimeter.

While the data are still too few to finally establish a limiting condition for Paschen's law, its correctness for determining air density correction factors following the method recently given by the writer (AIEE TRANSACTIONS, January 1938, page 45) and even for extrapolation up to pressures of several atmospheres receives further confirmation from these new researches.

It is unfortunate that Finkelmann terminated his uniform field curves too soon after the deviation from Paschen's law to show the approach to a limiting spark-over voltage at high pressure found by Professor Skilling. Finkelmann does however give results for concentric cylinders which show this phenomenon. For a gap of about 0.9 centimeter in air and a field which is only slightly nonuniform the gradient for the constant spark-over voltage is about 240 kilovolts maximum per centimeter, or only about half that observed by Professor Skilling. On the other hand, some results reported many years ago by Hayashi (*Annales de Physique*, Volume 45, number 4, 1914, page 431) for gaps quite similar in geometry to those of Professor Skilling show very much higher limiting gradients of the order of 1,000 kv maximum per centimeter. These facts are consistent with Professor Skilling's statement that various experimenters do not agree as to the heights of the level portion of the sparking voltage-pressure curves. Evidently determining factors in this phenomenon have not been brought under control.

H. H. Skilling: I am in thorough agreement with the discussion by Professor Ver Planck, and particularly wish to emphasize his conclusion that more data are needed. It is, of course, evident that there is room for all the work that has been done at atmospheric pressure to be repeated at many different pressure levels. The present paper is of the nature of a progress report, and it is hoped that further data can be published from time to time as they become available. The author is now extending his work to longer gaps and higher voltages.

In response to an unpublished discussion, it should be mentioned that the usual experimental procedure was to pump air into the test chamber until the pressure was somewhat in excess of 300 pounds per square inch. A valve in the supply line was then closed, and as the temperature of the enclosed air, heated by compression, approached room temperature, the pressure fell to about 300 pounds. After each measurement of sparking voltage, some of the air was released and the valve was again closed; the air was cooled by expansion, but it returned to room temperature in two or three minutes. It was not necessary to wait for room temperature to be reached before testing, as the *density* of the enclosed air could not change; the pressure gauge was read, however, after its indication had reached a steady value.

Generator Damper Windings at Wilson Dam

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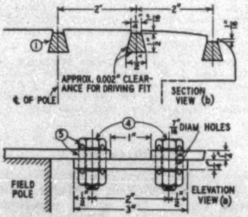
Synopsis: The majority of water-wheel generators now in service have been built without damper windings. Recent technical investigations and laboratory tests have indicated that, in addition to reducing oscillations and improving system stability, damper windings are very effective for reducing high-peak voltages during unbalanced short circuits. This applies particularly under switching conditions of clearing a fault when the load is disconnected from the distant end of a transmission line, and the charging current results in capacitive loading of the unfaulted phases. The existence of similar overvoltage conditions was recognized by the Wilson Dam operating staff and later confirmed by oscillograms during tests. This paper summarizes the studies and tests based on the observed overvoltage conditions and describes a method for adding damper windings to large water-wheel generators which are already installed. Short-circuit tests are extended to generators on a large scale which confirm the earlier theoretical and laboratory tests and demonstrate the advantages to be gained by adding damper windings to other water-wheel generators now in service. The tests show that damper windings in one generator when operated in parallel with another generator without dampers are effective in reducing high-peak voltages during unbalanced faults. Data are given to show the changes in machine reactances due to adding damper windings.

FOR many years amortisseur, or damper windings, were included in water-wheel generators only when they were required to meet special conditions such as high starting torque for automatic station service, synchronous condenser operation, or system-stability requirements.

The large water-wheel generators at Wilson Dam were installed during 1925 and 1926 without damper windings. The existence of an overvoltage condition during disturbances on an interconnected system was recognized by the Wilson Dam operating staff. At the suggestion of an interconnected company, overfrequency relays were installed for the purpose of disconnecting overspeeding generators although justification of this type of protection was not supported by records obtained from frequency and voltage recorders over a period of several years.



ITEM	BILL OF MATERIAL DESCRIPTION	FOR ONE GENERATOR NUMBER REQUIRED
1	$\frac{1}{2}''\times\frac{1}{2}''\times\frac{1}{4}''\times 50\frac{1}{2}''$ LONG COPPER BAR	360
2	$\frac{1}{4}''\times 1\frac{1}{4}''\times 12\frac{3}{8}''$ LONG COPPER BAR CUT TO FIT	144
3	$\frac{1}{4}''\times 1''\times 3''$ LONG COPPER BAR	144
4	$\frac{3}{8}''\times 1''$ HEXAGONAL-HEAD MACHINE BOLT	288
5	$\frac{1}{32}''$ STEEL KEEPER WASHER	288

It was expected by local plant engineers that the final explanation of this condition would be obtained during other tests with an oscillograph. During the latter part of 1935 a six-element oscillograph was purchased for use by the electrical testing laboratory. During March 1936, staged tests were made to determine certain relay and equipment characteristics. Other tests followed late in the summer. During the first series of tests one phase of a new lightning arrester broke down. This arrester failure together with the results of preliminary tests led to laboratory studies by the manufacturer and the resulting paper by Wagner.[2] The oscillograms indicated that high-peak voltages were present on the unfaulted phase during line-to-line faults on the distant end of a transmission line which had been taken out of service for tests. Peak voltages as high as 343 kv between high-voltage bus and ground were recorded on the unfaulted phase at the sending end of the 154-kv line. Peak voltages of this magnitude are severe on equipment during the duration of an unbalanced fault.

Technical and Design Features

The generator used for these tests is rated 32,500 kva, 0.8 power factor, 12,000 volts, three-phase, 60 cycles, 100 rpm, vertical axis, and the detailed reactances are given in table III. Staged tests without damper windings were also made on a 25,000-kva generator with other data similar to the above machine except it was built by another manufacturer and similar peak voltages were obtained during these tests.

During conferences and correspondence on the subject, several suggestions were given for the prevention of damage to the equipment due to overvoltage.

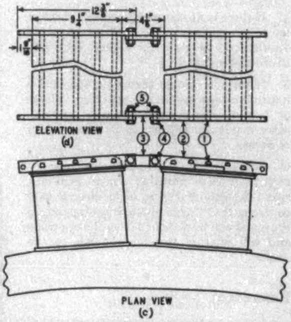


Figure 1. Sketch showing details of damper winding installation

(a)—Details of copper connection between adjacent poles

(b)—Section view of field pole showing location and dimensions of slots and damper bars

(c) and (d)—Plan and elevation view assembly sketches

1. A set of specially designed spark gaps connected between the high-voltage bus and ground and designed to operate with a few microseconds' delay after the appearance of sustained overvoltage.
2. A combination filter and protective gap connected to the generator leads.
3. The installation of damper windings which would effectively limit the magnitude of unbalanced, distorted phase potentials.

Paper number 38-129, recommended by the AIEE committees on power generation and electrical machinery, and presented at the AIEE Southern District meeting, Miami, Fla., November 28–30, 1938. Manuscript submitted September 26, 1938; made available for preprinting November 7, 1938.

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The authors wish to acknowledge the services of the electrical testing laboratory and operating department personnel who made the tests and their assistance in analyzing the results and also the assistance of the Wilson Dam shops in working out some of the details of the winding installation.

1. For all numbered references, see list at end of paper.

4. Continue the present practice of operating the plant with all generators electrically connected to the same bus, except as required by other system operating conditions. It was also recognized that it was desirable to have other balanced loads particularly synchronous motors or condensers connected to these machines.

The first suggestion was rejected because of operating problems involved together with space requirements and cost. The transformers were already provided with protective gaps. The second suggestion was rejected for substantially the same reasons. Since the effectiveness of the first two suggestions was doubtful for this particular application and would not correct the cause, it was believed that the expense could be used to better advantage in correcting the trouble at its source. The third suggestion for the installation of damper windings was considered the most effective because the machine characteristics directly responsible for the overvoltage condition would be corrected and other benefits of damper windings for improving the stability of rotating machines would be obtained. Both generator manufacturers concurred in recommending the use of damper windings as the best solution for the machines of their own manufacture. Published information on this subject was reviewed and technical investigations were made.

Doherty and Nickle[1] stated that the voltage at short-circuit line to neutral may rise to $(2\,x_q'/x_d') - 1$ times the voltage before short circuit on the open phases. See table III for the identification of x_q' and x_d'. This indicated that for a single-phase-to-neutral fault on the generator it was possible to obtain peak voltages 2.65 times the normal voltage across the terminals of the unfaulted phase.

The manufacturer gave some indications of improvement to be obtained by adding damper windings. These values given below represent the ratio of the peak voltage to normal voltage of the unfaulted phase during a line-to-line fault:

Condition	Generator Only	20 Per Cent External Reactance
Continuous damper		
(*a*) maximum flux	1.34	1.2
(*b*) minimum flux	1.17	1.1
Discontinuous damper		
(*a*) maximum flux	1.84	1.5
(*b*) minimum flux	1.42	1.25

This information indicated the expected reduction in peak voltage to be obtained with each type of damper winding.

The paper by Wagner,[2] written after the designs were completed but before the last short-circuit tests, gave some useful information concerning the effect of inductive, pure resistance, induction-motor and capacity loads on the peak voltages, and the effect of damper windings. On page 1389[2] it is also stated that for a terminal-to-terminal fault on an unloaded machine, the crest of the sum of the odd harmonics measured from the sound phase to the short-circuited phases just after short circuit is equal to $(3/2)(x_q'/x_d')$ times the crest of the normal line-to-neutral voltage for a machine without dampers. For a machine with damper windings the factor is $(3/2)(x_q''/x_d'')$. These reactances can be identified by referring to table III of this paper. If the corresponding values for this generator are used, these formulas would indicate a ratio of 2.54 without dampers and 1.64 with the continuous damper which was added. All of the above-calculated values, unless otherwise indicated, apply to only the generator and are in fair agreement when we consider the state of knowledge of the subject and the differences in assumptions for the equations. Other factors, including the type and location of fault, different external impedances, different transmission-line constants, and the part of the reference-voltage wave at the instant the fault occurs (maximum, minimum, or some intermediate flux condition) should be taken into account, and these result in complicated computations.

During the technical investigations, additional information concerning damper windings and generator characteristics was obtained from published articles by Wagner,[3] Kilgore,[4] Wright,[5] Park and Robertson,[6] Linville,[7] and Wagner and Evans.[8] Clarke, Weygandt, and Concordia[9] have reported some additional information which was not available until our tests were completed.

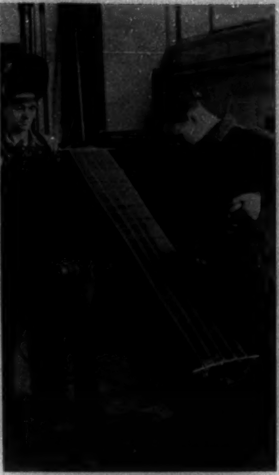

Figure 2. View showing method of swaging copper bars in slots and welding of bars to segments of short-circuiting copper rings

Preliminary information from the manufacturer suggested the use of five round one-half-inch-diameter copper bars in each pole. This was a good theoretical shape of bar which would result in minimum change in normal field distribution at the surface of the field pole. Round bars are very difficult and expensive to install in the field on account of requiring removal of the laminations from the field pole, possible damage to the field insulation, the cost of making a new die, and requiring a punch press. Round bars can therefore be better installed at the factory.

During the design, such items as the equipment available in the shops and in the plant, the classes of workmen available to perform each operation, features which would require dismantling the machine, if this is not desired for other maintenance work; obstructions to be avoided; available space; provision for performing each operation, such as machining, welding, etc.; mechanical re-

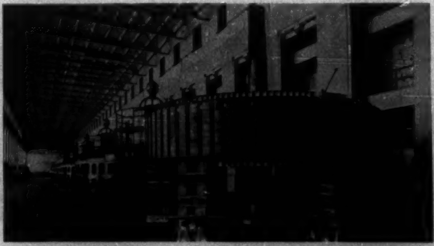

Figure 3. Rotor with field poles and bolted connections between copper segments of adjacent poles nearly completed

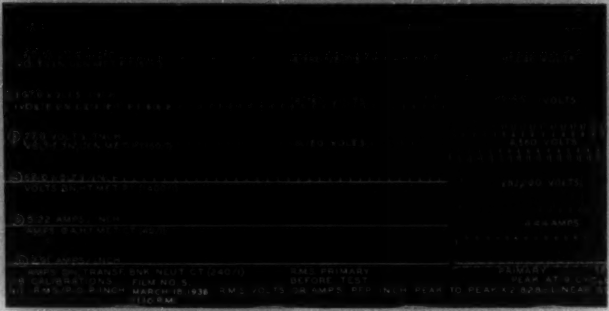

Figure 4. Oscillogram of test number 1

1. Generator phase-*C*-to-neutral voltage
2. Generator phase-*B*-to-neutral voltage
3. Generator phase-*A*-to-neutral voltage
4. Line phase-*B*-to-neutral voltage
5. Line phase-*A* current
6. Transformer neutral current

quirements and factor of safety during maximum overspeed; electrical, insulation, magnetic, and physical requirements, including thermal expansion, etc., had to be considered.

In order to obtain minimum disturbance to the normal magnetic field at the pole face, the one-quarter-inch minimum width of opening into the slot without excessive cost was determined by experiment in the shop. The bars were designed to have approximately the same cross section as a one-half-inch-diameter copper bar. Sketches of the preliminary design for each of the two ratings of main generators in the plant were submitted to the manufacturer of the corresponding machine for comments. The principal changes which resulted were increasing the cross section of copper links between the copper segments of adjacent poles for electrical conductivity and the use of bolted connections instead of using laminated brush copper links welded to the copper bar at each end. Slots at the bolted connection were considered a better provision for thermal expansion and contraction at this location. Sketches of the completed design for this machine are shown in figure 1.

Details of the Damper Winding Installation

This generator had been in service approximately 12 years and the insulation was beginning to show signs of brittleness and other indications of deterioration. Special handling was therefore necessary in order to forestall serious damage to the generator parts during the course of alterations. Each field pole was carefully removed by means of a sling and placed on a truck for transportation from the powerhouse to the shops which was a distance of approximately one-quarter mile. A field pole was first mounted on the bed of a large planer in the conventional manner for cutting the slots, but difficulty was experienced in obtaining uniform slots due to the tendency of the planer tool to "dig in." The scheme selected was a modified milling head attached to the cross-feed of the planer. This equipment was designed and built for this particular application. Each of the five slots were cut in three transverses of the milling head. The angle of the cutter and the position of the planer bed holding the pole-piece was changed for each cut resulting in a slot which has the form of an inverted keystone or dovetail. The field pole was clamped securely, and during the cutting operation it was covered to prevent steel chips from dropping between the windings. The accumulation of chips was blown away by an air jet as required.

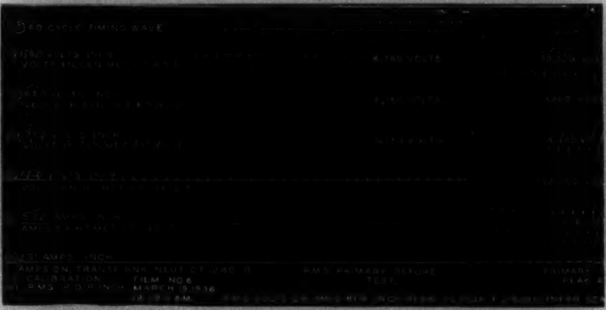

Figure 5. Oscillogram of test number 2

T. 60-cycle timing wave

1. Generator phase-*C*-to-neutral voltage
2. Generator phase-*B*-to-neutral voltage
3. Generator phase-*A*-to-neutral voltage
4. Line phase-*B*-to-neutral voltage
5. Line phase-*A* current
6. Transformer neutral current

The keystone-shaped copper bars were inserted in the slots. Figure 2 shows a cradle structure which was used to support the field poles and also shows how the welding and swaging were done. These operations, of course, were not performed at the same time. The electrical bonding at the ends of the bars was done by means of a carbon arc and "silphos" welding rod. Sufficient clearance was provided between the copper cross-connection and the iron laminations to allow for only differential thermal expansion and contraction. An air hammer was used in swaging the bars to make the outer part of the bars fit more firmly in the slots and prevent noise or excessive vibration while in service.

During the progress of the work, a number of tests were made to check the expansion of the bars while carrying heavy 60-cycle alternating currents. Sample welds and mechanical and electrical tests were made, as required, in order to check the suitability of the installation as well as the electrical and mechanical requirements.

Figure 3 shows the field poles nearly all assembled on the rotor. The copper bars which form the electrical connections between the damper bars of adjacent field poles may also be seen near the upper and lower part of each field pole. These bars are bolted together with steel machine bolts and steel keeper washers are used to prevent them from becoming loose. Each part of the generator was marked before dismantling and the parts were reassembled in their original locations.

The generator rotor and shaft were realigned on being put back together. All parts were cleaned and the coils were treated with insulating varnish during the course of the work. The machine when finally put back in service was in better operating condition than before this work was started.

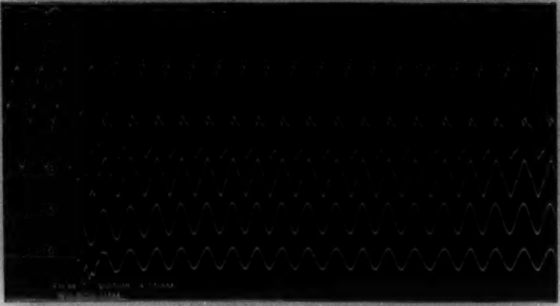

Figure 7. Oscillogram of test number 9

T. 60-cycle timing wave

1. Generator phase-*A*-to-neutral voltage
2. Generator phase-*C*-to-neutral voltage
3. Generator phase-*B*-to-neutral voltage
4. Line phase-*B*-to-neutral voltage
5. Line phase-*A* current
6. Transformer neutral current

Short-Circuit Tests

After the generator was reassembled, short-circuit tests were made to determine the effectiveness of the damper windings. The test conditions were made to correspond with those during the tests made before the installation of damper windings.

The generator windings are connected in wye with the neutral point grounded through a resistor. The low-voltage side of the power transformer bank is connected in delta and the high-voltage side is connected in wye with the neutral solidly grounded. Potential transformers connected wye–wye to the low-voltage bus made possible the measurement of phase-to-phase, or phase-to-ground potentials. The potential transformers of the high-voltage metering equipment are normally connected open delta and these were reconnected to measure the potential between the unfaulted phase of the high-voltage bus and ground. High-voltage short-circuit currents were measured by means of wound-type current transformers located either in the high-voltage metering assembly or in the circuit between the transformer-bank neutral point and the station ground.

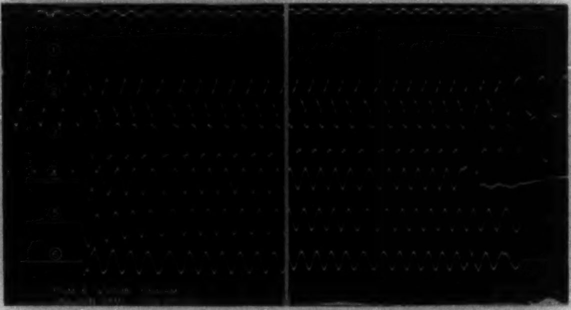

Figure 6. Oscillogram of test number 8

T. 60-cycle timing wave

1. Generator phase-*A*-to-neutral voltage
2. Generator phase-*C*-to-neutral voltage
3. Generator phase-*B*-to-neutral voltage
4. Line phase-*B*-to-neutral voltage
5. Line phase-*A* current
6. Transformer neutral current

Oscillograph records were made of the fault current and the unfaulted phase-to-ground potential on the high-voltage side and the three phase-to-neutral potentials of the generator. Records of field current and other features were taken during some of the tests.

All of these staged short-circuit tests were made on the Wilson Dam to Centerville section of the Wilson-West Nashville 154-kv line. This section of the line was taken out of service and fault connections were made at Centerville, Tenn., which is 68.36 miles from Wilson Dam. The generator combinations were connected to energize the transformer bank at approximately no-load voltage, and the faults were initiated by closing the high-voltage line breaker. Another high-voltage breaker and the low-voltage bus breakers were used for back-up breakers in case of emergency.

Test numbers 1 to 5, inclusive, in table I were made before damper windings were added. Test numbers 6 to 9, inclusive, were made after damper windings were added to generator number 8. During the comparison of results it was observed that tests 1 and 2 were made with 11,500 rms volts between generator terminals; tests 3, 4, and 5 were made at 11,400 volts and tests 6 to 9, inclusive, were made at 10,700 volts. In order to have all tests on the same basis for comparison, the peak voltage between the low-voltage bus and ground before the fault was applied was used as 100 per cent and compared with the corresponding voltages during the fault. The corresponding peak voltage between the high-voltage bus and ground before the fault was applied was obtained by calculation from the transformer ratio and used as 100 per cent for comparison with peak voltages existing during the tests. The transformer ratio was 10,980 volts delta to 154 kv wye. The maximum peak voltage obtained from the oscillogram after the fault was applied was expressed as a percentage of the corresponding initial peak voltage. These percentages were used as the basis for comparison.

These tests were made with no-load voltage on the generators. The fault currents and also the peak voltages during the fault would be higher if the generators had additional excitation with the generators under load. An approximate estimate of the increased voltage peaks due to additional excitation under load could be obtained by using the generator magnetization curve for the corresponding field current under load conditions and using the ratio of the new voltage to the voltages at which these tests were made. An

Table I. Summary of Tests

Test Number	Conditions	Phase	Low-Voltage Bus Phase to Ground Peak Volts (Kv)	Low-Voltage Bus Phase to Ground Peak Volts (Per Cent)	High-Voltage Line Unfaulted Phase to Ground Peak Volts (Kv)	High-Voltage Line Unfaulted Phase to Ground Peak Volts (Per Cent)	High-Voltage Line Fault Amperes at Nine Cycles (RMS)*
1	Generators 5 and 6, no reactors, bank 12, phase-*C*-to-*A* fault	*A*	6.54	69.3			444
		B	20.3	215	303	227	
		C	17.84	189			
2	Generator 6 with reactors, bank 12, phase-*C*-to-*A* fault	*A*	5.48	58.3			236
		B	17.25	183	303	227	
		C	14.74	156			
3	Generator 8, regulator in, no reactors, bank 11, phase-*B*-to-*C* fault	*A*	20.55	220.5	343	261	
		B	17.88	192.0			
		C	5.95	63.8			291
4	Generator 8, regulator in, no reactors, bank 11, phase *B*-*C*-ground fault	*A*	13.27	142.2	196.5	150	
		B	12.61	135.3			
		C	5.95	63.8			251
5	Generator 8, regulator in, no reactors, bank 11, phase-*A*-ground fault	*A*	This film				
		B	no				
		C	good		222.2	170	
6	Generator 8, regulator in, bank 11, no reactors, phase-*A*-to-*C* fault	*A*	5.4	61.8			211
		B	11.5	131.8	206.2	166	
		C	13.63	156			
7	Generators 7 and 8, regulators in, no reactors, bank 11, phase-*A*-*C* fault	*A*	5.09	58.3			325
		B	14.0	160.3	231	186	
		C	14.3	164			
8	Generator 8, regulator in, no reactors, bank 11, phase-*A*-*C*-ground fault	*A*	3.08	35.2			245
		B	10.33	118.2	156.7	126.5	
		C	13.1	150			
9	Generator 8, regulator in, no reactors, bank 11, phase-*A*-ground fault	*A*	8.79	100.5			304
		B	9.14	104.7	148.7	119.7	
		C	13.45	154			

* The current waves are not all perfect sine waves. These root-mean-square currents are therefore approximate.

Table II. Comparison of Test Results

Test Conditions	Bus	Peak Volts Without Dampers Kv (1)	Peak Volts Without Dampers Per Cent (2)	Peak Volts Dampers in One Generator Kv (3)	Peak Volts Dampers in One Generator Per Cent (4)	Maximum Per Cent Volts Decrease With Dampers (5)	Maximum Per Cent Volts Increase Without Dampers (6)
Two generators Phase-phase fault	LV	20.3	215	14.3	164	23.7	31
	HV	303	227	231	186	18.05	22
One generator Phase-phase fault	LV	20.55	220.5	13.63	156	29.2	41.3
	HV	343	261	206.2	166	36.4	57.2
One generator Phase-phase-ground fault	LV	13.27	142.2	13.1	150	5.5*	5.2*
	HV	196.5	150	156.7	126.5	15.65	18.6
One generator Phase-ground fault	LV	Film no good		13.45	154		
	HV	222.2	170	148.7	119.7	29.6	42

NOTE: Designation (LV) above refers to the maximum bus-to-ground peak voltage of the three phases of the low-voltage bus as recorded in table I, Summary of Tests.
(5)—Column (2) minus column (4), expressed as a percentage of column (2).
(6)—Column (2) minus column (4), expressed as a percentage of column (4).
* An increase in voltage at low-voltage bus is indicated, but the high-voltage line shows a reduction.

exact solution would require detailed calculations involving the internal characteristics of the generator.

Figures 4 to 7, inclusive, are oscillograms of test numbers 1, 2, 8, and 9, respectively. The voltage and current values printed on the films of figures 4 and 5 are the magnitudes at nine cycles made during preliminary analyses. The maximum peak voltages used for the comparison of test results are given in table I.

Table I shows the station combinations used for each test, the type of fault, and the resulting peak voltages and per-cent voltages. Generators 5, 6, 7, and 8 are identical machines and the transformer banks are identical.

Table II is a comparison of test results before and after damper windings were added to generator number 8. This table also shows the per-cent increase in peak volts which were obtained before this type of damper winding was added to the generator. The advantages to be gained by adding damper windings to waterwheel generators now in service are self-evident.

Theoretical calculations were not made to determine if a different length of transmission line would result in higher peak voltages. C. F. Wagner[2] has reported that transmission lines having different lengths or different constants will produce harmonics of different magnitude and therefore different peak voltages during unbalanced faults without damper windings in the generator.

Changes in Generator Reactances

Table III gives the calculated per unit reactances of the generator without damper windings and the corresponding values after damper windings were added. These calculated reactances were obtained from the manufacturer.

Conclusions

1. The results obtained in the reduction of peak voltages for different types of unbalanced faults are summarized in table II. These percentages may change a slight amount if the faults occurred at different points of the voltage wave of the short-circuited phase, but these tests may be considered as representative of the improvements to be expected.

2. These tests show that one generator with damper windings when operated in parallel with another generator without damper windings is very effective for reducing the peak voltages during unbalanced faults.

3. A close inspection of the open-circuit voltage of the oscillograms in figures 6 and 7 will reveal five very small voltage ripples corresponding to the five damper bars in each pole. These voltage ripples, or harmonics, are too feeble to cause telephone interference and they are not noticeable when the machine is operated in parallel with another generator. These harmonics can be reduced by using round copper bars or reducing the width of the dovetail-shaped copper bars near the surface of the field poles, but it would be more expensive and would result in a more difficult mechanical job of machining the slots for the damper bar installation.

4. Since the direct axis subtransient, negative-sequence, and zero-sequence reactances have been reduced, the fault currents are increased by the addition of damper windings. However, for faults beyond the generator bus the external impedance prevents the fault current from being increased in direct proportion to the reduction in generator reactance. Table I shows the comparative root-mean-square fault currents in the high-voltage line nine cycles after the beginning of the fault.

5. It is believed that the installation of damper windings will solve some of the problems of operating companies who have unexplained voltage surges during system disturbances.

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Table III

Per Unit Reactance		Amortisseur		Per Cent Reduction
		Without	With	
Leakage	x	0.22	0.22	0
Direct synchronous	x_d	1.15	1.15	0
Quadrature synchronous	x_q	0.73	0.73	0
Direct transient	x_d'	0.40	0.40	0
Quadrature transient	x_q'	0.73	0.73	0
Direct subtransient	x_d''	0.32	0.265	17.2
Quadrature subtransient	x_q''	0.73	0.29	60.3
Negative sequence	x_2	0.48	0.28	41.7
Zero sequence	x_0	0.14	0.123	12.13

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8. Symmetrical Components, C. F. Wagner and R. D. Evans. First edition 1933, McGraw-Hill Book Company, New York.

9. Overvoltages Caused by Unbalanced Short Circuits (Effect of Amortisseur Windings), E. Clarke, C. N. Weygandt, and C. Concordia. AIEE Transactions, 1938 (August section) pages 453–68.

Discussion

H. E. Bussey (General Electric Company, Atlanta, Ga.): The authors of this paper present in an admirable manner the advantages to be derived by the addition of amortisseur windings to salient-pole generators in the suppression of overvoltages occasioned by short circuits. Great ingenuity is shown in coping with the mechanical problems incident to the installation of amortisseur windings in a generator as large as this, not originally designed for amortisseur windings, and with limited mechanical facilities.

Much has been written on this subject as may be seen from the bibliography accompanying the paper, but only in the last few years has any great weight been given to the importance of amortisseur windings on salient-pole generators in the suppression of high voltages under short-circuit conditions.

The present paper is an important contribution in that it presents the field experience before and after the application of amortisseur windings, so that a direct comparison can be drawn as to the effectiveness of the addition.

The authors mentioned in the bibliography accompanying the paper the work of Clarke, Weygandt, and Concordia as represented by AIEE paper No. 37-74. In this paper some very important considerations are set forth and while the paper by Clarke, Weygandt, and Concordia is now of common knowledge, a statement of the conclusions of this paper may not be out of order in this discussion. It is stated that the principal determining factor for the magnitude of overvoltages caused by line-to-line short circuits is the ratio x_q''/x_d'', of quadrature-axis subtransient reactance to direct-axis subtransient reactance, and that this ratio is a function of the amortisseur characteristic.

The maximum possible overvoltage caused by a line-to-line short circuit on an open-circuited machine is given very closely by the formula

$$e_{max} = 2\frac{x_q''}{x_d''} - 1$$

An important point is that the overvoltage may be considerably increased over the value of this equation by terminal capacitance, the worst condition arising when the capacitive reactance is equal to nine times the negative-sequence reactance of the generator.

In the design of an amortisseur winding for salient-pole generators, consideration must be given not only to the design constants of the generator and to the characteristics of the transmission line and the load, but should also take into account the effect of the design of the amortisseur winding on the reactances of the machine.

For the reasons pointed out, it is desirable that where amortisseur windings are to be applied to existing machines that the manufacturer be freely consulted as much valuable data on the design constants of the generators in question is available from this source. Manufacturers will, I am sure, be glad to co-operate on existing machines not having amortisseur windings.

S. B. Crary (General Electric Company, Schenectady, N. Y.): This paper is of considerable interest as it shows the results of tests made on the same machine under similar conditions before and after an amortisseur winding was installed. Such a direct comparison is not always possible. These tests indicate the improvement that may be realized by the use of an amortisseur winding, and are in general agreement with the mathematical analyses which have recently been made of this phenomena.

Whether or not to install amortisseur windings in water-wheel generators has been the subject of discussion for a number of years. It has been realized that they are generally beneficial. However, these benefits did not always seem to be sufficiently important to justify the additional expense of their installation. As a result, as stated by the authors, there are many water-wheel generators which do not have amortisseur windings. It seems to be in order briefly to review benefits which are now recognized as being obtained by their use in water-wheel generators.

1. Amortisseur windings are of value in providing additional torque for generators which are automatically synchronized when such synchronization may occur when the machine is out of phase or slightly off system speed, and also in increasing the possibility of pulling back into synchronism in case synchronism is lost following a system disturbance. The amortisseur windings give additional torque besides that provided by the main field winding, which may be quite limited except at a slip very close to synchronous speed even when the discharge resistance is in the circuit, because of the inadvisability of using a high value of discharge resistance.

2. Amortisseur windings reduce hunting which may become a definite problem under certain circuit conditions as they provide positive damping when a machine is operating at light load. This is of particular value when it is desired to operate a water-wheel generator as a synchronous condenser or under the condition of overexcitation with small kilowatt load. The tendency for oscillation is increased when the machine is connected by lines having an appreciable amount of resistance to the power system or load.

3. Amortisseur windings, because of their tendency to damp out the oscillations more quickly, are of some benefit in improving the ability of machines to ride through system disturbances, faults, and switching operations. This gain in stability, however, is small. High-resistance amortisseur windings or a double amortisseur winding having low- and high-resistance windings were at one time proposed. The advantage of the high-resistance winding is that it provides a braking torque during the period in which an unbalanced short circuit is on the system. This torque, to the extent that it loads the generator, is of benefit in increasing the stability limits. However, with quick switching and the need for systems to ride through the more severe three-phase faults, the benefits of a high-resistance winding do not seem to justify the expense of its installation.

4. Amortisseur windings are effective in reducing circuit-breaker recovery-voltage rates. Messrs. Park and Skeats have shown that the rate of rise is a function of the ratio x_q''/x_d''.

5. Amortisseur windings are also of benefit in protecting the field winding against current surges in the armature circuit which may be caused by lightning or internal short circuits. The amortisseur winding reduces the induced voltage in the individual field-pole windings and is a factor under these conditions in reducing the field circuit insulation stress.

6. Amortisseur windings are effective in reducing the overvoltages due to unbalanced faults as has been shown recently by two papers, which have analyzed this phenomena, and by the paper under discussion, which has indicated by test the improvement which may be obtained by the use of amortisseur windings.

The reduction of overvoltages due to unbalanced faults is another reason previously not fully recognized why an amortissuer winding should be used in a water-wheel generator. With the effort to coordinate insulation levels and provide proper protection for system equipment, the use of a properly designed amortisseur winding is a step in a direction which will allow further economies and improvements to be made. It now appears that with a fuller recognition of these advantages amortisseur windings will be used more in the future than they have been recently.

We believe that it would be very interesting and worth while if the authors could give information as to the ratio of the quadrature to the direct axis reactances before and after installation of the amortisseur windings. If static tests for the x_q'' and x_d'' could be made, this would be an indication of the effectiveness of the amortisseur windings at Wilson Dam, as the ratio of these reactances is a fair measure of the ability of the amortisseur to reduce the overvoltages. In checking over the data given in the paper, apparently the reactance of the transformer banks (11 and 12) used in the tests was not given. This would be of interest if it were desired to make calculations to check the magnitude of overvoltage obtained in the tests.

R. B. George: I wish to acknowledge the fine co-operation we received from the manufacturers during this work, particularly in furnishing generator reactance data and in giving constructive criticism of the proposed design before we started the work of installing the damper windings.

Mr. Bussey's statement concerning how much has been written on this subject is true at the present time, but the most useful contributions for future undertakings were published after our designs were completed. Wagner's paper[2] and Clarke, Weygandt, and Concordia's paper[9] were published after our design was completed. Mr. Wagner also published a series of articles on this subject in the *Electric Journal* during 1938. When my theoretical studies were made, the paper by Doherty and Nickle[1] and Wagner's paper[3] were the only ones I found directly applicable to the problem. Some useful information is contained in references 4 to 8, inclusive, of the bibliography but, since these papers cover different subjects or viewpoints, one should be well acquainted with the technical features of the problem to apply them to this problem.

Mr. Crary's discussion gives a good summary of the uses of damper windings and the benefits to be obtained by installing them.

For the generator without damper windings, the ratio $x_q'/x_d' = 1.825$. After damper windings are added, we use the ratio x_q''/x_d'' which is equal to 1.132. Since the ratio of the quadrature-axis reactance to the corresponding direct-axis reactance is a measure of the peak voltage during unbalanced faults, a reduction of 37.97 per cent is indicated. The calculated values of quadrature-axis and direct-axis reactances are given in table III of the paper. No tests were made to measure the various reactances because such tests are expensive and some of them would require precautions to prevent injury to the windings of a generator which had been in operation for nearly 12 years.

The transformer banks were identical and have 0.221 per cent resistance and 9.88 per cent reactance at 65,000 kva base. The constants of the transmission line are 23.4 ohms resistance, 53 ohms reactance, 349×10^6 mhos total susceptance, length 67 miles, and operating voltage 154 kv.

If studies of combinations of generator, transformer, and lines are made, the inductance corresponding to the negative-sequence reactance should be used at the harmonic frequency being studied. Formulas for various features or combinations may be found in references 1, 2, and 9 of the bibliography.

Reconditioning of Insulating Oils by Activated Alumina

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THE use of insulating oil in electrical equipment carries with it the problem of prevention of deterioration when the oil is subjected to elevated temperatures in the presence of air. Deterioration may be accelerated by the presence of moisture and organic acid. This condition is found in certain types of transformers and oil-filled bushings.

Since oxidation of oil results in an increased acidity which can be determined by tests, it is possible to gauge the rate of deterioration by making tests at intervals. The degree of deterioration is determined by the weight in milligrams of potassium hydroxide required to neutralize the acidity of one gram of oil. This test has been described by the American Society for Testing Materials as a "Tentative Method of Test for Neutralization Number for Petroleum Products and Lubricants," designation: *D*-188-27-*T*. The neutralization number so obtained is the weight in milligrams of potassium hydroxide to neutralize one gram of oil.

Effects of Oxidation

After oil has reached a relatively high acidity number the products of oxidation appear as a sludge in the oil. A pasty coating forms over all parts of the equipment which are immersed in oil. The deposit is greater in thickness where there is a marked differential in temperatures between the oil and the immersed surface. The increase in the thickness of the deposit is especially noticeable on water cooling coils. In the case of transformers, the removal of heat from the core to the heat-radiating surface is retarded and the transformer operates at an increasingly higher temperature under a given load condition. In time the transformer must be removed from service and the sludge washed from the core with a high-pressure stream of oil, and the sludge must be removed from the cooling coils or other radiating surfaces.

In oil-filled bushings a number of factors affect the formation of acid and sludge. Means are usually taken to shield the oil in the gauge glass from sunlight by the use of paint or amber glass. Contact of circulating oil with air should be avoided. Heat from sunlight with a higher ambient temperature on transformer locations indicate that transformer bushing oil should have a higher rate of acid formation. The neutralization number obtained by testing the oil in a large number of 150-kv bushings indicated that sludge formation would start in about four years where the bushings were located on transformers, and in about five years where the bushings were located on oil circuit breakers. After the neutralization number reached 0.5, considerable difficulty was experienced in cleaning the bushings free of sludge. Where the bushings were not equipped with a device to prevent the entrance of moisture, a considerable amount of water was found in the oil. Some bushings are now equipped with a device to prevent the entrance of moisture and at the same time circulation of the oil in the body of the bushing does not bring it in contact with the air surface. The principle of the conservator type of transformer is used in this device.

Control of Oxidation

In recent years the rate of oxidation has been brought under control by the use of two methods in new transformers. One method is the use of the conservator type of transformer, and the other method involves the introduction of nitrogen in the place of air above the surface of the oil.

In several large industrial plants the problem of maintenance of electrical insulating oil in old transformers became acute. About 500,000 kva capacity in both high- and low-voltage transformers were involved in the problem. In 1932 experiments were started with the use of activated alumina both as a desiccating agent and acid adsorption agent in the control of oxidation of oils in steam and

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Paper number **38-127**, recommended by the AIEE committee on electrical machinery and presented at the AIEE Southern District meeting, Miami, Fla., November 28–30, 1938. Manuscript submitted August 22, 1938; made available for preprinting November 1, 1938.

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The development work described in this paper was carried on by the author who acknowledges the valuable contributions of time and ideas by O. H. Pierce.

hydraulic turbines, transformers, and bushings.

Qualities of Material

Activated alumina is a relatively new type of solid adsorbent and it has been employed as a desiccant for dehydration of gases, vapors, and liquids. In the treatment of oils its selective adsorptive property was found to be very effective for the removal of acid in solution. The method which has proved satisfactory is to contact the oil with activated alumina, after the oil has been treated in a centrifuge or a blotter-paper press to remove the sludge and moisture. A sufficient time-interval contact with the activated alumina is necessary to allow adsorption through the highly porous granules. The material is essentially aluminum oxide, in porous form. It is classed among the commercially pure chemicals. For oil treatment the best results were obtained with grade-A activated alumina in granular form, crushed to 4-8 mesh size. The granules have high strength and resistance to crushing, shock, and abrasion and, therefore, tend to retain their original size and produce a minimum of dust during use. The weight of the packed material is approximately 50 pounds per cubic foot. The size of particles was selected to give a satisfactorily low resistance to flow of oil through the filter bed. The most important problem in developing the material for the maintenance of oil was the reactivation of the material after it had reached its adsorptive capacity. This placed the material on an economic parity with fuller's earth.

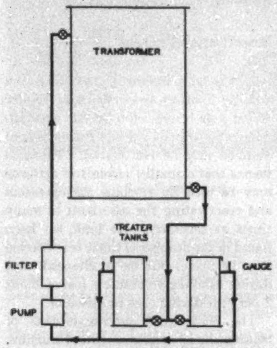

Figure 1. Schematic diagram of oil treating equipment using open-type tanks

Certain advantages and economies accrue to the operating engineer in having a single material which will cover the usual problems encountered in the maintenance of oil. The major requirements may be stated as follows: The removal of moisture and the removal of acidity as formed in the oil contained in oil-filled bushings. The removal of acidity from deteriorated oil. The removal of acidity from reconditioned or new oil by automatic external apparatus or by direct immersion in oil storage tanks or in certain small oil-filled apparatus.

Method of Use

In large transformers which could not be removed from service for an extended period, the following method of removal of acidity was used. All sludge was removed from the tank and core of the transformer as well as from the oil. After returning the transformer to service, the oil was circulated through a blotter-paper press into a tank filled with activated alumina and thence through an automobile-type filter into the transformer. The tank should be of a closed type if the pump on the filter is used to force the oil through the activated alumina into the transformer. Sludge-free oil may be caused to flow by gravity through the activated alumina into the blotter-paper filter press and pumped directly into the transformer. In this case the tank containing the adsorbent need not be a closed vessel. The oil should be admitted at the bottom and flow upward in order to carry a minimum of dust, although the amount of dust is very small. The open-type tanks used consisted of discarded lightning-arrester tanks. In treating 12 7,000-kva transformers, 150/13.2-kv rating, 1,600 pounds of activated alumina was used, half of the quantity being placed in each tank. Each transformer contained 3,300 gallons of oil. Figure 1 shows a diagram of the arrangement of the equipment, using open tanks. Figure 2 is a photograph of the open-type tanks.

Figure 2. Open-type oil treating tanks

Quantity of Material

The quantity of activated alumina required for a given amount of oil is based on the condition of the oil and the final neutralization number desired. This may be determined by reference to figure 3. In the foregoing example, the oil had a neutralization number of 0.70. A final neutralization number of 0.10 was desired. The approximate weight of one gallon of oil is 7$^{1}/_{2}$ pounds. The weight of 3,300 gallons of oil is 24,750 pounds. Referring to the chart, figure 3, the initial acidity of 0.70 is found and traced vertically to the intersection with the curve marked 0.10 as final acidity and to the

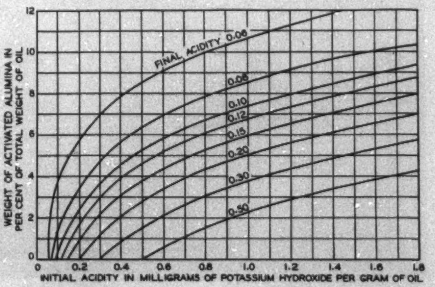

Figure 3. Chart showing quantity of activated alumina required for oil treatment

left horizontally where the weight of activated alumina will be found to be 6.3 per cent of the total weight of the oil. Therefore, 1,559 pounds will be required to treat the specified 3,300 gallons of oil. About 60 gallons of oil remained in the activated alumina and was lost during each reactivation.

Time Required

The length of time required for the correction of acidity varies according to certain conditions. The higher the acidity the faster the correction for a given amount of material. The longer the material is used, between reactivations, the slower becomes the rate of adsorption. It is economical to reactivate the material before reaching the end point. There is a slight slowing up in efficiency of the process on account of low temperatures.

The minimum size of transformer treated was 100 kva. In small transformers the best method is to suspend a canvas bag, filled with activated alumina, inside the transformer. If this is not desirable, the oil may be accumulated and treated in a quiescent state for ten days in a tank containing about one-tenth pound of activated alumina for each pound of oil. The tank may be refilled with oil several times before reaching the capacity of the adsorbent. The maximum time required for treatment of one transformer was eight weeks for a 14,000-kva transformer containing 4,000 gallons of oil. One operator was used, working 40 hours per week, 2,100 pounds of activated alumina were used. (The acidity at the beginning was at neutralization number 0.98 and 0.14 at the end.)

In the treatment of 12 7,000-kva transformers of the type described in the preceding paragraph, one operator worked 40 hours each week for six weeks on each transformer. The oil was circulated at a rate of 7,000 gallons per week or 175 gallons per hour. Rapid circulation does not give a sufficient time for the oil to remain in contact with the material. The acidity was reduced from a neutralization number of 0.70 to 0.10. The rate of correction by weeks follows:

First week	0.70 to 0.46
Second week	0.46 to 0.34
Third week	0.34 to 0.22
Fourth week	0.22 to 0.17
Fifth week	0.17 to 0.12
Sixth week	0.12 to 0.10

The material was reactivated at the end of the second and fourth weeks in order to gain a slight increase in rate of correction.

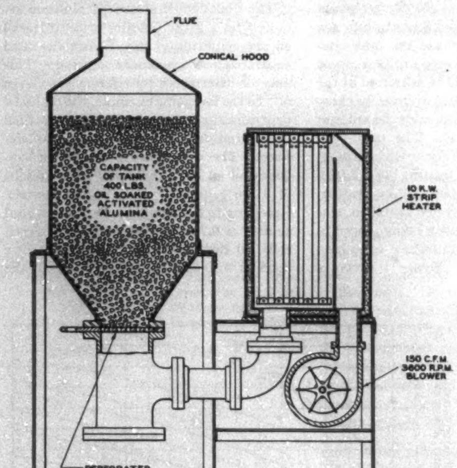
FLUE
CONICAL HOOD
CAPACITY OF TANK 400 LBS. OIL SOAKED ACTIVATED ALUMINA
10 K.W. STRIP HEATER
150 C.F.M 3600 R.P.M. BLOWER
PERFORATED SLIDING GRATE

Figure 4. Reactivating unit

During the reactivation process, one tank containing 750 pounds of material would be taken out of service with the other tank maintained in operation. After the process slows up, experience indicates that the final reduction may be made automatic by the means of using a thermo-syphon by-pass filter. By the use of the proper quantity of material the acidity may be further reduced or an equally satisfactory condition is to maintain the oil at a reasonably satisfactory level which is believed to be anywhere below a neutralization number of 0.20. In the job described the process was continued two weeks longer than usual because the transformers were being fitted with inert-gas equipment and the automatic equipment was not considered necessary.

Reactivation Process

It has been indicated that there is a definite limit to the amount of acidity which may be adsorbed by the material. After this point is reached, the activated alumina may be reactivated. This also means that a smaller amount of material may be used by stopping the operation and reactivating the adsorbent as many times as necessary. No limit has been found in the number of times reactivation may be made, but the mechanical wear during handling will cause a loss of about 3 per cent during each reactivation.

The reactivation process consists of heating the oil-soaked activated alumina, in a suitable container, with hot air at 400 degrees Fahrenheit blown through the bed of alumina. When the temperature of the bed exceeds the ignition temperature of the oil, combustion proceeds with air supplied at room temperature until all carbonaceous material is consumed. Control of the draft should be used so that the temperature of the activated alumina does not exceed 1,000 degrees Fahrenheit. Temperatures in excess of 600 degrees Fahrenheit permanently impair the activated alumina for the adsorption of moisture, but do not affect its property of adsorption of acid from oil

Reactivation Unit

A diagram of the reactivating unit is shown in figure 4. A 3,600-rpm two-inch blower, driven by a one-fourth-horsepower motor delivers 150 cubic feet of air per minute to the heating chamber which contains 10 kw in electric space heaters. A four-inch pipe conducts the air, heated to 400 degrees Fahrenheit, to the base of the tank containing 400 pounds of activated alumina. The opening of the coni-

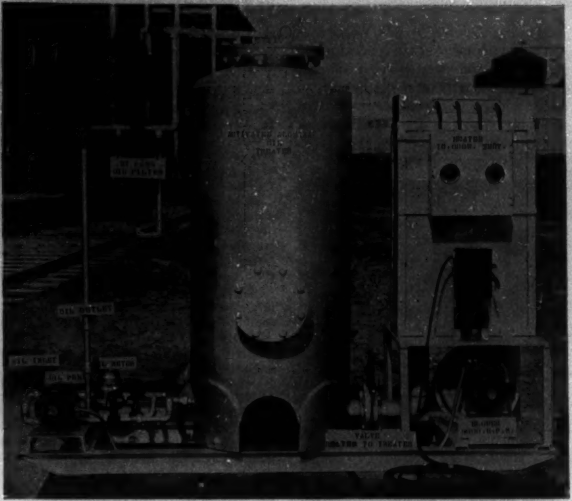

Figure 5. Reactivating unit

cal base is covered with a cast-iron sliding door, perforated to admit the air blast. The tee-fitting is attached to the base of the container and a blank cover is bolted on. This cover is removed and the sliding door or grate above is removed in order to remove the material after reactivation. Figure 5 is a photograph of the combined reactivating and oil treating unit.

Care of Oil

Consideration has been given to oil which had reached an advanced stage of deterioration. After an oil has been cleaned and the reduction of acidity accomplished, or in the case of new oil, it is desirable to maintain the oil well below the sludging point. In order to do this the transformers have been equipped with a small external tank filled with activated alumina in six-ounce canvas bags. In one group of 14 2,500-kva transformers, the external tank was made from a four-foot section of 10-inch pipe, fitted with flanges at top and bottom. A one-inch diameter pipe connects from the top of this tank to the oil filling connection at the top of the transformer. A similar pipe connects the bottom of this tank to the drain connection on the transformer. The difference of temperature between the top and bottom of the transformer provides a circulation of the oil by thermo-syphon effect. The external tank was charged with 50 pounds of activated alumina which will maintain the 700 gallons of oil contained in the transformer at the original acidity for one year. These transformers are operated at a slight overload continuously and past experience indicates that the neutralization number increases about 0.20 in one year. With a known rate of deterioration and a given quantity of oil, the size of the by-pass unit may be determined for any desired time interval by reference to chart, figure 3. Figure 6 shows a thermo-syphon treater mounted on each of three 333-kva, 13,000/440-volt transformers.

Oxidation of Treated Oil

Experience with transformers operating with treated oil, dating from 1932 to the present, indicates that none of the old types of oil as well as the new types of oil show an increase in acidity at a faster rate after having the acidity lowered by this treatment. This examination included transformers ranging from 100 kva to 14,000 kva.

Bushing Oil

More than 75 150-kv bushings have been equipped with a small bag, made of heavy cotton cloth, known as domestic, filled with three pounds of activated alumina. This bag is coiled in the form of a horseshoe in the glass bowl at the top of the bushing. This is the maximum quantity which could be placed in the space available. Experience checks the calculation that this amount of material should keep the oil in original condition for two years. The bags are changed during the regular inspection periods. The use of new material permits the use of water adsorption, in addition to acid adsorption. Experience indicated that all of the water which entered by breather action was adsorbed by the material.

Transformer Breather

Another serious contamination of transformer oil results from the entrance of water into transformers through the normal breather action caused by changes in temperature. The prevention of entrance of moisture has been studied as a parallel problem to the one of preventing

Figure 6. Thermo-syphon treaters

the ill effects of oxidation. A very practical type of dehydrator was developed with a sight indicator showing the condition of the desiccator in relation to exhaustion of adsorption properties. The design is shown in figure 7, and a photograph of one installation is shown in figure 8. In this installation, the material used was 20 pounds of grade *E*, 8–14 mesh, activated alumina, which is colored by a chemical which is blue when the material is in the dry state. As moisture is adsorbed it gradually fades to a light pink when completely saturated. The physical state of the material does not otherwise change. The material may be reactivated, unless accidentally oil-soaked, by heating in a ventilated electric oven to a temperature of 350 to 600 degrees Fahrenheit until the blue color is restored.

The first transformers to be equipped were five 4,000-kva oil-insulated self-cooled units. At the time of installation, the dielectric test of the oil averaged 22,400 volts. Three months later the average test was 28,800 volts. These transformers previously required filtration of the oil to remove moisture every 12 months.

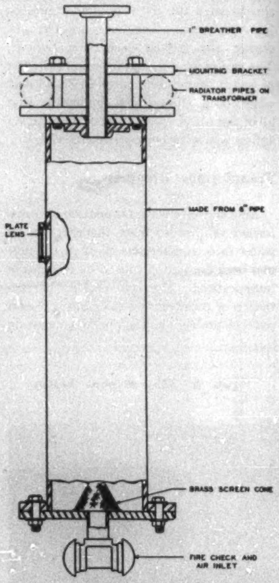

Figure 7. Transformer breather using activated alumina

Conclusion

The development of the apparatus and methods for the maintenance of oils has given the industry a new approach to a difficult problem which concerns the continuity of service of power equipment. Other materials which have long been available were restricted to use in central storage and treating plants. Such use is restricted to treatment after considerable deterioration has taken place. A program of preventive maintenance led to the development of apparatus and the selection of a material which would remove acidity as formed in field apparatus. The problem was not considered solved until methods, apparatus, and material were adapted to portable use in any field location. One operator of a semi-skilled type is required only during the initial reconditioning process and the infrequent reactivation of material in automatic filters.

Figure 8. Transformer breather

Discussion

J. H. Battle (Memphis Power and Light Company, Memphis, Tenn.): Mr. Housley's paper pertaining to the maintenance of insulating oil is very timely. There is a definite need today for some means of reducing the acidity of insulating oil. Speaking from the power companys' point of view, loads are increasing to the full-load point and even above on equipment that has been in service eight or ten years or longer. The oil in this equipment tends to sludge easily and is difficult to maintain at a reasonable dielectric strength. The sludging causes decreased heat dissipation efficiency. The difficulty in maintaining reasonable dielectric strength causes additional maintenance expense.

As the author points out various means have been devised to slow up the deterioration process. These means may take the form of: reducing the area of oil exposed to the air; keeping moisture, a source of oxygen, out of the oil; or keeping an inert atmosphere above the oil by eliminating the oxygen in the air.

Reducing the area of the oil exposed to the air is accomplished by use of a conservator or expansion tank. Since the oil in the conservator tank gets as hot as the transformer and since on the older transformers this conservator tank is open to the air, the oil in the conservator tank becomes deteriorated. The alternate heating and cooling of the transformer breathes this deteriorated oil into the transformer tank. About a year and a half ago we installed magnetic oil gauges in the conservator tanks of about 19 large power transformers. In all of these conservator tanks we found very bad sludge conditions. Any method to protect the oil in the conservator tank will further protect the oil in the transformer tank. The dehumidifier breather the author describes in his paper will prevent moisture from entering the conservator tank. Thus it will slow up the deterioration process. A more desirable breather would be one that would eliminate the oxygen also.

To us the problem of maintaining in good condition the oil in large conservator-equipped power transformers is not so acute as another problem. That problem is the maintenance of oil in underground distribution transformers. On the Memphis Power and Light Company system the single-phase units range in size from 50 to 150 kva. The three-phase units are 300 kva. Thus each unit contains only a small amount of oil relative to a power transformer but the total of our whole underground system amounts to about 25,000 gallons.

We have found that the dielectric strength of about half this oil becomes unsatisfactory in a year's time. So each spring rather than make dielectric tests on each bank of transformers, we change the oil in the whole underground system. The change is done by means of an electric tank truck. The old oil is pumped out of the transformer, the core and coils flushed off, and reconditioned oil pumped back in. From 1,200 to 1,500 gallons is changed per day.

The old oil is reconditioned by means of a combination centrifuge and blotter press. We have frequently had trouble bringing this oil up to a satisfactory dielectric strength. Sometimes we have found it necessary to run one batch through the machine several times. Acidity tests show that this oil is high in acid. With the end in view of reducing the acidity of this oil, we have experimented with activated alumina. Our outfit consists of two 300-gallon oil tanks equipped with immersion heaters and an alumina tank made from an old water tank 13 inches in diameter and 58 inches high. The tanks are piped so that oil may be transferred from tank to tank through the alumina or circulated through the alumina from one tank. The temperature of the oil is maintained at about 130 degrees Fahrenheit and is pumped through the alumina at a rate of about 60 gallons an hour. The alumina tank contains about 225 pounds of the chemical. We have found that the alumina does reduce the acidity but it is slow. Our experience is that it takes eight to ten hours to reduce the acidity by $^{1}/_{10}$ milligram KOH per gram of oil when the acidity is high and longer when the acidity is low. Of course, we could speed up the process by using more alumina but this would increase the cost.

Where it is desired to absorb the acid as it is formed in the oil, alumina answers the purpose very well. The alumina may be placed in bags inside the transformer, oil switch, or storage tanks. The author's method of using an external tank connected to valves at the bottom and top is preferable in the case of transformers in that it does not interfere with the circulation of oil within the transformer.

In the reactivation process it is very desirable that the alumina be handled as little as possible in order to cut down waste and save labor. The best method would be to reactivate the alumina in the same tank the oil is reconditioned in. We would like to ask the author what his experience on this has been.

In conclusion may we repeat that there is a definite need for some method of reducing the acidity of deteriorated insulating oil and Mr. Housley's methods seem to be an answer.

Frank E. Johnson, Jr. (New Orleans Public Service, Inc., New Orleans, La.): This paper presents a solution to a problem which has always concerned everyone operating oil-filled transformers. Although the paper treats the subject almost exclusively on the basis of old transformers, it is evident that suitable design features could be incorporated in new transformers to allow the use of this method for the removal of acids and moisture as soon as formed. No doubt transformer manufacturers are cognizant of this, and will provide these facilities as soon as the demand arises.

With reference to the installation of an external tank on a 2,500-kva transformer, mention is made that the neutralization number increased about 0.20 in one year prior to treatment, and that 50 pounds of activated alumina were used for the 700 gallons of oil. Was this rate of increase typical for various sizes and types of transformers investigated? Fifty pounds of activated alumina is about one per cent of the weight of the oil treated. It therefore appears, for the case cited, that one per cent by weight per year would be required for a 0.20 increase in neutralization number. Referring to figure 3, the "initial acidity" figure was evidently about 0.55 prior to treatment, and 0.35 after treatment. Are these values independent of time, that is, could 100 pounds of activated alumina be used with a two-year interval between renewals?

If the weight required is proportional to the time, then another chart can be prepared from figure 3 which will be somewhat easier to interpret for a user desiring to stabilize, or hold his acidity at a given value. The same ordinates can be applied on the chart, but the abscissas can be given as "rate of increase of acidity per year." The independent curves then become "initial acidity" curves instead of "final acidity," and it is possible, knowing the rate of increase of acidity, to pick out the necessary quantity of activated alumina to eliminate the acidity increase.

Figure 1 in this discussion illustrates this point.

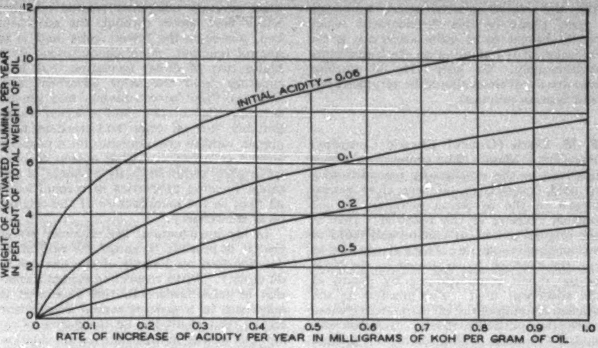

Figure 1

T. H. Mawson (The Commonwealth and Southern Corporation, Birmingham, Ala.): Oil reclamation at the present time is secured by several methods. This paper, by Mr. Housley, gives another method that has additional possibilities. Various schemes providing chemical neutralization with subsequent cleaning of the treated oil to remove the products of this reaction, together with sludge that can exist, will give oil that is satisfactory for reuse.

This particular paper deals with oil that has been used in transformers and bushings. It does not discuss the treatment of oil used in circuit breakers which is subject to considerably different service. Would there be any difference in the treatment of circuit-breaker oil?

In discussing one particular group of transformers which were operating continuously at a slight overload, and which were equipped with thermosiphon treaters, mention is made of an increase of 0.2 per year in neutralization number. If sludge begins to form when the neutralization number reaches 0.4, the full treatment with blotter press and other equipment should be used to reclaim this oil at intervals which depend upon an upper limit of acidity, or sludging, which has been previously determined.

This brings up the point of cost. Costs of reclamation from various sources vary from 3 to 12 cents per gallon for complete reclamation by other methods. For comparison the fixed charges on reclamation equipment plus the direct cost of labor and materials, plus the cost of oil, will determine the value of reclamation and the cheapest method.

The point should also be raised as to the value of reclaiming oil that has shown rapid deterioration when new. Cases on record show that certain oils which have met all of the usual specifications for new oil have not kept these properties for as long a time as other types of oil. After reclamation the same weaknesses appeared. Can any method be used that will make this oil satisfactory?

There are so many points involved on any system that the value of reclamation becomes a problem that is indigenous to that particular system. It must be solved on the basis of over-all costs covering a period of years. This paper gives a possibility of cost reduction, and, with an addition to the present experience of six years of such treatment, it is possible that these costs can be worked out to the satisfaction of all of the interested parties.

C. F. Hill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by Mr. Housley is quite important to many power companies due to the fact the extensive study of apparatus in service has made them conscious of the oil rejuvenation problem.

The author has called attention to one or two rather important advantages of the aluminum oxide, particularly its durability, lack of dusting, and finally, the ease of reactivation.

The statement is made that this new material is competitive with fuller's earth. It would be of considerable interest to have a detailed comparison of these two materials as to rate of reaction with the oil and other factors which control cost.

One statement in this paper does not agree with the usual notion of oil behavior. Normally, it is assumed that removal of the antioxidants by such filtering processes permits more rapid oxidation with further exposure of the oil. Housley states that the rate of oxidation does not speed up—or rather that the acid formation rate does not increase after removal of these acids by activated alumina. The question naturally arises as to the conditions of operation before and after the treatment.

It would also be of interest to know the cost of this reclaiming process per gallon of oil. There has been a considerable doubt if the cost would be reasonable, considering that the treated oil is not nearly a new oil.

One phase of this development which would appear to be quite attractive is the inclusion of the alumina on the transformer continuously. To keep the oil purified and dry at all times should be very desirable and appears practical.

F. M. Clark (General Electric Company, Pittsfield, Mass.): The many problems involved in the satisfactory reconditioning of used transformer oil bear close resemblance to the problems presented in the original refining of the material. Just as the original crude oil can be subjected to too much or too little refining treatment, so can the used oil be subjected to too little or too much reconditioning by means of the adsorbent used. The problem in the original refining of the oil is to remove those materials susceptible to easy oxidation and sludge formation while leaving untouched those original constituents of the oil which act as stabilizers and inhibitors of oxidation. The same problem confronts the application of adsorbents in the reconditioning of used transformer oils. In this case, the undesirable products are those formed as the result of previous oxidation and accompanying chemical change in the oil.

Mr. Housley presents two methods of maintaining transformer oil in suitably good condition during use. The first involves the treatment of the oxidized oil with activated alumina as, for example, by circulation through a bed of the material. Essentially this method is similar to the fuller's earth treatment of oil as a refining step in the original preparation of the material. The second method presented by Mr. Housley involves, during the use of the oil, its continuous gravity circulation through a bed of the activated alumina in accordance with the method of Cox who used fuller's earth in a similar manner.

In both methods of treatment, the objective of Housley appears to be the maintenance of the oil in a condition approaching the original oil color and acidity. It is generally recognized, however, that oil color and oil acidity are properties which in themselves are not safe gauges of sludge formation in transformer oil. Many oils can be obtained with equally low color and of neutral reaction but with widely varying rate of sludge formation in service. In fact, data accumulated indicate that the most severely adsorbent treated oil, that which first comes through the adsorbent bed, possesses the lowest color and is of neutral reaction, yet is characterized by a higher rate of sludge formation than later runnings from the same adsorbent bed, although these latter runnings may possess a much higher color and reaction value. Suitably low oil color and freedom from organic acidity are requisites for a properly treated transformer oil but appear to have no greater significance as a gauge of the sludge-forming properties in reconditioned oil than in the manufacture of the original oil at the refinery.

In his introduction, Mr. Housley states that it is possible to gauge the rate of oil deterioration by a study of the increase in oil acidity. With respect to sludge formation in transformer oil, this statement is only true in a general sense. Dependent on the type of oil and on the degree of refining as well as on the condition of oxidation in transformer use, sludge may form at low or high acid concentration. There appears to be no fixed relation between change in oil acidity and sludge formation in transformer oil which would allow the acidity value to be used as a gauge of the sludging character of the oil. Sludge formation is not a direct result of oil oxidation but appears as the product of a secondary reaction involving the polymerization of products formed in the primary oil oxidation. Among such products are the chemically unsaturated molecules formed as a result of oil oxidation but not capable of evaluation in a test for oil acidity.

Mr. Housley's paper presents no data concerning sludging properties. Sludge formation data are highly desirable and necessary in evaluating the serviceability of reconditioned transformer oils. The relation between the severity of the reconditioning treatment of the oil and the oil sludging tendency should be established. Data available appear to indicate that a used transformer oil, reconditioned by a properly selected and applied treatment with an adsorbent, although possessing many of the properties of a new oil such as low color and neutral reaction, does not possess the high degree of resistance to sludge formation normally associated with new transformer oil. It would be interesting and valuable were Mr. Housley to present data of this type on oils reconditioned by the use of activated alumina.

J. E. Housley: The interest shown by those giving discussions is greatly appreciated by the author. This problem of reconditioning insulating oils is certainly an important item on the maintenance program of any power system.

We have had no trouble from sludge in our circuit-breaker oils and in answer to Mr. Mawson's question, we have had no experience in treating these oils.

In the particular group of transformers to which Mr. Mawson refers, the use of a blotter press is avoided by removing the acid as it is formed and thus the oil is maintained below the sludging point. This is accomplished by the use of thermosiphon treaters.

As to the question of cost which has been raised in several of the discussions, it can be said that comparative costs of activated alumina treatment will differ according to local conditions. The cost of treatment per gallon of oil may be expected to range from 2.5 cents to 5.0 cents.

Mr. Johnson, in his discussion, presents an interesting relationship between the weight of activated alumina used and the time of treatment. There is a proportionality existing between the two and the curves, which he presents in his discussion, very clearly show this relationship. On one 2,500-kva transformer, 50 pounds of activated alumina has maintained 700 gallons of oil in good condition for $1^{1}/_{2}$ years. At this moment, we do not know how much longer this will maintain the oil. This particular transformer operates at a slight overload continuously.

In response to Mr. Battle's problem, it is suggested that the process of reducing the acid may be speeded up by more frequent reactivations, thus keeping the efficiency of the activated alumina at a maximum. The reactivating unit, shown in figure 5, is used both for treating and reactivating. However, I believe separate reactivation is more desirable, particularly when activated alumina is used to remove acid as formed (thermosiphon treaters) and where the reactivator is required for use on different batches of material.

We cannot answer Mr. Clark's statement regarding acidity and sludge formation specifically, but we have found acidity to be a good criterion of oxidation and sludging in our oils. Our experience has been that oils of fairly low neutralization numbers give no trouble from sludging.

Crossbar Dial Telephone Switching System

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IT IS the purpose of this paper to describe briefly the crossbar dial-telephone central-office switching system which has recently been developed by the Bell System for use in large cities. Sixteen years ago, in February 1923, a paper was read before the Institute, by E. B. Craft, L. F. Morehouse, and H. P. Charlesworth of the Bell System, which outlined the history and the problems involved in telephone central-office switching and described the panel dial central-office system which had just been developed and was being introduced in the large cities. The first central office of this type was placed in service in December 1921, and since that time 456 panel dial offices serving nearly $4^1/_2$ million subscriber stations have been installed in 26 different cities throughout the country. During these years many improvements have been made in the panel system to make it more serviceable to the telephone public and to meet the new problems which have arisen, but in addition, the engineers of the Bell System have continued their search to find new and better means for meeting telephone switching demands. This work has resulted in the adoption of the crossbar-type central-office switching equipment. Two offices of this type were placed in successful operation during 1938 and others are in process of manufacture and installation.

It will be appreciated that for large metropolitan areas, the development and economic introduction of a central-office switching system which differs materially from the existing systems is a rather large undertaking. The system must fit into the existing plant as a whole without material change. Generally any important changes affecting the subscribers' use of the telephone or the methods used by switchboard operators should be avoided. Existing numbering plans should not be affected, existing classes of service should be continued, and the addition of others made feasible in case they should be required.

All of these and many other factors have been taken into account and all requirements have been met by the crossbar system which offers important improvements in telephone switching, both in operation and maintenance. Its introduction does not make any of the existing equipments obsolete in the sense that these equipments will be less serviceable nor will it cause their replacement. Central offices of the crossbar type can be installed in the same building with existing panel central offices without loss in operating economies in either type of office. Certain equipment, such as the existing and additional outgoing trunks to other central offices, manually operated switchboard positions, operating room and maintenance desks, power plant and alarms, can be used in common by the two types of offices in the same building.

General

Before describing the crossbar system it is desirable first to give a brief outline of the principal functions of a dial central-office equipment. Such an office is capable of serving 10,000 subscriber line numbers, and is provided with a sufficient number of connecting switches, trunks, and associated circuits so that under usual peak loads of traffic, calls will be completed promptly.

The central-office circuits, in response to the lifting of the receiver by the calling subscriber, connect the subscriber line to the switching equipment. This equipment then extends the calling line, "link by link," through several switching stages to the called line as determined by the called office code and line number dialed by the calling subscriber. When the connection has been established to the called line, the subscriber bell is rung and when the subscriber answers, the talking connection is completed. During the conversational period the connection is held under control of the calling telephone, and when the telephone receivers are replaced, the central-office equipment and the telephones are released for use on other calls. The equipment, of course, transmits the busy-tone signal to the calling subscriber if the called line is found busy, and automatically routes a call for a discontinued or an unassigned line to an operator who informs the subscriber of the status of such a line.

Operators and associated switchboards are provided in the dial system to handle certain classes of calls and to render assistance to subscribers when required. Calls to these operators are established in response to the dialing of operators' codes in a manner similar to the establishment of calls to other subscribers.

Operators are usually provided to complete calls terminating in a dial office which are originated by subscribers served by manual offices.

Prior to the introduction of the crossbar system, the Bell System employed two general types of dial central offices. These are the well-known step-by-step and panel systems.

The step-by-step system has been used generally in the smaller cities which are frequently served by a single central office or by a relatively small number of offices and where the trunking problems are consequently less complicated. The switches of the step-by-step system are controlled directly by the impulses from the subscriber dials and, necessarily in conformity with the dial, the system operates on a decimal basis. The selectors of this system are first moved under control of the dial to any one of ten vertical positions, corresponding to the numeral of the digit dialed, and in the case of trunk hunting switches, is then automatically rotated over a row of ten trunk terminals to find an idle trunk during the interdigital time of the dial.

The step-by-step switch thus has access to ten different groups of trunk terminals with ten terminals each. The location of the trunk groups on the switches is governed by the digits dialed and consequently the relocation of a

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Paper number 39-57, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 17, 1938.

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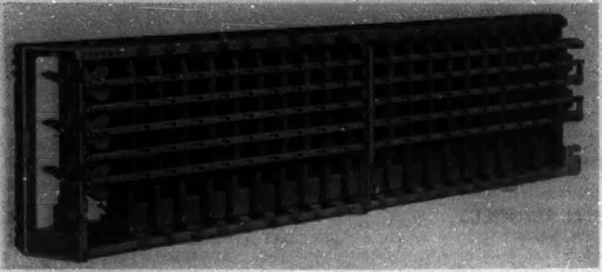


Figure 1. Crossbar switch—front view

group necessitates directory changes. These limitations in trunk access and flexibility are not material handicaps in the smaller cities throughout the country where the system is giving excellent service.

The panel system meets the complex service requirements of the larger cities with their large volume of traffic and multiplicity of central offices. In these cities the number of trunk groups is large and the number of trunks in the groups varies widely. Further, the number of groups and their sizes are frequently changed by the introduction of new offices and changes in the character or extent of existing central-office areas.

In the panel system, senders are provided which record and store the dial pulses as they are dialed and then independently control the operation of the switching units. The large panel-type switches provide access to large groups of trunks and to a large number of groups, and at the same time permit considerable variation in the sizes of the groups. The necessary flexibility in the size and location of the trunk groups is obtained by flexibly wired routing equipment provided in decoder circuits which are associated with the senders. These facilities permit trunk group locations on the switches as dictated by traffic regardless of the office codes listed in the directories and dialed by the subscribers. The panel system also readily provides for the routing of calls through intermediate or tandem offices where the traffic between offices can be more economically handled in this manner.

The crossbar system also makes use of the sender and decoder method of operation and provides a still greater flexibility in the trunking arrangements than is obtained by the panel system.

The Crossbar System

The two outstanding features of the crossbar system are the "crossbar switch" which is used for all major switching operations, and the "marker" system of control which is used in the establishment of all connections throughout the crossbar office.

The crossbar system is essentially a relay system employing simple forms of relays and relay-type structures for all switching operations. The apparatus consists almost wholly of crossbar switches, multicontact relays, and the usual small relays similar to those generally employed in all telephone systems. The switching circuits are wired to the contacting springs of the switches, and the connections through the switches are made by pressing contacts together by means of simple electromagnetic structures instead of the moving brushes and associated fixed bank terminals of other systems.

The use of relay-type apparatus with its small, pressure-type contact surfaces economically permits the use of twin or double contacts with thin layers of precious metal for all contact points. Obviously, double precious metal contacts make for reliable operation, especially with the low speech and signaling currents inherent to a telephone system.

The short mechanical movements and the inherently small operating time intervals of the "relay-like" crossbar switch permits the use of common circuits or "markers" to control the operation of the switches. This has permitted the use of large assemblies of switches and associated relays on unit frames which can be wired and completely tested for operation in the factory before the units are shipped.

In the design of the switching frames and associated control circuits, one of the objectives realized has been the standardization of a relatively small number of different types of equipment units, thereby simplifying manufacture and merchandising. This also simplifies the engineering of the equipment by the telephone companies in the preparation of their specifications to meet the particular traffic requirements of the various central offices.

The marker system used for controlling the switching operations has many advantages, the more important of which will be disclosed later in the general description of the operation of the equipment. It might be mentioned here, however, that the marker is an equipment unit consisting almost entirely of relays, which completes its functional operations in the establishment of a call in a fraction of a second. This short operating time permits a few markers to handle the entire traffic in the largest office. The markers are connected momentarily by means of multicontact relays to the various switching units of the office to control the establishment of the calls through the crossbar switches.

An outstanding advantage of the marker system of control is the "second-trial" feature, by means of which two or more attempts can be made to establish a call over alternate switches and trunks when the normally used paths are all busy. The markers are arranged to detect short-circuited, crossed, grounded, and open-circuit conditions at all vital points, and before releasing from a connection they make circuit checks to insure that the connection has been properly established. When trouble conditions are detected, they make a second attempt to complete the connection, after sounding an alarm and recording the location and nature of the trouble encountered. The marker system facilitates the introduction of new service features and changes in operation, which may be found desirable from time to time, due to the fact that the principal controlling features of the entire system are vested in a small number of markers.

Apparatus

CROSSBAR SWITCH

The crossbar switch from which the system derives its name is the basic switching unit of the system. Figure 1 shows the front view of a 200-point crossbar switch.

Fundamentally this switch consists of three major functional parts: (*a*) 20 separate vertical circuit paths, (*b*) 10 separate horizontal circuit paths, and (*c*) a mechanical means for connecting any one of the 20 vertical circuit paths to any one of the 10 horizontal circuit paths by the operation of electromagnets. From a structural viewpoint the switch is comprised of a rectangular welded frame on which are mounted 20 vertical units and the selecting mechanism consisting of 5 horizontal bars operated by 10 selecting magnets.

Primarily the switch is a multiple relay structure with 20 vertical relay-like units, each unit having an operating or "holding" magnet and ten sets of contacts in a vertical row. The switch arrangement provides a rectangular field of contacts in 20 vertical rows and 10 horizontal rows or a total of 200 sets of contacts, one set at each "crosspoint." These crosspoint contacts are operated independently of each other by a coordinate operation of the horizontal and vertical bars. The horizontal bars are controlled by the 10 horizontal or "selecting" magnets and the vertical bars by 20 vertical or "holding" magnets. Any set of contacts in any vertical row may be operated by first operating the selecting magnet corresponding to the horizontal row in which the set of contacts is located, and then by operating the holding magnet associated with the vertical row. Since the contacts are held operated by the holding magnet alone, the selecting magnet is operated but momentarily and is released as soon as the holding magnet is operated. After the selecting magnet is released, other connections may be established through the switch by the operation of other selecting and holding magnets. It is thus apparent that ten connections can be established through the switch, one for each of the horizontal paths.

From figure 2 the rather simple mechanical interlocking of the horizontal and vertical bars which causes the operation of a set of crosspoint contacts will be understood. The ten sets of contacts in a vertical row are associated with the vertical or "holding" bar of the row. Each horizontal or "selecting" bar is provided with 20 selecting fingers which are made of flexible wire. These fingers are mounted at right angles to the bar, one at each of the vertical rows of contacts. Thus when a selecting bar is rotated through a small arc by its magnet, the selecting fingers will move up or down into a position so that when a holding bar is operated by its magnet, it will engage the selecting finger at the crosspoint of the two bars and cause the corresponding set of contacts to operate. The selecting bar and the fingers not used will then be released when the selecting magnet is released, but the selecting finger used to operate the selected set of crosspoint contacts will remain latched and the contacts held closed by the holding bar until the holding magnet is released at the end of the connection. The selecting fingers are each provided with a damping spring to reduce vibration on the operation and release of the fingers.

It will be noted that the selection operation is performed by five horizontal bars although there are ten horizontal rows of contacts. This is accomplished by operating the bars in either of two directions. As shown in figure 1, two magnets are associated with each bar, one whose armature is on top of the bar, the operation of which causes the selecting fingers to move in a downward direction, and the other whose armature is below the bar causing the fingers to move upward. The selecting bars are restored to the normal or mid-position by the centering springs located on the end of the switch adjacent to the magnets.

Figure 3 shows the vertical unit of the crossbar switch with its ten sets of normally open "make" type contact springs, the holding magnet at the bottom, and the long vertical armature to which is attached the vertical holding bar. The vertical unit shown has six pairs of contacts at each of its ten crosspoints. Other vertical units are provided with ten sets of three, four, and five pairs of contacts per set. One spring of each pair as shown is a fixed spring consisting of a projection of an insulated vertical metal strip, made in the shape of a comb. This strip extends from the top to the bottom set of contacts of a vertical row. Wiring lugs are provided at the lower end of these vertical strips facing the rear to which are wired the lines or trunks of the vertical circuit paths. At the lower end of these strips and facing the front is another projection used by the maintenance force for testing purposes. The mate or movable spring of each pair is individually insulated from all other springs. These springs extend to the rear of the switch for wiring purposes and may be strapped horizontally to the

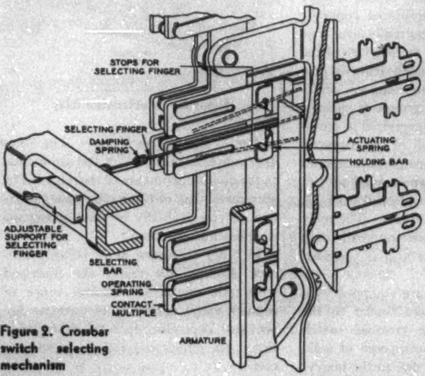
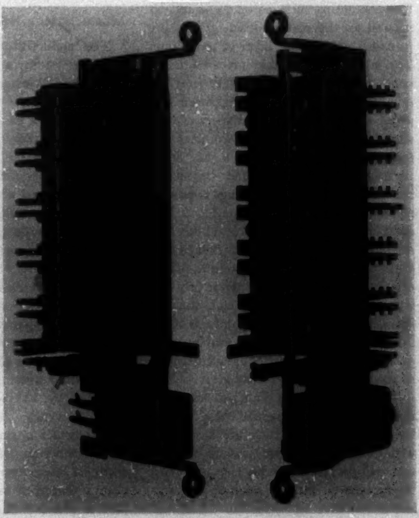
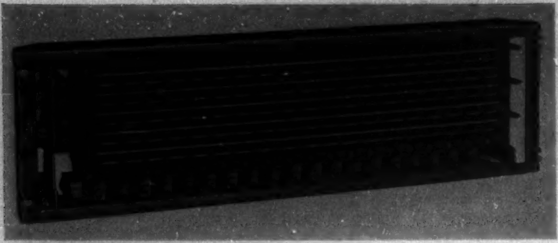


Figure 2. Crossbar switch selecting mechanism

Figure 3. Crossbar switch vertical unit





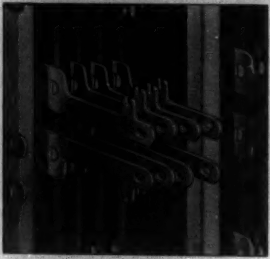


Figure 4. Crossbar switch—rear view

corresponding springs of adjoining vertical units to extend the horizontal circuit path through the switch.

The contacting ends of the thin movable contacting springs are bifurcated to provide two flexible contacts in parallel. The contacting surfaces on these springs as well as the mating fixed springs are provided with a thin layer of palladium. The use of the double precious metal contacts is an important feature of the crossbar system in providing more reliable contacting surfaces. Experience has shown that the chance of simultaneous failures of both contacts of a pair is extremely small. The actual contacting surfaces of each pair of springs consist of small bars of contact metal located at right angles to each other. These bars are composed of a ribbon of nickel capped with a thin layer of palladium. This crossbar arrangement of contacts provides a rather large area over which the two springs can make contact with each other, and thereby permits considerable tolerance in the manufacture and adjustment of the contact spring assemblies.

The switch may be equipped with "off normal" contact spring assemblies. When these are furnished they are associated with each selecting or holding magnet and are operated like relay contacts when the associated magnet operates, regardless of which crosspoint contact is closed. They are used to perform circuit functions as required in the various uses of the switch.

In the design of the switch special attention was given to the problem of wiring and cabling. Figure 4 shows the wiring terminals on the rear of the switch. These terminals are arranged for individual wiring and also have staggered, notched projections so that the terminals can be readily strapped together horizontally with bare wire as shown. This is an important feature of the switch since it permits a multiple of terminals to be easily soldered together and reduces the wire congestion on the switch.

The 200-point crossbar switch is $9^{1}/_{4}$ inches in height and $30^{1}/_{2}$ inches in length. In addition a 100-point switch $20^{1}/_{2}$ inches in length is provided. This switch is similar to the 200-point switch but is equipped with ten vertical units.

Multicontact Relay

The multicontact relay used in the crossbar system is shown in figure 5. It resembles in design the vertical unit of a crossbar switch. The relay is provided in four sizes in respect to the number of contacts, namely, in 30, 40, 50, and 60 sets of individually insulated contacts, all of which are of the normally open type which are closed when the magnets of the relay are operated. Each relay is provided with two separate magnets, armatures, and associated groups of springs, and both magnets are energized in parallel in order to close all of the contacts. By operating the two magnets independently the structures can be used as two separate relays, each equipped with 15, 20, 25, or 30 sets of contacts. The relay occupies a mounting space approximately 2 inches by 11 inches and is provided with a cover.

All contact springs are equipped with twin contacting surfaces similar to the contacts used on the crossbar switch except that they are composed of solid bars of precious metal due to the heavy-duty requirements. To facilitate wiring, these relays are manufactured with two types of wiring terminals. In one type the movable springs are of graduated lengths and are provided with notched lugs for bare wire strapping to permit the multipling of springs horizontally to corresponding springs on other relays mounted adjacent. In the second type the strapping lugs are omitted and all springs are of the same length and are provided with soldering eyelets for individual or nonmultiple wiring.

The multicontact relay finds its chief use in the common connector circuits where a large number of leads must be connected simultaneously to a common circuit.

U- and Y-Type Relays

New and improved general-purpose small relays which have been coded the *U* and *Y* type are used in this system. Figure 6 shows a typical *U*-type relay. Although somewhat similar to the *E*- and *R*-type relays which have been in common use in the telephone systems for many years, it differs from them principally in that it has a heavier and more efficient magnetic structure which permits the use of a greater number of contact springs. These relays permit the use of spring assemblies up to a maximum of 24 springs in various combinations of springs, including transfer contacts and simple make and break contacts. The relays are constructed of relatively simple parts, most of which are blanked and formed in the desired shapes in the same manner as the earlier *E*- and *R*-type relays. The cores are made from round stock and are welded to the mounting bracket of the relay. The structures of all of these relays are similar and differ principally in their spring assemblies and windings.

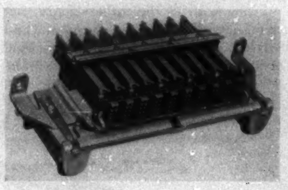


Figure 5. Multicontact relay

In order to insure more reliable contact closures, the relays are equipped with twin contacts. Various types of contact metal and sizes of contacts are provided, depending upon the characteristics of the circuit controlled by the contacts.

Improved methods of clamping the springs in their assemblies, together with the design of the springs, provide stability and minimize manufacturing and maintenance adjusting effort.

Contacts practically free from chatter on both the operation and release of the relay have been obtained by the use of relatively heavy stationary springs, short thin movable springs, and a pivoted arrangement of the armature suspension. By reference to figure 6 it will be seen that the rear ends of the armature are pivoted by two pins which project through holes in the hinge bracket mounted on the rear spring assembly. In the earlier flat-type relays of the *E* and *R* type, the armature was suspended at the rear by means of a reed-type armature hinge.

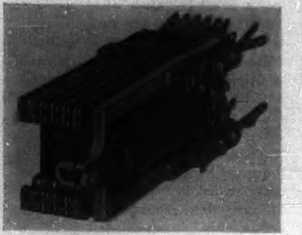


Figure 6. U-type relay

The *Y*-type relays make use of the same manufacturing tools and processes as the *U* type. Copper or aluminum sleeves are provided over the cores beneath the windings to secure the slow release characteristics required on these relays. The relay armature is embossed so that when the relay is operated, satisfactory contact is made between the metal surfaces of the magnetic circuit which insures uniform time characteristics.

In both the *U*- and *Y*-type relays the cylindrical cores permit the use of form-wound coils which are wound on special machines and slipped over the cores when completed. In the manufacture of these coils a removable mandrel is used. It is covered with a layer of sheet cellulose acetate and accommodates several coils. These coils are then automatically wound on the mandrel from different spools of insulated wire. Separations are left between adjacent coils so that when the winding operation has been completed the individual coils can be separated. A very thin sheet of cellulose acetate is automatically interleaved between successive layers of wire to hold the wire in place and to provide insulation between layers. This general method of winding coils also is used for the magnets of the crossbar switches and multicontact relays.

Functions of the Equipment Units

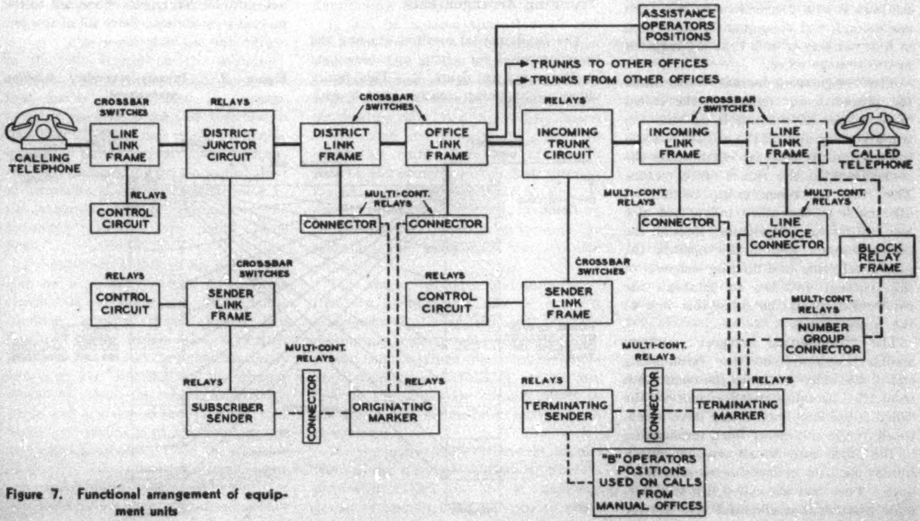
The general operation of the system as a whole may be more easily understood by first describing the principal equipment units in the system and their functions before proceeding with a description of the operation of the circuits. A simplified block diagram of the principal equipment units of the system is shown in figure 7. It will be noted that in general there are three types of equipment units:

1. The transmission battery supply and supervisory circuits consisting of the "district junctors" and the "incoming trunks."
2. The crossbar-switch frames.
3. The common "control" circuits, the "senders," and the "markers."

The "district junctor" and the "incoming trunk" circuits are composed principally of small relays. The district junctors furnish the talking battery for the calling subscribers and supervise the originating end of connections. The incoming trunks control the ringing of the called subscriber bells, furnish talking battery for the called subscribers, and supervise the terminating end of connections.

The switch frames, which consist almost entirely of crossbar switches, provide the means for switching between the subscriber lines, the district junctors, and the incoming trunks. Switch frames also are used for switching the district junctors and the incoming trunks to the senders.

The "senders" consist principally of small relays and their functions are similar



ASSISTANCE OPERATORS POSITIONS
TRUNKS TO OTHER OFFICES
TRUNKS FROM OTHER OFFICES
CROSSBAR SWITCHES
RELAYS
CROSSBAR SWITCHES
RELAYS
CROSSBAR SWITCHES
LINE LINK FRAME
CALLING TELEPHONE
DISTRICT JUNCTOR CIRCUIT
DISTRICT LINK FRAME
OFFICE LINK FRAME
INCOMING TRUNK CIRCUIT
INCOMING LINK FRAME
LINE LINK FRAME
CALLED TELEPHONE
RELAYS
CONTROL CIRCUIT
MULTI-CONT. RELAYS
CONNECTOR
CONNECTOR
MULTI-CONT RELAYS
CONNECTOR
MULTI-CONT. RELAYS
LINE CHOICE CONNECTOR
BLOCK RELAY FRAME
CROSSBAR SWITCHES
RELAYS
CONTROL CIRCUIT
SENDER LINK FRAME
RELAYS
CONTROL CIRCUIT
CROSSBAR SWITCHES
SENDER LINK FRAME
MULTI-CONT. RELAYS
NUMBER GROUP CONNECTOR
MULTI-CONT. RELAYS
RELAYS
SUBSCRIBER SENDER
CONNECTOR
RELAYS
ORIGINATING MARKER
MULTI-CONT. RELAYS
RELAYS
TERMINATING SENDER
CONNECTOR
RELAYS
TERMINATING MARKER
"B" OPERATORS POSITIONS USED ON CALLS FROM MANUAL OFFICES

Figure 7. Functional arrangement of equipment units

to those of the operators at a manual switchboard. The "subscriber senders" register the called numbers from the subscriber dials and transmit the necessary information to the "markers," to the "terminating senders," and to the manual operator positions in manual offices for completing connections to the called lines.

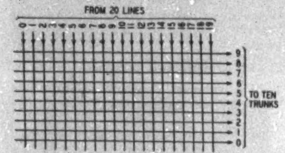


Figure 8. Simple trunking arrangement with a single 200-point crossbar switch

The subscriber senders also control the operation of the selectors in distant panel offices. The "terminating senders" in the terminating end of the crossbar office receive the numerical digits of the called numbers from the subscriber senders of any dial office and transmit the required information to the "terminating markers" for setting up the connections to the called lines.

The "markers" are the most important control circuits in the system. They are composed of both small and multicontact relays. There are two types, one for originating traffic and one for terminating traffic. The operating time of the markers is short, considerably less than one second, and consequently only three or four markers of each type are required in the average office.

The "originating markers" determine the proper trunk routes to the called office. They have access to all outgoing trunk circuits and all the crossbar-switch frames that are used for establishing the connections to the called office trunks. They test the trunk group to find an idle trunk to the called office, and also test and find an idle channel through the switch frames, and finally operate the proper selecting and holding magnets of the crossbar switches to establish the connections from the subscriber line to the trunk circuit.

The "terminating markers" perform similar functions in the terminating end of the office to set up the connection from the incoming trunk circuit to the called subscriber line. They have access to all of the subscriber lines terminating in the office, and to all crossbar-switch frames used for connecting to subscriber lines. They test the called line to determine whether it is idle, and also test for and find an idle channel through the switch frames and finally operate the proper magnets of the crossbar switches and establish the connection to the called subscriber line.

In addition there are common "control" circuits associated with the "line link" and the "sender link" frames for controlling the operation of the switches on these frames. There are also the common "connector" circuits, consisting mainly of multicontact relays, which are used for connecting the markers to the senders, to the switch frames, and to the test terminals of the called subscriber lines.

It should be noted that the line-link frames, although shown separately, are used for both originating and terminating traffic.

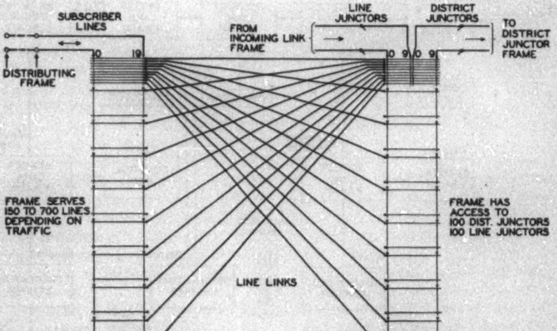
After the talking connection has been established between two subscribers, all of the common control units, such as the senders, markers, connectors, line-link control circuit, and the sender-link frames and their associated control circuits, will have been released, and the talking connection will be maintained in this condition by the holding magnets of the crossbar switches used on the line-link, district, office, and incoming-link switch frames. These switch magnets are held operated under control of the supervisory relays in the district junctor and the incoming trunk circuits and are released when the subscribers replace the receivers.

Trunking Arrangements

The fundamental method of using the crossbar switch for setting up connections is illustrated in figure 8. This figure shows a 200-point crossbar switch with 20 vertical units each wired to a subscriber line and ten trunks strapped horizontally across the switch. With such an arrangement, any one of the 20 lines may be connected to any one of the ten trunks. The number of lines which can be connected to the same ten trunks may be increased to 40 by adding a second 200-point crossbar switch with 20 different lines connected to its verticals and by wiring the horizontal contact multiple of this second switch to the horizontal multiple of the switch shown in figure 8. By adding other switches in this manner, any number of lines may be given access to the ten horizontal trunks.

To obtain greater trunking access, two groups of switches known as "primary" and "secondary" are used. Figure 9 illustrates this primary- and secondary-switch arrangement as used in the line-link switch frames and in various forms throughout the crossbar office. The switches are arranged in two vertical files of ten primary switches and ten secondary switches. There are 20 subscriber lines connected to the verticals of each of the ten primary switches and 20 trunk circuits are connected to the 20 verticals on each secondary switch. The horizontal multiples on the primary switches are connected to the horizontal terminals of the secondary switches, each primary switch having one horizontal path connected to each of the ten secondary switches. With this arrangement, the 20 lines of any primary switch have access to all 200 trunks connected to the secondary switches. Since all of the pri-

Figure 9. Primary-secondary trunking arrangement


SUBSCRIBER LINES
DISTRIBUTING FRAME
0
19
FROM INCOMING LINK FRAME
LINE JUNCTORS
DISTRICT JUNCTORS
TO DISTRICT JUNCTOR FRAME
0
9
0
9
FRAME SERVES 150 TO 700 LINES DEPENDING ON TRAFFIC
FRAME HAS ACCESS TO 100 DIST. JUNCTORS 100 LINE JUNCTORS
LINE LINKS
PRIMARY
SECONDARY

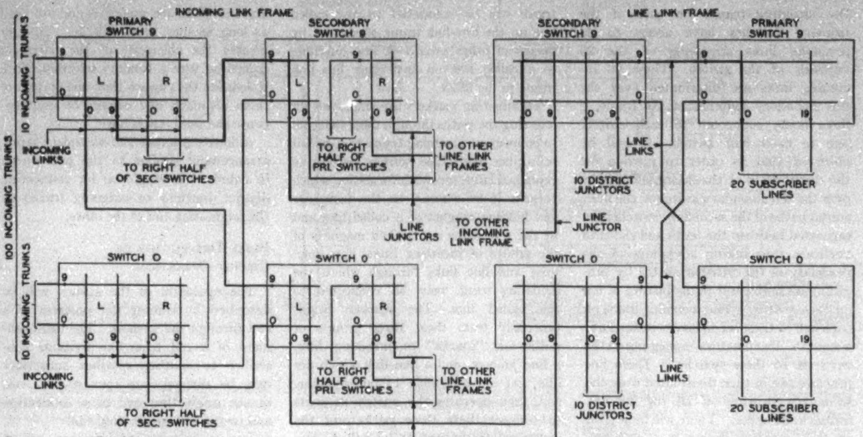


Figure 10. Double primary-secondary trunking arrangement

mary switches are wired in this manner, that is, with their ten horizontal paths distributed over the ten secondary switches, then all of the 200 lines on the primary switches have access to the 200 trunks on the secondary switches. It is evident that another vertical file of ten primary switches may be added with 20 subscriber lines connected to the verticals of each switch, and with the horizontal paths strapped and connected to the horizontal paths of the primary switches shown. This would give 400 lines access to the 200 trunks on the secondary switches. In actual practice on a line link frame, several files of primary switches may be connected together in this manner depending upon the traffic volume of the subscriber lines. The circuit paths connecting the horizontal rows of terminals of the primary switches to the horizontal rows of terminals of the secondary switches are called "line links."

To establish a path from a line circuit on a primary switch to a trunk circuit on a secondary switch, the common "control" circuit serving this line-link frame locates the subscriber line to be served and then simultaneously selects an idle "line link" on the primary switch on which the subscriber line appears and a group of trunks wired to a secondary switch in which there are one or more idle trunks. Thus the selection of the line link is made contingent upon the availability of trunks, and by means of this together with the primary-secondary distribution of the links a very efficient usage of the links and trunks is obtained.

In the line-link frame shown in figure 9, it will be seen that the trunks on the verticals of the secondary switches are split into groups of 100 trunks each, one group being connected to the "district junctors" and used for originating traffic and the other group of 100 trunks being connected to "line junctors" and used for terminating traffic.

It will be noticed that there is but one crossbar-switch appearance of a subscriber line in the office. This is on a vertical unit of a primary crossbar switch where both the originating and terminating calls are completed by means of the same line-link circuits. Thus all originating traffic from any of the 20 lines on any primary switch flows through the associated ten line links to the 100 district junctors and all terminating traffic to these 20 lines flows through the same ten line links from the 100 line junctors.

This single "primary and secondary" trunking arrangement also is used at other points in the system, such as in the originating and terminating sender-link switch frames, where the circuits reached are nondirectional, that is, where any one of the selectable circuits wired to the frame can be used for setting up a connection.

For the switch frames where the circuits reached are directional, that is, where a particular called line or a particular group of trunks must be used in order to complete a connection, the problem of trunking becomes more complex and it is necessary to provide a trunking arrangement using two "primary and secondary" switch frames arranged in tandem.

Figure 10 shows a typical arrangement of this kind which is necessary to secure the required trunking flexibility and efficiency. This figure shows an "incoming link" frame to which incoming trunks are connected and a "line link" frame to which subscriber lines are connected as described above. These two frames are used in tandem for establishing the terminating connections between the incoming trunks and the called subscriber lines. As is indicated, 100 incoming trunks are connected to the 100 horizontal paths of the ten incoming link frame primary switches, there being ten incoming trunks connected to each of the primary switches. A total of 150 to 700 subscriber lines may appear on the verticals of the primary switches of the line-link frame; however, only 200 lines or 20 on the verticals of each of the ten primary switches are shown in the figure.

In order to connect a particular incoming trunk to a particular called line, an idle channel is selected through these two switch frames, consisting of an "incoming link" on the incoming-link frame, a "line junctor" between the two frames, and a "line link" on the line-link frame, and all are connected in series as shown in the figure. It will be noted that the incoming trunks on each of the primary switches have access to 20 incoming links appearing on the 20 verticals of the switch. These 20 incoming links are distributed over the ten secondary switches of the frame, 2 links being connected to each switch, one to each half switch. It will be observed that in order to provide for the distribution of the 20 incoming links over the ten secondary switches, the horizontal paths of the secondary switches are separated between the tenth and eleventh verticals, thus taking advantage of the flexibility of the crossbar switch by providing 20 horizontal paths instead of ten on each switch. The incoming links, on each half of these secondary switches, have access to "line junctors" appearing on the verticals of these switches. These line junctors are in turn distributed over the secondary switches of all the line-link frames in the office. There will be at least one line junctor as shown, from each secondary switch on an incoming link frame to a secondary switch on every line-link frame in the office, or a minimum of ten line-junctor paths between any incoming-link frame and any line-link frame. The number of the line junctors between these frames will vary depending upon the number of frames required in an office. The line junctors on the verticals of each of the line-link frame secondary switches in turn have access to ten line links on the horizontal paths. These ten line links are, as described above, distributed over the primary switches of the line-link frame, one to each primary switch. These line links then have access to the called subscriber lines which appear on the verticals of the primary switches. With this arrangement of switches and the three groups of interconnecting link paths, any incoming trunk can be connected to any called line on the line-link frame shown, or by means of other groups of line junctors, to a called line on any other line-link frame in the office.

Terminating markers are employed for selecting the paths through these switches to connect an incoming trunk to a called subscriber line. The marker, as will be explained later, records information which permits it to connect to the test wire and holding magnet of a called line and to the test wires and switch magnets of the groups of incoming links, line junctors, and line links through which the incoming trunk may be connected to the called line. The marker simultaneously tests these three groups of paths and "marks" an incoming link, a line junctor, and a line link which are idle and are accessible to one another, and then operates the switch magnets to connect these three paths and the incoming trunk and the called line together. The paths are selected in an ordered arrangement, so that the lowest numbered incoming links, line junctors, and line links are preferred and are used as long as they are available. This increases the efficiency of the paths as compared with a random selection, since it reduces the chance that one or two of them although idle cannot be used because the third one is busy.

A double primary and secondary trunk arrangement similar to the one shown in figure 10 is employed for connecting district junctors to outgoing trunks in the originating end of the office.

BRIEF DESCRIPTION OF CIRCUIT OPERATION

The operation of the system will be described by tracing the progress of a call through the system. The establishment of a call from one crossbar subscriber to another crossbar subscriber may be divided into four stages: two in the originating end of a connection and two in the terminating end.

1. The calling subscriber is connected to a sender for the purpose of registering the called number which is dialed.

2. The subscriber sender is connected to

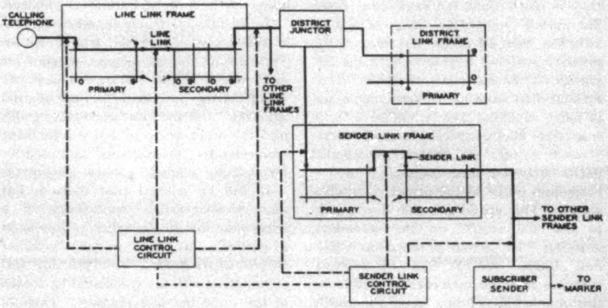

CALLING TELEPHONE
LINE LINK FRAME
LINE LINK
PRIMARY
SECONDARY
TO OTHER LINE LINK FRAMES
DISTRICT JUNCTOR
DISTRICT LINK FRAME
PRIMARY
SENDER LINK FRAME
SENDER LINK
PRIMARY
SECONDARY
TO OTHER SENDER LINK FRAMES
LINE LINK CONTROL CIRCUIT
SENDER LINK CONTROL CIRCUIT
SUBSCRIBER SENDER
TO MARKER

Figure 11 (above). Calling line connected to district junctor and subscriber sender

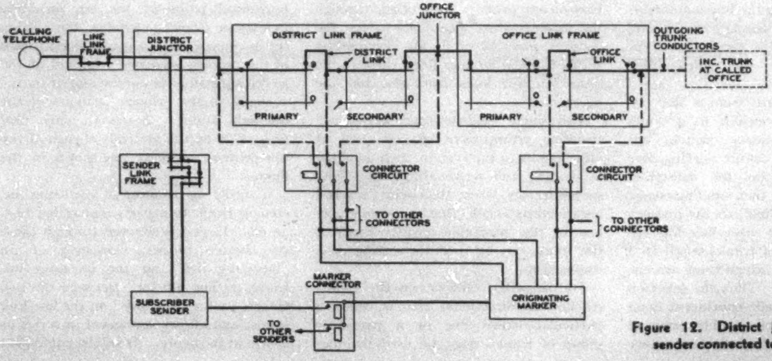

CALLING TELEPHONE
LINE LINK FRAME
DISTRICT JUNCTOR
DISTRICT LINK FRAME
DISTRICT LINK
PRIMARY
SECONDARY
OFFICE JUNCTOR
OFFICE LINK FRAME
OFFICE LINK
PRIMARY
SECONDARY
OUTGOING TRUNK CONDUCTORS
INC. TRUNK AT CALLED OFFICE
SENDER LINK FRAME
CONNECTOR CIRCUIT
CONNECTOR CIRCUIT
TO OTHER CONNECTORS
TO OTHER CONNECTORS
SUBSCRIBER SENDER
MARKER CONNECTOR
TO OTHER SENDERS
ORIGINATING MARKER

Figure 12. District junctor and subscriber sender connected to the outgoing trunk

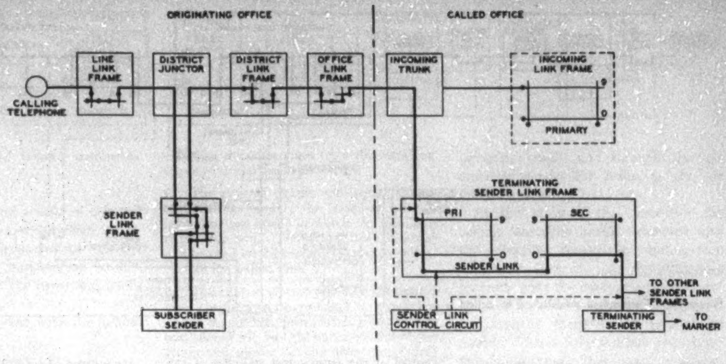


Figure 13. Connection established to terminating sender

an originating marker and the marker selects the switch frames for establishing the connection to an outgoing trunk.

3. The outgoing trunk circuit is connected to a sender in the terminating end to register the called number.

4. The terminating sender is connected to a terminating marker and the marker selects the switch frames for establishing the connection to the called subscriber line.

The first stage in the progress of a call is illustrated in figure 11. It will be seen that the line of a calling subscriber terminates on a vertical unit of a primary crossbar switch located on a line-link switch frame. When the subscriber receiver is lifted from the telephone preparatory to dialing, a line relay is operated, as in other systems, and the circuits proceed with the establishment of the connection to an idle subscriber sender which will register the called number when it is dialed.

The circuit functions on this stage of the call are as follows:

1. The subscriber line is located by the "line link control" circuit which is common to the line link frame, by a co-ordinate method of testing. That is, the control circuit determines the primary crossbar switch in which the line is located and the particular vertical unit in the switch on which the line is terminated. This operation is similar to the line-finder operation in other dial systems, except that the operation is accomplished by relay operations instead of by a mechanically traveling brush.

2. The line-link control circuit then simultaneously selects an idle line link between the primary switch in which the line appears, and a secondary switch on which a group of district junctors appears which has at least one idle district junctor in the group and which has access to idle senders and an idle sender link.

3. This will bring into operation the common "sender link control" circuit of the sender-link switch frame to which the selected group of district junctors is connected. This control circuit will select an idle district junctor in this group which appears on a primary switch on the sender-link frame. There are ten sender links serving the selected district junctor. These ten sender links and the ten sender groups to which they have access on the secondary switches are then tested simultaneously to find an idle sender link with access to a group of senders in which there are one or more idle senders. When this choice has been made an idle sender in the group is then selected.

4. The two control circuits in co-operation with each other operate, first the selecting magnets and then the holding magnets associated with the paths selected on the switches of both the line-link and sender-link frames, and thereby establish the connection from the calling subscriber to an idle subscriber sender.
This connection may be traced by referring to figure 11, from the calling line on the vertical unit on a primary switch of the line-link frame, through a line link and a secondary switch, through a district junctor circuit, to a vertical unit on a primary switch of the sender-link frame, through a sender link and a secondary switch to a subscriber sender which is connected to a horizontal circuit path on the secondary switch.

5. The two control circuits are then released and made available for use on other calls. The connections through the switches to the sender are held established by means of the holding magnets which are held operated over a signal control lead, called the "sleeve" lead, under control of the relays in the sender, which in turn are under control of the subscriber telephone.

Upon completion of these operations which take but a fraction of a second the subscriber sender transmits the dial tone to the calling subscriber as an indication to dial the number. When the subscriber dials, electrical impulses are transmitted to the sender, which receives and registers them. When the sender has registered the office code, which in New York City for example is contained in the first three digits dialed, the sender will connect itself to an idle originating marker by means of multicontact relays of a marker connector circuit.

Before proceeding further it is desirable to mention several other functions of the two common control circuits used for setting up this part of the connection.

1. The control circuits signal to the sender the class of the calling line, that is, for example, whether the line is a coin line or a noncoin line.

2. The sender-link control circuit signals to the sender the number of the district-link switch frame on which the selected district junctor appears, since this identification will be used later in the establishment of the connection.

3. The sender-link control circuit tests the circuit paths chosen from the line circuit to the sender before disconnecting from the connection, in order to insure the proper establishment of the connection. In case of a failure the control circuits will make repeated trials to establish the connection over different paths and give an alarm to the maintenance force.

4. Emergency control circuits are provided for use in case the regular control circuits are removed from service for maintenance reasons.

The next stage in the progress of the call is illustrated in figure 12. In this stage of the call the principal control unit is the originating marker. Its major function is to control the switches in the establishment of the connection to an idle outgoing trunk circuit to the called office, which may terminate in a distant office or in the same office as the calling subscriber.

When the subscriber sender connects to the originating marker through the connector circuit, the sender transfers the called-office code indication and the district link frame identification to the

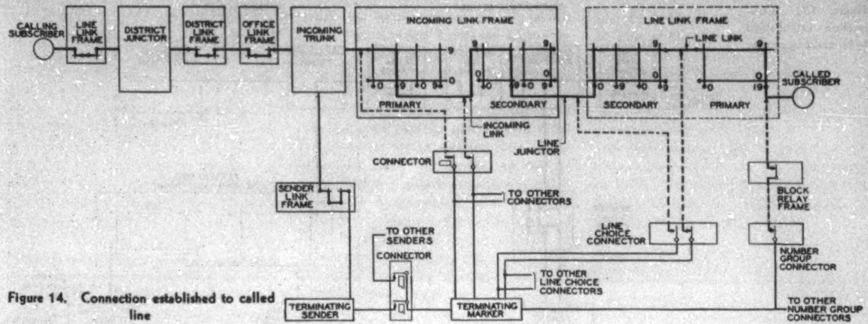


Figure 14. Connection established to called line

marker circuit. The called-office code indication causes the operation of a "route" relay in the marker corresponding to the particular office called.

There are a number of route relays in each marker and one is assigned to each called office routing. The route relay is connected as required by the office code to which it is assigned, so that it will direct the marker to the trunks of the called office and to the office-link switch frame on which these trunks appear and indicate the number of trunks in the group. The route relay also is connected to determine the type of the called office, such as crossbar, panel, or manual, and to set up the corresponding circuit conditions in the subscriber sender to enable the sender to handle the connection properly after the marker has been released. The connections of the route-relay contacts to the control relays in the marker are made flexible so as to permit the assignment of any route relay to any office code and to permit changes to be made from time to time in the route information, changes in trunk group sizes and location, changes in the type of terminating office, etc. The route relays and associated flexible connection facilities represent a considerable portion of the marker equipment, especially in large metropolitan offices where several hundred central offices are involved.

When the route relay is operated, the marker proceeds with the establishment of the connection as follows:

1. It connects to the office-link frame on which the trunks to the called office appear. This connection is made through the office-link frame connector circuit, one of which is provided for each office-link frame. Through this connector the marker is extended to the test leads of any desired trunk group on the office-link frame and to the crossbar switches of the frame. When so connected the marker has exclusive control of the trunks and switches of the frame and other markers which desire connection to the same frame are deprived of access until the connected marker releases.

2. The marker next tests the outgoing trunks to the called office and selects an idle one. If, as determined from the route relay, the trunks are divided over more than one frame, the marker will connect to the second group of trunks on the second office-link frame in case the first group of trunks is found to be busy.

3. The marker also connects by means of a connector circuit to the district-link frame associated with the district junctor to which the calling line is connected. The identification of this frame was obtained from the sender and the sender-link control circuit as previously mentioned. Through the connector circuit of the district-link frame the marker is extended to the control leads of the district junctor circuit and the crossbar switches of the district-link frame. As in the case of the office-link frame, only one marker is connected to a frame at a time.

4. The marker after selecting an idle trunk circuit which appears in a horizontal circuit path on one of the secondary switches of the office-link frame, then proceeds with the selection of an idle channel through the switches of the two switch frames. A number of these connecting channels is provided between the district junctor and the outgoing trunk. Each channel consists of a "district link" on the district-link frame, of an "office link" on the office-link frame, and an "office junctor" connecting the district-link frame to the office-link frame. The marker tests a group of these channels simultaneously and selects an idle one. It then operates the switch magnets which will connect these three paths of a channel, and the district junctor and the outgoing trunk together, thereby establishing a connection from the district junctor to the outgoing trunk.

5. When the marker has completed this operation it checks the connection to insure that it has been properly established and that it is capable of being held under control of the district junctor, before releasing itself from the connection.

6. The marker performs these functions in approximately 0.5 second then releases and becomes available for use on other calls.

It will be observed that the three links involved in establishing the connections between the district junctors and the outgoing trunks are used in series and are chosen simultaneously. Generally in other systems the establishment of a connection involving three such paths, is made in three successive stages with a possibility that after a selection has been made at one stage it will be found that the paths accessible to it are all busy and, therefore, the connection cannot be completed.

Before describing the next stage in the establishment of a call, it is desirable to point out other features and functions of the originating marker.

1. The marker permits wide variations in the sizes of trunk groups, permitting trunk groups as small as two and as large groups as may be required. This makes for an efficient use of the office-link frame terminals and thereby tends to reduce the office-link frame equipment.

2. The marker makes a second trial to establish connections over alternate trunk routes in case calls cannot be completed over the normally used groups because of busy conditions.

3. The marker makes a continuity test of the circuits over which the switches are controlled and tests them for short circuits, crosses, and open and grounded circuits which would interfere with the proper establishment of a call and where troubles are detected, it signals this condition to a common "trouble indicator" where an indication of the trouble and its location is recorded and a maintenance alarm given. The call is then completed over another group of circuits.

The first stage in the progress of the call through the terminating end of a crossbar office is illustrated in figure 13. It consists of connecting the incoming end of the selected trunk to a terminating sender for the purpose of receiving the number of the called line from the subscriber sender.

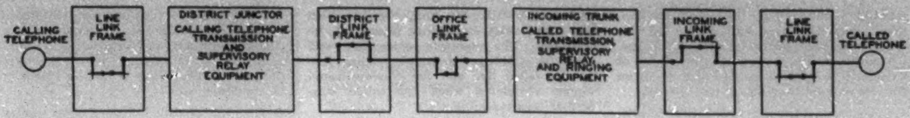
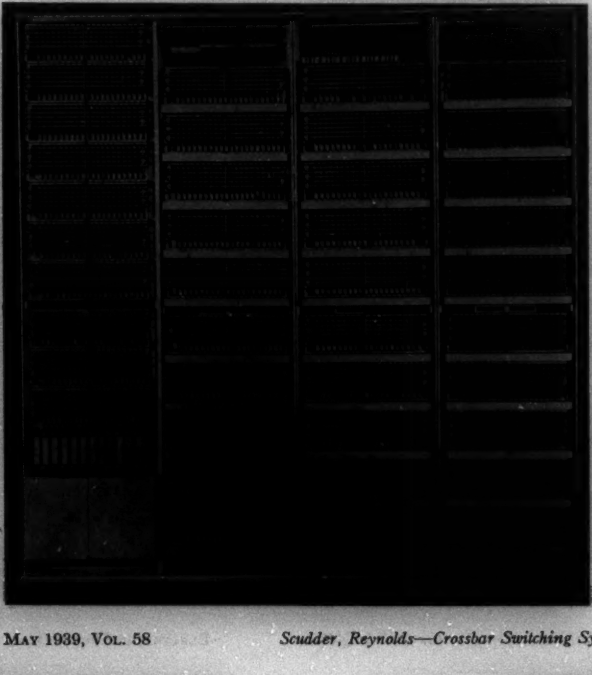


Figure 15. Completed talking connection

When the incoming trunk is selected by the originating end of the office equipment, the "sender link control" circuit associated with the terminating sender-link frame on which the incoming trunk appears, is called into action. The control circuit then proceeds with the following functions:

1. To locate the incoming trunk circuit, which appears on one of the ten horizontal paths of a primary switch.
2. It selects an idle sender link between this primary switch and a secondary switch on which there is an idle terminating sender.
3. The control circuit selects one of the idle terminating senders reached through the secondary switch and then operates the selecting and holding magnets associated with the selected circuits, which will establish the connection from the incoming trunk to the terminating sender.

Figure 16. Line-link frame



4. The control circuit will signal to the terminating sender the number of the incoming-link frame in which the incoming trunk appears. This frame identification will be used later in establishing the connection to the called line.
5. The control circuit will then disconnect after checking to insure that the connection to the sender has been properly established and that it will be held under control of the trunk and sender circuits after the control circuit leaves the connection.

As soon as this operation has been completed, which takes but a fraction of a second, the terminating sender will be in direct connection with the subscriber sender in the originating end of the connection. This path may be traced, by referring to figure 13, from the subscriber sender through the sender-link frame, through the district junctor, through the district-link and office-link frames, over the outgoing trunk to the incoming trunk, and through the terminating sender-link frame to the terminating sender.

At this stage of the connection the calling subscriber is still connected with the subscriber sender, and dialing may be still in progress. As the subscriber proceeds with the dialing of the digits of the called number, the subscriber sender will transfer them to the terminating sender. This is done by means of impulses transmitted over the circuit paths between the two senders. When the subscriber sender has completed the transfer of the called number to the terminating sender, the subscriber sender will be released and the calling line will then be connected through the district junctor to the incoming trunk.

When the terminating sender has secured the record of the called line number, the sender then connects to an idle terminating marker by means of multicontact relays of a connector circuit.

The next stage in the progress of the call is shown in figure 14. The terminating marker is the principal control unit at this point in the connection. Its principal function is to provide means for establishing the connection from the incoming trunk to the called subscriber line.

When the terminating sender has connected to the terminating marker, the sender will transfer both the called line number and the incoming-link frame identification to the marker. The terminating marker then proceeds to establish the connection to the called line as follows:

1. It connects itself to the particular "number group connector" circuit including the "block relay" frame in which the called line appears in its numerical sequence. All subscriber lines are provided with a set of three test terminals which appear on the block-relay frame. These terminals correspond to the directory number of the subscriber line. A number group connector generally has access to the test terminals of several hundred line numbers depending on the terminating traffic to the lines.
2. The marker will obtain a connection through the number group connector to the busy-test terminal of the particular called line and determine whether the line is busy or idle.
3. It will determine from the two other test terminals, the identification of the

line-link frame where the called line appears, and the horizontal group of line links which has access to the called line. In addition, it determines the type of ringing to be applied to the called line from the circuit conditions on the test terminal.

4. Assuming that the called line is idle, the marker will connect, through the line-choice connector circuit, to the line-link frame and to the ten line links which have access to the called line.

5. It will then connect, through the connector circuit, to the incoming-link frame associated with the incoming trunk to which the calling subscriber line is now connected. The incoming-link frame identification was obtained from the sender-link control circuit through the sender as previously mentioned.

6. The marker will then select an idle channel through the incoming-link and line-link frames as previously described. This channel will consist of an "incoming link," a "line junctor," and a "line link" all to be connected in series. The marker then operates the proper selecting and holding magnets of the crossbar switches in each frame which establishes the connection from the incoming trunk to the called subscriber line.

7. The marker will then cause the incoming trunk to start the proper ringing over the called subscriber line and to transmit the ringing-tone signal over the trunk to the calling subscriber.

8. At this point the terminating marker and the terminating sender will have completed their functions and, together with the terminating sender-link frame, will be released. The complete connection will then be established from the calling line to the called line and the conversational circuit completed when the called subscriber answers.

If the terminating marker finds the called line busy it will cause the incoming trunk circuit to transmit a busy tone to the called subscriber.

The terminating marker has the following other important functions:

1. If the call is for a private branch exchange (*PBX*) the condition on one of the test terminals of the called line in the number group connector will inform the marker that the line is one of a group of lines. The marker will test all of the lines in the group, testing up to as many as 20 simultaneously, and will select an idle one. The lines of a private branch exchange may be assigned to nonconsecutive numbers within the usual 10,000 series, and with the exception of the numbers dialed, they may be assigned to line numbers in a special group of 2,500 outside of the 10,000 series. These features reduce the necessity for number changes due to the growth of private branch exchanges, and conserve subscriber line numbers in the office. The lines of a private branch exchange group can be distributed over several line-link frames and over two number-group connectors to equalize the terminating traffic load in the case of busy private branch exchanges.

2. The marker recognizes numbers dialed which are unassigned, disconnected, or changed numbers, and automatically routes such calls to an operator who will inform the calling subscribers as to the status of the numbers called.

3. In case the called number is on a party line, the marker determines from one of the test terminals which station of the line is to be rung, and signals the incoming trunk to provide the proper ringing.

4. The marker tests the continuity of the circuit paths to be used to the called line before establishing the connection, to insure that the connection is properly set up and that it will be held under control of the subscriber telephone after the marker disconnects. The marker also tests for short circuits, crosses, and grounds, and in case of a failure due to any inoperative condition it will connect itself to the common trouble indicator and leave a record of the trouble and its location and give an alarm to the maintenance force.

Figure 15 shows the complete talking connection through the various trunks and switch frames as finally established after all of the common control circuits have been released.

On a call to a subscriber served by a panel dial office, the connection is

Figure 17. Three frame assembly of district-junctor, district-link, and sender-link frames

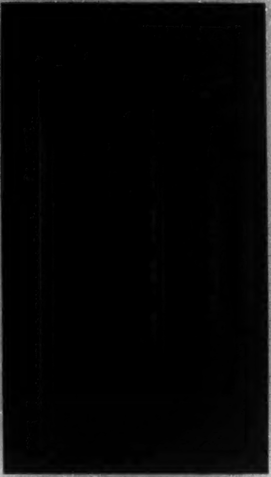


Figure 18. Sender frames

routed through the district-link and the office-link frames in the same manner as on a call terminating in a crossbar office, but in this case the idle trunk chosen on the office-link frame terminates in an incoming panel switch in the distant panel dial office. The subscriber sender of the crossbar office causes the incoming and final selectors in the terminating panel office to select the called subscriber line without the aid of any terminating senders in either office. When the subscriber sender has completed these functions it will be released and the connection will be established from the calling line over the interoffice trunk circuit and through the terminating panel incoming and final selectors to the called line. On this type of call the subscriber sender operates in the same manner as though the called line were in a crossbar office, and the selectors in the panel office operate in the same manner as though the call had originated in another panel office. No changes are required in the panel selectors to function with the crossbar office.

On a call for a subscriber in a manual office, the call would be routed through the switches of the district-link and the office-link frames as previously described and connected to a trunk circuit on the office-link frame which terminates in the *B* switchboard in the manual office. The subscriber sender of the crossbar office then transfers the called number by impulses transmitted over the interoffice trunk circuit to the operator's position equipment in the manual office. The called number appears in the form of visible numbers on the operator's keyshelf. The operator completes the connection by "plugging" the associated trunk circuit, which terminates on a cord and plug, into the called subscriber line jack.

A call originating in a panel dial office for a subscriber line in a crossbar office reaches the crossbar office through an incoming trunk circuit as in the case where the call originated in a crossbar office. The call from the panel office is then handled by the crossbar office terminating sender and marker in exactly the same manner as described for calls originating in the same crossbar office.

A call originating in a manual office for a line connected to the crossbar office reaches the crossbar office over an incoming trunk circuit from an *A* operator's position in the manual office. These incoming trunks in the crossbar office are similar to the incoming trunks previously described. In this case, however, the incoming trunk is connected to a terminating *B* sender and by means of this sender to a *B* board operator in the crossbar office. The *B* operator will obtain the called number verbally from the distant *A* operator and then, by means of the keyset on her position, register the called number in the terminating sender. The terminating sender will then select a terminating marker and the connection will be established in exactly the same manner as described for a call originating in a crossbar office.

MAINTENANCE FACILITIES

Automatic routine testing circuits are provided for testing all the principal circuit units, such as the district junctors, incoming trunks, and senders. These test circuits automatically put each circuit, one after the other, through all of its functions on all classes of calls to insure that it performs satisfactorily. It tests the important relays of the circuits to insure that they have the proper adjustment to handle the worst circuit conditions. In case any circuit fails to meet the test conditions, the test is stopped and an alarm given to the maintenance force.

Trouble indicator circuits are provided for use in connection with the test and maintenance of the marker circuits. These circuits are arranged so that when trouble is encountered by a marker, the marker will seize the trouble indicator and operate combinations of relays and light small lamps which indicate the nature and the location of the failure and give an alarm to the maintenance force.

Figure 19. Originating marker frame

Equipment

Figure 16 shows a typical switch frame used in the crossbar system. This particular frame is a "line link" frame which serves a group of subscribers for both originating and terminating traffic. The frameworks on which the equipment is mounted are constructed of rolled bulb angle-iron sections with a sheet-metal base. The bulb angle construction provides a framework which is light in weight and has the required strength, and permits an equipment mounting arrangement which conserves space and facilitates the wiring of the apparatus. The frames are welded and incorporate such features as sanitary base construction, guards to protect the apparatus and wiring against damage from the rolling ladders located between the rows of frames, and a cable duct or runway for the a-c power service cables with plug receptacle outlets for use with electric soldering irons, portable lights, etc.

These frame equipments are built in standardized units, which provide the required flexibility to satisfy the variations in telephone traffic and classes of service encountered in the different telephone areas. Where it has been necessary to divide an equipment assembly into several units, due to the limitations of handling, shipping, and to care for different classes of service, the equipments have been designed so that the installation effort required for interconnecting such units has been reduced to a minimum.

The bays of equipment located at the right, in figure 16, equipped with crossbar switches, are the primary line-link bays. The vertical units of these crossbar switches are wired to the subscriber lines. These primary bays are made available in units of 100 and 200 line capacities. As discussed previously the number of primary bays provided in a line-link frame may be varied to fit the traffic load of the subscriber lines. The left-hand bay of this frame contains the vertical file of crossbar switches, known as the secondary switches and the vertical units of these switches are wired to district junctors and line junctors. The line-link control circuit apparatus, which is common to the frame, is located at the bottom of this bay.

Figure 17 shows a group of three frame units, namely, the subscriber sender link, the district junctor, and the district-link frames, which are closely associated in the trunking network and have been designed as a fixed equipment group. However, for shipping reasons the group is divided into three separate equipment units. The district-junctor circuits, consisting primarily of relays, are mounted in groups on the middle frame. These groups are provided in standardized units of various types, such as those required to serve coin and noncoin subscriber lines. A similar arrangement of frames is used for the combination of terminating sender link, the incoming trunk, and the incoming-link frames.

Figure 18 shows a row of subscriber sender frames and a frame of *A* operator senders located at the extreme right. These frames accommodate five senders which may be of one type, or a combination of both types. The crossbar switch shown on the right of each subscriber sender unit, is a part of the sender circuit and is employed for the purpose of registering the called numbers dialed by the subscribers. The *A* operator senders are associated with the *A* operator switchboard equipment and are used for the completion of certain classes of calls such as toll and assistance calls.

A view of the originating marker frame is shown in figure 19. There will be a variation in the equipment on this frame for different cities due to the variation in the number of route relays required, the number depending upon the number of central-office codes that may be dialed by subscribers and operators. This variable feature is cared for by providing the route relay equipment in bays of 100 codes as shown in the right-hand bay. The terminal fields shown below the route relays on the frame provide the flexible connecting facilities which permit the use of any route relay for any office code and which readily permit changes in routings, variations in trunk group sizes, and other features which are subject to change from time to time.

Figure 20. Battery supply feeders, power wiring, fusing, etc.

The power-plant equipment provided for the crossbar offices is similar to the equipment now being furnished for all large dial central offices. The principal power supply arrangements provide 48-volt direct current for the operation of practically all the signaling and the telephone transmission circuits. Also several other sources of direct current are provided for miscellaneous purposes as in other standard dial systems. A new distribution scheme for the battery feeders on the frames is employed which reduces the amount of copper required. A common set of 48-volt battery feeders supplies the signaling and talking current for all frames. Individual frame filters are connected across the battery supply leads at the frames where a noise-free battery supply is required for talking circuits. Figure 20 shows a view of the overhead battery cables, conduits for the a-c power leads, and the fuse cabinets for the fusing of the battery supply to a row of frames.

APPLICATION

As mentioned in the first part of this paper, two crossbar dial central offices were cut into service in 1938 and these have now been in commercial operation for several months. One of these offices serves a residential area in Brooklyn, while the other serves a congested business area in the midtown Manhattan district of New York City. The operation of these offices under actual service conditions has been highly satisfactory and our expectations in regard to performance have been fully realized.

This type of system will be used for new offices in large cities instead of the panel system as rapidly as manufacturing and plant conditions permit and the apparatus which was designed for this system will be used in other fields of the telephone system.

Breakdown Studies in Compressed Gases

By ALVIN H. HOWELL
ASSOCIATE AIEE

THE superior dielectric strength of compressed gases, their low dielectric loss, their low dielectric constant, their low cost, and the fact that they are self-healing make them very promising insulating media. Compressed-gas insulation has been used in precision condensers where low dielectric loss is essential; it has been tried in other types of equipment such as transformers; it has been suggested for electric power cables where the low dielectric constant is of considerable importance; and recently it has found interesting and important application in the production of very high voltages by electrostatic means for use in X ray and nuclear physics work.

While several investigations of the insulating properties of compressed gases have been made, nearly all of them have dealt with small total voltages and were made under conditions very different from those that would obtain in a practical application. The present work outlines the scope of former studies, gives an introduction to the physics of breakdown in gases, and presents experimental data for gas pressures, principally air, up to 600 pounds per square inch and direct voltages up to 450,000 volts. The results have been analyzed in the light of existing theories, and new evidence is presented to support the modifications in theory that are suggested.

Experimental Aspects

Spark-over or breakdown between a pair of electrodes immersed in a gas will occur at a voltage which is, at ordinary temperatures, determined primarily by the gas pressure, the electrode separation, the electrode configuration, the nature of the gas, and the nature of the applied voltage, particularly its rate of increase. The effect of pressure and electrode separation on the sparking voltage is given by Paschen's law,* which states that for a given gas and a uniform field the sparking voltage is a function of the product of the electrode separation and the pressure; the more general similarity law holds for electrodes which give nonuniform fields.

The curve in figure 1 shows the relationship between the sparking voltage for air and the product of the pressure and the electrode separation. The minimum exhibited by the curve may be considered to separate the region of vacuum breakdown from that of pressure breakdown. It will be observed that no uniform air gap can have a sparking voltage less than 350 volts. In the region of pressure breakdown the sparking voltage rises steadily as the product of the pressure and the electrode separation is increased.

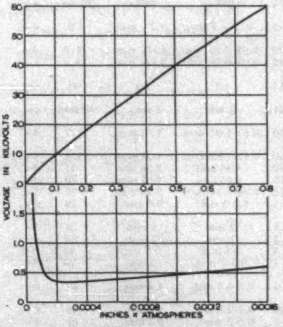


Figure 1. Breakdown voltages for air—the Paschen relationship (from Schumann[13])

Since pressures below atmospheric were used in the experiments which led to the formulation of Paschen's law, the curve in figure 1 has to be restricted with regard to pressure. The numerous tests which have been made at higher pressures, summarized in table I, have shown that data for pressures above a certain value, possibly ten atmospheres, cannot be represented by a single curve.

As figure 1 shows, large sparking voltages occur only when the product of pressure and electrode separation is large. Table I shows that in this region only a few measurements have been made, and they are for alternating voltages. The present work is aimed in the same direction but with direct potentials which, of course, permit a study of the effect of polarity.

With one exception the measurements presented in this paper have been made in air with gauge pressures up to 600 pounds per square inch and voltages up to a maximum of 450,000 volts. These studies have been made with uniform fields, needle point to plane, and concentric-cylinder electrode arrangements.

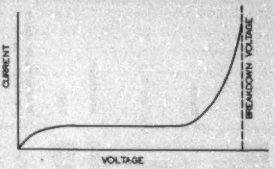


Figure 2. Current flow before the initial discharge

Uniform fields were used because they are of general interest and they permit a proper comparison with the results of others. The needle-point-to-plane arrangement was selected in order to determine the effect of pressure on the current-voltage characteristic of such points. This information is of interest to those operating Van de Graaff generators in compressed air. The cylindrical electrodes are also of general interest. Only cylindrical arrangements having a ratio of cylinder diameters approximately equal to ϵ were used. This ratio produces, for a given outside diameter of cylinder and a fixed voltage between cylinders, the smallest gradient at the surface of the inner conductor.

The current flowing through the gas prior to sparking was studied for the uniform fields and the cylinders as well as for the point-to-plane configuration. This feature of the work, measurement of current above atmospheric pressure, has not been reported by other workers in this field.

Theoretical Background

In order to introduce the theoretical side of the breakdown picture, let the current-voltage characteristic for a pair of plane electrodes be considered. Figure 2 shows that the current first rises in proportion to the voltage at low voltages, then reaches a constant or saturation value (perhaps 10^{-12} amperes with the equipment used in the present experi-

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Paper number 39-10, recommended by the AIEE committee on research and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted August 5, 1938; made available for preprinting November 29, 1938.

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* In reality W. S. Harris[14] discovered the Paschen relationship in 1834, more than a half century before Paschen's publication. This is the more remarkable because absolute potential measurements could not be made until 1860. Harris therefore states his results in terms of the charge on a condenser of fixed capacity; for this case charge and voltage are proportional.

ments and maximum pressure), and later increases rapidly as the breakdown voltage is approached. When this limiting voltage is reached a new equilibrium condition is set up which is determined by the experimental system, but which is characterized by much larger values of current. The new equilibrium condition may be a glow (or some related discharge such as a brush or corona) or it may be an arc. In cases where the glow appears first an arc will appear at a still higher voltage. In the discussion that follows the term initial discharge is used to designate the first discharge that appears, and the voltage at which this occurs is called the ignition voltage or the initial voltage. The term breakdown voltage is used synonymously with sparking voltage to designate the voltage at which the initial discharge transfers to an arc.

Table I. Index to Former Studies of Breakdown at Pressures Above Atmospheric

Name	Reference	Year	Maximum Pressure (Atmospheres)	Maximum Pressure for Maximum Spacing	Range of Spacing	Maximum Spacing for Maximum Pressure	Maximum Potential (Kilovolts)	Nature of Potential	Gas	Electrodes
Wolf	50	1889	5	5	1 mm.	1 mm.	19	d-c	Air, O_2, N_2, CO_2	Spherical segments (10 cm. diameter)
Wolf	50	1889	9	9	1 mm.	1 mm.	19	d-c	H_2	Spherical segments (10 cm. diameter)
Hemptinne	26	1902	60	60	(*a*)	(*a*)	5.6 max	a-c	N_2	Platinum wires (0.2 mm. diameter)
Hemptinne	26	1902	50	50	(*a*)	(*a*)	6.4 max	a-c	CO_2, O_2	Platinum wires (0.2 mm. diameter)
Hemptinne	26	1902	80	80	(*a*)	(*a*)	4.6 max	a-c	H_2	Platinum wires (0.2 mm. diameter)
Ryan	44	1904	100	100	0.156–0.25 in.	0.25 in.	50 max	a-c	Air	Sewing needles (point-point)
Ryan	44	1904	62	62	0.08 –0.25 in.	0.25 in.	58 max	a-c	CO_2	Sewing needles (point-point)
Ekern	6	1904	102	102	0.1–0.3 in.	0.3 in.	43 max	a-c	Air	Sewing needles (point-point)
Ekern	6	1904	42	42	0.2 in.	0.2 in.	50 max	a-c	Air	Various kinds of points
Ryan	42	1905	49	49	0.096 in.	0.096 in.	110 max	a-c	CO_2	Aluminum wires (0.09375 in. diameter) rounded ends
Guye and Guye	15	1905	86	86	~0.2 mm.	~0.2 mm.	19	d-c	Air, N_2, O_2	Platinum wires (1 mm. diameter)
Guye and Guye	15	1905	63	63	~0.2 mm.	~0.2 mm.	12	d-c	H_2	Platinum wires (1 mm. diameter)
Guye and Guye	15	1905	53	53	~0.2 mm.	~0.2 mm.	19	d-c	CO_2	Platinum wires (1 mm. diameter)
Fessenden	10	1905	12	12	0.083 in.	0.083 in.	40 max	a-c	Air	Flat plate condenser. [First application of compressed gas.]
Cassuto and Occhialini	3	1907	102	1.6	0.1–6.3 mm.	0.1 mm.	(*b*)	d-c	Air	Planes (4 cm. diameter)
Ceruti	4	1909	40	25	0.03–0.22 mm.	0.156 mm.	13.5	d-c	Air	Gold plated spheres (15 mm. diameter)
Ceruti	4	1909	50	30	0.03–0.22 mm.	0.125 mm.	24.6	d-c	Air	Platinum plated spheres (15 mm. diameter)
Watson	48	1909	15	15	(*c*)	(*c*)	(*c*)	d-c	Air	Spheres (0.3175, 0.635, 2.54 cm. diameter)
Wien	49	1909	21	21	3 mm.	3 mm.	48 max	a-c	Air, CO_2	Condenser (5 concentric cylindrical plates)
Hayashi	25	1914	70	70	0.52–1.82 mm.	1.82 mm.	117	d-c	Air, N_2	Magnesium spheres (1 cm. diameter)
Hayashi	25	1914	70	60	0.52–1.82 mm.	1.08 mm.	110	d-c	CO_2	Magnesium spheres (1 cm. diameter)
Hayashi	25	1914	30	30	0.58 mm.	0.58 mm.	41	d-c	N_2 and H_2 mixed	Gold spheres (1 cm. diameter)
Hayashi	25	1914	70	70	0.65 mm.	0.65 mm.	70	d-c	(*d*)	Gold spheres (1 cm. diameter)
Guye and Stancescu	19	1917	53	16	0.34–2.24 mm.	0.34 mm.	35	d-c	CO_2	Gold plates (14 mm. diameter)
Guye and Mercier	16	1920	26	4.1	0.5–5 mm.	0.5 mm.	54	d-c	CO_2	Planes (45 mm. diameter)
Guye and Mercier	16	1920	4.1	4.1	5 mm.	5 mm.	53	d-c	CO_2	Calottes (150 mm. radius of curvature)
Guye and Mercier	16	1920	23.1	23.1	1 mm.	1 mm.	52	d-c	CO_2	Calottes (30 mm. radius of curvature)
Guye and Mercier	16	1920	34.9	34.9	0.5 mm.	0.5 mm.	43	d-c	CO_2	Calottes (15 mm. radius of curvature)
Hammershaimb and Mercier	23	1921	1*	1	0.5–5 mm.	5 mm.	18	d-c	N_2	13 sets of electrodes ranging from points to planes
Hammershaimb and Mercier	23	1921	4*	4	0.5–4.5 mm.	4.5 mm.	52	d-c	N_2	9 sets of electrodes
Hammershaimb and Mercier	23	1921	10*	10	0.5–2.5 mm.	2.5 mm.	69	d-c	N_2	8 sets of electrodes
Hammershaimb and Mercier	23	1921	50	1	0.5–5 mm.	0.5 mm.	69	d-c	N_2	Spherical segments (15 mm. diameter)
Hammershaimb and Mercier	23	1921	55	1	0.5–5 mm.	0.5 mm.	75	d-c	N_2	Planes (10 mm. diameter)
Hammershaimb and Mercier	23	1921	50	1	0.5–5 mm.	0.5 mm.	80	d-c	N_2	Planes (45 mm. diameter)
Guye and Mercier	17	1922	51	5.5	0.5–5 mm.	0.5 mm.	62	d-c	CO_2	Spherical segments (15 mm. diameter)
Hammershaimb	22	1923	50	10	0.5–2.5 mm.	0.5 mm.	80	d-c	N_2	Spherical segments (15 mm. diameter)
Guye and Weigle	20	1923	22	22	0.5 mm.	0.5 mm.	24	d-c	CO_2	Spherical segments (15 mm. diameter)
Guye and Weigle	20	1923	43	23	0.5–1 mm.	0.5 mm.	54	d-c	CO_2	Spherical segments (15 mm. diameter)
Guye and Weigle	20	1923	38	20	0.5–1 mm.	0.5 mm.	60	d-c	N_2	Spherical segments (15 mm. diameter)
Guye, Mercier, and Weigle	18	1925	40	5	0.5–5 mm.	0.5 mm.	56	d-c	CO_2	Spherical segments (15 mm. diameter)
Palm	34	1926	15	15	2–8 mm.	8 mm.	220 max	a-c	N_2	Planes (108 mm. diameter)
Palm	34	1926	15	15	2–8 mm.	8 mm.	255 max	a-c	CO_2	Planes (108 mm. diameter)
Goodlet, Edwards, and Perry	14	1930	5	5	0–10 cm.	10 cm.	100 eff	a-c	Air	Rods ($^1/_2$ in. diameter), rounded ends
Reher	38	1931	26	2	1–18 mm.	2 mm.	110 eff	a-c	Air	Planes (about 5 to 8 in. diameter)
Reher	38	1931	28	10	0.5–4 mm.	2 mm.	105 eff	a-c	Air	Spheres (16 mm. diameter)
Bölsterli	1	1931	17*	17	0–6 cm.	6 cm.	200 eff	a-c	N_2	Needle gap
Bölsterli	1	1931	20	20	3.2 mm.	3.2 mm.	60 eff	a-c	N_2, CO_2	Concentric cylinders (25.4 and 31.8 mm. diameter)
Bölsterli	1	1931	20	20	3.2 mm.	3.2 mm.	13 eff	a-c	He	Concentric cylinders (25.4 and 31.8 mm. diameter)
Zeier	51	1932	116	5	0.1–5 mm.	0.4 mm.	100	d-c	Air	Spheres (15 mm. diameter)
Zeier	51	1932	48	5	0.2–5 mm.	0.6 mm.	80	d-c	Air	Spherical segments (300 mm. diameter)
Zeier	51	1932	48	48	0.3–0.5 mm.	0.5 mm.	70	d-c	Air	Sphere (2 mm. diameter)—segment (300 mm. diameter)

* = constant at value stated.

(*a*) Very small; not specified. (*b*) Arbitrary units. (*c*) Results show gradients as a function of pressure. (*d*) Air, N_2, O_2, CO_2, H_2, and illuminating gas.

To understand the behavior of the current it is necessary to know that normally cosmic rays, radioactive radiations, and photoelectric action are constantly producing ions and electrons in the gas, at the electrodes, and at the walls of the container if there are any. At low voltages the current is produced by the movement of these charge carriers, under the action of the electric field, to the electrodes. The saturation condition is reached when the charge carriers arrive at the electrodes at the same rate as that at which they are produced by the causes mentioned above, allowing of course for such effects as recombination. The current rises above the saturation value when the electric field is able to cause a multiplying of the charge carriers in the gas and at the electrodes. This multiplying action increases rapidly with

Table I (Continued). Index to Former Studies of Breakdown at Pressures Above Atmospheric

Name	Reference	Year	Maximum Pressure (Atmospheres)	Maximum Pressure for Maximum Spacing	Range of Spacing	Maximum Spacing for Maximum Pressure	Maximum Potential (Kilovolts)	Nature of Potential	Gas	Electrodes
Zeier	51	1932	116	5	0.2–4 mm.	0.6 mm.	80	d-c	N_2	Spheres (15 mm. diameter)
Zeier	51	1932	48			0.8 mm.	80	d-c	N_2	Spherical segments (300 mm. diameter)
Zeier	51	1932	29			1.2 mm.	80	d-c	N_2	Segment (300 mm. diameter)—plane
Zeier	51	1932	48	48	0.3–0.5 mm.	0.5 mm.	58	d-c	N_2	Sphere (2 mm. diameter)—segment (300 mm. diameter)
Zeier	51	1932	56.5	5	0.2–4 mm.	0.6 mm.	90	d-c	CO_2	Spheres (15 mm. diameter)
Zeier	51	1932	48				75	d-c	CO_2	Spherical segments (300 mm. diameter)
Zeier	51	1932	48	5	0.1–~3 mm.	0.6 mm.	70	d-c	CO_2	Spheres (2 mm. diameter)
Zeier	51	1932	39	39	0.5 mm.	0.5 mm.	58	d-c	Air and N_2 mixed	Spheres (15 mm. diameter)
Palm	33	1934	25	14	1–16 mm.	1 mm.	280 max	a-c	N_2	Planes (108 mm. diameter)
Palm	33	1934	23	18	1–8 mm.	1 mm.	290 max	a-c	CO_2	Planes (108 mm. diameter)
Palm	33	1934	10*	10	6.5 mm.	6.5 mm.	375 max	a-c	N_2	Concentric cylinders (38 and 51 mm. diameter)
Palm	33	1934	15.7*	15.7	18.5 mm.	18.5 mm.	445 max	a-c	N_2	Concentric cylinders (133 and 170 mm. diameter)
Palm	33	1934	14*	14	23.5 mm.	23.5 mm.	556 max	a-c	N_2	Concentric cylinders (150 and 197 mm. diameter)
Palm	33	1934	14*	14	6.4 mm.	6.4 mm.	228 max	a-c	N_2	Concentric cylinders (25.4 and 31.8 mm. diameter)
Goldman and Wul	12	1934	17	17	3–17.5 mm.	17.5 mm.	80 max	a-c	N_2	Point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	12	1934	17	13	1–10 mm.	5 mm.	130	d-c	N_2	Negative point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	12	1934	17	17	1–50 mm.	50 mm.	150	d-c	N_2	Positive point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	12	1934	17	9	3.7–10 mm.	3.7 mm.	115	(*e*)	N_2	Positive point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	12	1934	17	6	3.7–10 mm.	5 mm.	160	(*e*)	N_2	Negative point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	13	1935	25	25	10 mm.	10 mm.	55	d-c	N_2	Positive point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	13	1935	25	25	10 mm.	10 mm.	130	d-c	N_2	Negative point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	13	1935	25	25	10 mm.	10 mm.	50 max	a-c	N_2	Point (platinum wire 0.435 mm. diameter)—plane
Goldman and Wul	13	1935	31	31	10 mm.	10 mm.	50 max	a-c	N_2	Point-plane. Temperature = 13°C.
Goldman and Wul	13	1935	31	31	10 mm.	10 mm.	36 max	a-c	N_2	Point-plane. Temperature = 100°C.
Goldman and Wul	13	1935	Up to 48		Up to 20 mm.		150	a-c and d-c	N_2	Various odd shapes (all unsymmetrical gaps)
Finkelmann	11	1937	20	~ 9	2–20 mm.	7 mm.	375 max	a-c	N_2	Brass plates (13.5 cm. diameter)
Finkelmann	11	1937	20	~ 7	1–20 mm.	7 mm.	375 max	a-c	CO_2	Brass plates (13.5 cm. diameter)
Finkelmann	11	1937	20	~ 8	1–20 mm.	7 mm.	400 max	a-c	Air	Brass plates (13.5 cm. diameter)
Finkelmann	11	1937	20	~14	1–20 mm.	10 mm.	380 max	a-c	H_2	Brass plates (13.5 cm. diameter)
Finkelmann	11	1937	19	19	9.35 mm.	9.35 mm.	250 max	a-c	Air, N_2, CO_2	Concentric cylinders (180 and 199.4 mm. diameter)
Finkelmann	11	1937	21	21	9.35 mm.	9.35 mm.	165 max	a-c	H_2	Concentric cylinders (180 and 199.4 mm. diameter)
Finkelmann	11	1937	11	11	19.3 mm.	19.3 mm.	370 max	a-c	Air, N_2, CO_2	Concentric cylinders (159.8 and 199.4 mm. diameter)
Finkelmann	11	1937	16	16	19.3 mm.	19.3 mm.	360 max	a-c	H_2	Concentric cylinders (159.8 and 199.4 mm. diameter)
Finkelmann	11	1937	8	8	29 mm.	29 mm.	380 max	a-c	Air, N_2, CO_2	Concentric cylinders (140.0 and 199.4 mm. diameter)
Finkelmann	11	1937	13	13	29 mm.	29 mm.	375 max	a-c	H_2	Concentric cylinders (140.0 and 199.4 mm. diameter)
Finkelmann	11	1937	6	6	39.7 mm.	39.7 mm.	375 max	a-c	Air, N_2, CO_2	Concentric cylinders (119.2 and 199.4 mm. diameter)
Finkelmann	11	1937	10	10	39.7 mm.	39.7 mm.	365 max	a-c	H_2	Concentric cylinders (119.2 and 199.4 mm. diameter)
Rodine and Herb	40	1937	5	3	2–10 mm.	5 mm.	100	d-c	(*f*)	Brass spheres (5 cm. diameter)
Hudson, Hoisington, and Royt	29	1937	7	7	3 mm.	3 mm.	150	d-c	(*g*)	Brass spheres (5 cm. diameter)
Hudson, Hoisington, and Royt	29	1937	7	7	3 mm.	3 mm.	98	d-c	(*h*)	Brass spheres (5 cm. diameter)

* = constant at value stated.

(*e*) Impulse. (*f*) Various mixtures of CCl_4 and air. (*g*) Various mixtures of CCl_2F_2 and air. (*h*) Various mixtures of SO_2 and air.

voltage at voltages just below the ignition value. At the ignition voltage the current ceases to depend on external sources of ionization; it becomes self-sustaining.

A complete analysis of the problem involves accurate knowledge about all the possible methods of producing charge carriers either at the electrodes or in the gas, about the manner in which these carriers are moved through the gas as a result of diffusion and the influence of the electric field, and finally about the manner in which they may disappear. At the present time it is recognized that new carriers may be produced by a number of methods, but adequate quantitative knowledge about most of these is not yet available.

Plausible ionization processes in the gas are: (1) ionization by electron impact; (2) ionization by positive-ion impact; (3) photoelectric action; (4) ionization by metastable atoms and excited atoms; (5) multistage ionization (all types); (6) ionization by neutral atoms; (7) thermal ionization.

The anode need not be considered because electrons coming from it would immediately return. The possibility of the emission of positive ions from the anode appears remote and, so far, is not supported by experiment.

Clearly the difficulty is not one of finding a conceivable process by which the charge carriers can be multiplied, but rather one of finding which of the many available ones are important. There is general agreement that the action of electrons in the gas is the important or primary process. This is the α process. It assumes that one electron produces α new ion pairs in traveling one centimeter through the gas. Now if an electron leaves the cathode, new ones will be generated in geometrical progression, giving rise to an electron avalanche; the number of electrons in the avalanche is $\epsilon^{\int_0^d \alpha dx}$ after the distance d to the anode has been traversed. Experimental tests have shown that the current rise just preceding the initial discharge cannot be accounted for by the α mechanism alone.

The β mechanism assumes that the positive ions ionize neutral gas molecules as they move to the cathode and that one ion in moving one centimeter produces β new ion pairs. On the assumption that only the α and β processes are active, an equation may be derived which is able to describe the current rise before the initial discharge. The equation furthermore predicts an infinite current for a certain combination of the variables α, β, and d, or for a certain combination of pressure, voltage, and electrode separation. The relationship of variables predicting unlimited current flow has been interpreted to be the condition necessary for the initial discharge, and has been found to be in agreement with experimental values. The self-sustaining current which accompanies the initial discharge is established when the ions produced by one electron are able to produce one electron.

An alternate possibility is the γ mechanism which assumes that the positive ions liberate electrons when they strike the cathode, and that γ electrons are emitted by each positive ion. This assumption with the α mechanism also allows the current before the initial discharge to be described and the ignition voltage to be predicted.

It happens that there are still other mechanisms which when taken with the α process will allow the predischarge current to be described and the initial voltage to be predicted. They are the action of photons in the gas or at the cathode, and the action of metastable atoms in the gas or at the cathode. The most generally accepted one is the η mechanism which assumes that photons liberate electrons from the cathode and that η electrons are liberated by each photon. Of course it is also possible to satisfy the current and voltage requirements with a combination of two or more of the secondary processes.

The multiple solution difficulty arises from the fact that, in all equations, α dictates the principal behavior of the current rise in that it governs exponentials whereas the other quantities play a less dominating role. The experimental data are not especially precise and do not permit a distinction to be made between the various equations.

From the standpoint of understanding the elementary physical processes that are at work, it is evident that little can be concluded from considering only the predischarge current rise and the initial voltage. Since it has already been mentioned that one of the features of the present work is the measurement of current above atmospheric pressure, it might at first seem that this is misdirected effort. However, the currents which can be described by the equations mentioned above are not the currents measured in this study; the currents measured here are self-sustaining values.

Additional information has been obtained from other sources, such as the time necessary to initiate the self-sustaining condition, photographs of the early stages of breakdown, direct experiments which study the individual elementary processes, and the experience with all the various types of gas discharges. The evidence seems to indicate that many of the proposed mechanisms are either absent or that they are too rare to consider in the ordinary sparking phenomenon. At present it is believed that the important ones for the common gases are the α, γ, and η processes, with the additional consideration of field distortion due to space charge. For long sparks in mixed gases such as lightning strokes, or for self-sustaining discharges from isolated electrodes, it appears that photo-ionization in the gas becomes important as well. Here it seems necessary to have the electrons regenerated in the gas rather than at the electrodes, as they are with the γ and η processes.

Figure 3. Pressure vessel and generator

To date the theory has been concerned principally with explaining breakdown at pressures which do not exceed atmospheric. At higher pressures one might expect to find new processes at work; a discussion of this possibility will be included with the results.

Apparatus

The experiments presented in this paper were made in a special portable pressure vessel having an internal diameter of 13 inches and an inside length of 42 inches.

The tank was fabricated by welding together standard outside diameter pipe and steam fittings. It had two hand holes near the center and each contained a window. The tank was so suspended on its portable truck that all the operations, including assembly, could be managed readily by one person. Figure 3 shows the complete vessel and the generator in the operating position.

The high direct potentials (up to one-half million volts) as obtained from a Van de Graaff generator, were introduced into the pressure chamber through a solid Textolite bushing which was forced into a tapered nozzle at the upper end of the tank. The bushing extended 15 inches into the container and 65 inches outside.

The electrodes used for producing uniform fields were turned from ordinary cold-rolled steel. They had a flat section 2 inches in diameter and edges formed by using a constantly decreasing radius of curvature. The maximum diameter of each electrode was $7^{1}/_{16}$ inches. Figure 4 shows a sectional view of the three types of electrodes that were used. Experience showed that the distribution of sparks over the flat area was uniform and that the sparks were practically all confined to this area. For this reason it was assumed that the electrodes produced a satisfactory uniform field.

The low-potential electrode was supplied with a ball and socket joint which permitted the electrodes to be made perfectly parallel at any time simply by forcing them together. This electrode was moved by a fine screw which allowed the electrode separation to be known to within 0.001 inch. The technique used eliminated back lash, and stretching of the tank was readily corrected for by adjusting the scale. An ordinary spark plug was used as a bushing for leading the current through the tank wall; the leads both inside and outside the tank were shielded.

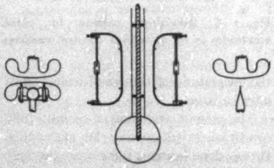

Figure 4. Electrode configurations: uniform field, concentric cylinder, point to plane

The needle-point-to-plane configuration was realized by replacing the low-potential electrode of the system described above with a 30-degree cone having a phonograph needle set in its point.

Copper was used for making the cylindrical electrodes for constructional reasons. In each case the inner electrode was attached to the conductor running through the bushing and terminated in an aluminum sphere which insured that the highest gradient would occur in the test section. The outer cylinders were held between spun flanges which were at ground potential, the cylinder was insulated from the flanges. The whole system was held in position by four adjustable screws directed radially against the tank wall from each flange. This allowed the cylinders to be made perfectly concentric. Experience showed that the distribution of sparks within the length of the outer cylinder was perfectly uniform and the insulating feature made it possible to measure the current flow to the outer cylinder.

Four sets of cylindrical electrodes were employed and each of these had a ratio of diameters as nearly equal to ϵ as it was possible to obtain them. The combinations of diameters used were $^{1}/_{2}$ inch and $1^{1}/_{16}$ inches; $^{3}/_{4}$ inch and $2^{1}/_{16}$ inches; $1^{1}/_{8}$ inches and 3 inches; and $1^{1}/_{2}$ inches and 4 inches. The outer cylinder was 5 inches long in all cases.

Gas was obtained in commercial tanks and was dried by passing it slowly through a five-foot tower filled with Drierite and then through phosphorus pentoxide. The moisture inevitably trapped in the vessel was removed by placing a container of phosphorus pentoxide in the tank and circulating the enclosed gas for a period of 10 or 12 hours. As the results will show, air was generally employed, though two tanks of nitrogen and one of helium were used in some special experiments.

The voltage measurements were made with a special resistor consisting of 960 10-megohm units connected in series and encased in a suitable Textolite container filled with insulating oil. A microammeter (accuracy one-half of one per cent) was used for measuring the resistor current. Numerous tests indicated that this voltage measuring system certainly gave an accuracy of two per cent, and probably of one per cent. The meter was used within a grounded case and was protected from surges by the usual system of a neon bulb and a suitable series resistance. The long lead to the resistor was completely shielded.

The current to the low-potential electrode was measured with a multiple-range semisuspension meter which was used in a grounded case with surge protection and shielded leads. Currents as small as 0.005 microampere were detectable with this instrument.

The pressure was measured with two Crosby steam gauges, one measuring pressures to 200 pounds and the other to 1,000 pounds.

The following simple and definite procedure was always followed in taking observations. The potential was increased in a step-wise fashion from a value definitely below the minimum voltage at which the gap would break down, until a spark was produced. The increments approximated one per cent or two per cent, and voltage was held constant for one minute at each step. The value at which breakdown occurred and also the current flowing just before breakdown were recorded. The procedure was repeated until about ten readings were available for averaging.

Although the technique employed is time consuming, it was felt that it produced the desired results. The readings of any one group sometimes differed from the average by as much as ten per cent, but the average was generally reproducible to within two to five per cent. The large amount of scattering occurred only at the higher pressures, where the time lag becomes excessively large; at low pressures, where the lag is small, the measurements can be taken a good deal faster and more accurately. Inasmuch as the breakdown generally consisted of a single spark, and this of short duration because of the small capacity of the generator, no trouble was experienced from the arc effect such as Reher[38] reports.

Some of the workers who have made measurements at high pressures have used ultraviolet radiation, X rays, or radioactive radiations on the cathode to cut down the time lag. Such devices were not used in this work, as it was desired to study the currents existing normally without added sources of ionization.

Results

UNIFORM FIELD

The results for the uniform field may well be introduced by considering figures 5 and 6. The first of these shows values of breakdown voltage as a function of electrode separation for various constant pressures. Clearly the voltage increases almost linearly with the separation at any pressure. Figure 6 shows the values of breakdown voltage as a function of pressure for various fixed separations. Here the voltage increases linearly with the pressure for the first eight or ten atmospheres and then more and more slowly as the pressure is raised still further. At 600 pounds the voltage is less than 60 per cent of what it would be if the linear rise had continued throughout the entire range. As other workers have observed at smaller spacings, the Paschen law does not apply above about ten atmospheres.

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[38] For all numbered references, see list at end of paper.

The above results were obtained from a single sample of carefully dried air. The measurements were repeated for three other samples of air, and it was found that the results are reproducible to within a few per cent when proper care and precautions are taken to have all essential conditions the same.

However, it is possible to get breakdown voltages widely different from those that have been presented. For example, by simply sanding the flat part of the electrodes the breakdown voltage at the higher pressures can be reduced by a factor of three or four. At the lower pressures the reduction is smaller, but even at one or two atmospheres it may be as much as 25 per cent for the first few sparks.

The detrimental effect of roughening the electrodes can be overcome by prolonged sparking. At low pressures the voltage rises rapidly with the first few sparks and quickly attains a maximum value. At higher pressures the maximum voltage is not attained until several hundred, or possibly thousands of sparks have passed. The beneficial result from sparking may be termed electrical conditioning of the electrodes and the measurements so far presented apply to completely conditioned electrodes. The results for such electrodes were found to be independent of the polarity of the generator or of the sample of air; they were the same when measurements were made with ascending or descending pressures.

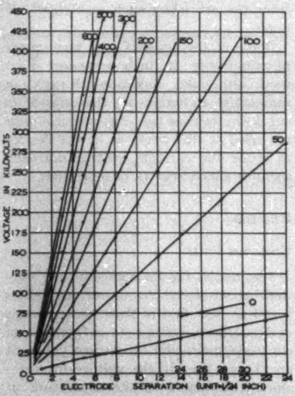

Figure 5. Breakdown voltages for plane electrodes in air at various constant gauge pressures

Figure 7 shows the effect of electrode conditioning on the breakdown voltage for a one-fourth-inch separation. The upper curve applies to well-conditioned electrodes, and the two lower curves to roughened electrodes; roughening has reduced the breakdown voltage by a factor of only two in this case. The two remaining curves apply to electrodes which have been partially conditioned. Both show the significant fact that conditioning is completed at the lower pressures before it is at the high ones. The curve which exhibits a maximum is of interest in that it shows how a carefully taken set of measurements may yield entirely misleading results. A considerable amount of conditioning was effected during the taking of these measurements because a number of spacings were studied at each pressure; only the results for the one-fourth-inch separation are given. The different points on this peaked curve therefore correspond to different amounts of electrode conditioning. This is not true for the other curves because their points were taken in quick succession using only one separation. The peak occurred because an amount of conditioning sufficient to compensate for the 100-pound reduction in pressure accrued between the time the one-fourth-inch point was taken at 600 pounds and the time the corresponding point was taken at 500 pounds. The anomalous results obtained by Ceruti,[4] where he found breakdown voltages increasing faster than either pressure or separation, may well have been caused by conditioning phenomena.

The conditioning process is evidently an electrode phenomenon. To verify this the sample of gas was changed (pressure = 500 pounds) when conditioning was partially complete and again when fully complete; in neither case was the breakdown voltage affected. Furthermore it seems that it is the irregularities on the electrodes which are responsible for the reduction in breakdown voltage; the sparking presumably burns off the irregularities. A compelling reason for believing this is that at small spacings tiny flashes can generally be seen on the surface of rough electrodes at the instant of sparking. They disappear by the time the conditioning process is completed. That the irregularities involved are extremely small is evidenced by the fact that no surface was produced which did not need some conditioning; the most satisfactory one was made by using the finest sandpaper, then crocus cloth, and finally buffing with rouge on a cloth wheel.

Other investigators have also mentioned that the breakdown voltage is augmented by operation of the apparatus; Wien[40] gives numerical values indicating a 20-per-cent improvement with use. Evidently a variety of results can be obtained for a given set of conditions with any apparatus and it is, therefore, necessary when reporting results to specify definitely the conditions under which they were taken. It is not unreasonable to suppose that part of the lack of agreement among the results of the different workers may arise from this source, since the magnitude of the conditioning effect may be large.

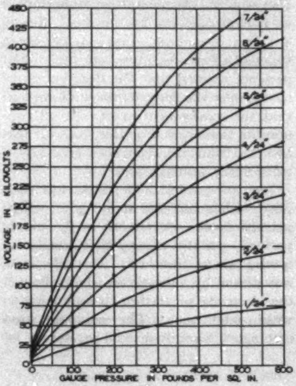

Figure 6. Breakdown voltages for plane electrodes in air at various constant spacings

The present procedure of quoting fully conditioned values may be misleading. It was done because these were the only values which were found to be reproducible. From an engineering standpoint it would seem that they are much too optimistic. From the standpoint of making comparisons with other results it leaves something to be desired because the amount of conditioning possible with each apparatus is presumably different.

But how can tiny irregularities on the surface of the large electrodes produce such a reduction in the breakdown voltage? In order to understand this it is necessary to know the important physical aspects of the breakdown mechanism at higher pressures. A knowledge of these processes is also necessary to explain the results obtained with fully conditioned electrodes and the reason for the failure of Paschen's law here.

It was mentioned earlier that in the ordinary sparking phenomenon for the common gases the important mechanisms are probably the so-called α, γ, and η processes, with the additional consideration of field distortion due to space charge. Photoionization was also mentioned for long sparks in mixed gases. These processes are doubtless also at work at the higher pressures.

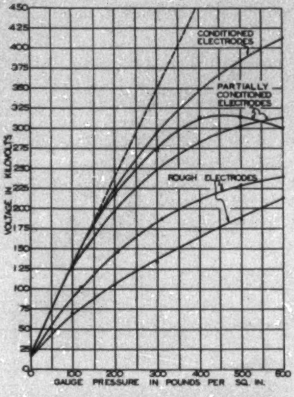

Figure 7. Effect of electrode conditioning on the breakdown voltage for plane electrodes in air at one-fourth-inch spacing

It has been suggested that the role of space charge becomes increasingly important as the pressure is raised and that the field distortion produced by it is the reason for the failure of Paschen's law at higher pressures. The assumption is that at pressures below ten atmospheres the effect of space charge is small and the field is uniform. The density of the space charge increases with the pressure and at about ten atmospheres it is sufficient to cause the gradient at the cathode to rise above the average gradient; at this time the breakdown voltage ceases to rise linearly with the pressure. Indeed, the hypothesis seems plausible, but it does not explain the detrimental effect of electrode roughening. Surely it teaches that the breakdown is initiated in the overstressed cathode region and that only the cathode irregularities are of importance. Roughening only one electrode should, therefore, introduce a polarity effect. This experiment was carried out and it was found that sanding the surface of one electrode reduced the breakdown voltage for both polarities by approximately the same amount.

The space-charge hypothesis may be modified to explain the lowering from roughnesses by assuming that self-sustaining point discharges are set up on the minute surface irregularities. Since a discharge of this type may exist on either a positive or a negative point, the detrimental effects are not restricted to roughnesses on the cathode. Lowering from points on the cathode could be caused by the space charge as explained above. Lowering from points on the anode might be due to the chaining of electron avalanches as discussed with the results for the point-to-plane configuration.

The existence of point discharges seems more reasonable when the current flow is considered. Figure 8 shows the current-voltage characteristic for plane electrodes at various stages of the conditioning process (one-fourth-inch separation, 500-pounds pressure). The actual numerical values should not be depended upon because they vary a great deal. It will be observed that this characteristic always has the shape one would expect for a point discharge. Further, it is difficult to account for current of this magnitude without assuming that a self-sustaining discharge exists; it definitely cannot be accounted for on the basis that it is the normal predischarge current which was discussed in connection with figure 2.

The point-discharge assumption is also consistent with the fact that roughnesses are less effective in producing voltage reductions at low pressure. Here the mean free path is longer than at high pressure, and only a few irregularities are of sufficient magnitude to permit the discharge to be established. The first few sparks remove these and conditioning is complete. At higher pressures, where the mean free path is reduced, point discharges can take place from smaller irregularities. Those are probably more numerous, making it necessary to pass many sparks before the conditioning is complete.

That the current-voltage characteristic retains its shape and the current its approximate magnitude throughout the conditioning process is also in harmony with the hypothesis. For very rough electrodes the discharges easily take place from the big irregularities and only a relatively low voltage is required. When the larger irregularities are removed a higher voltage is required for the discharge, and the curve for the current is shifted along the voltage scale as was shown.

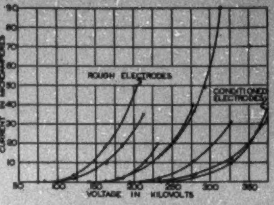

Figure 8. Effect of electrode conditioning on the current flow between plane electrodes (air at 500 pounds, one-fourth-inch spacing)

The point-discharge hypothesis is further fortified by a second method of conditioning the electrodes. It was found that conditioning could be effected by maintaining the voltage just below the breakdown value for relatively long periods of time. Conditioning amounting to perhaps 80 per cent of that realizable by sparking could be produced in 20 or 30 hours. The self-sustaining point discharge tends to remove the irregularity on which it was formed, and in these long periods this could produce a goodly amount of conditioning. It also seems probable that this method would not be capable of carrying the conditioning as far as the sparking process would carry it.

The space-charge hypothesis and the modification of it that has been suggested are not the only plausible processes which apply to breakdown at high pressures. It has also been suggested that Paschen's law fails because the gradient at the cathode becomes so high that field emission initiates breakdown. At ten atmospheres, where the law begins to fail, the average gradient is somewhat less than 0.4 × 10^6 volts per centimeter. The actual gradient is modified by the presence of space charge as well as by surface irregularities, and it may be possible under existing conditions that field emission becomes an important process. This hypothesis is easily able to account for the reduced breakdown voltages found with the roughened electrodes. But it certainly demands that roughnesses on the cathode are the important ones, and therefore that a polarity effect should exist. The absence of such an effect appears to be an irreconcilable contradiction of the hypothesis.

Another possibility is that the effect of the high field enters by lowering the potential barrier at the cathode, thus facilitating the escape of electrons.

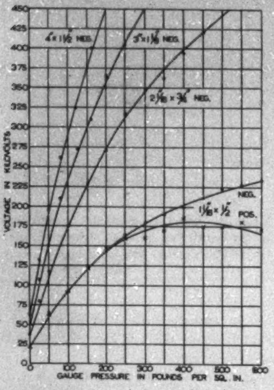

Figure 9. Breakdown voltages for cylindrical electrodes in air (inside cylinder positive and negative)

Either the γ mechanism, which assumes that positive ion bombardment of the cathode regenerates primary electrons, or the η mechanism, which assumes that photoelectric effects at the cathode regenerate the initial electrons, could be aided by such a field. Certainly this would occur at gradients lower than the values which are necessary for cold emission and the resultant lowering would set in gradually. This hypothesis, like that for field emission alone, is admirably suited to explain lowering of the breakdown voltage by irregularities on the cathode, but fails to account for the absence of a polarity effect.

Of the hypotheses which have been presented, only one seems to be consistent with the observations relating to the voltage lowering with rough electrodes and the current flowing between them. But this does not mean that the point-discharge hypothesis is to be used alone. Indeed it is able to explain only the voltage lowering due to irregularities and does not account for the failure of Paschen's law that is observed at higher pressures with fully conditioned electrodes, unless it is assumed that the electrodes can never be entirely free from irregularities which are important at the higher pressures. There is some evidence to support even this assumption in that it was found that there was always some current flow at high pressures prior to sparking.

Another phenomenon that was observed at low as well as high pressures is that after a delay of ten hours or more it was generally found that the first few sparks occurred at a somewhat reduced voltage. This is understandable if one supposes that minute irregularities are produced on the surface either by rusting action or by the settling of dust particles on the surface.

CONCENTRIC CYLINDERS

The results for the four sets of cylindrical electrodes are shown in figure 9. The smallest pair of cylinders showed a clear-cut polarity effect at the high pressures. The larger ones showed a decided polarity effect, and in addition the measurements for them were especially uncertain for the positive polarity (inside cylinder positive). For this case there seemed to be no definite breakdown voltage which could be reproduced. At the higher pressures the values ranged from about 50 to 80 or more per cent of the breakdown voltage for the negative polarity. In this respect the behavior of the cylinders was somewhat like that of the positive point-to-plane configuration. This might be expected since the cylinders present a less severe case of a nonuniform field. A conditioning effect was also present; however, the improvement with sparking was often suddenly terminated by a burst of sparks and a great reduction in the breakdown voltage. The voltage usually improved with further operation, but the lowering was ultimately repeated. This behavior suggests that the sparking did not always improve the electrodes, but sometimes damaged them. For the negative polarity the cylinders behaved like plane electrodes with regard to conditioning.

It is something of a surprise to find that the positive polarity supports the smaller voltage at high pressures. The field emission hypothesis, the modification of it, and the space charge assumption, all indicate that the reverse ought to be true. The point discharge proposition is again the only one of those proposed which seems to agree with the experimental results.

In the present case of a nonuniform field the self-sustaining discharge must always be formed on the inner cylinder. Then to account for the polarity effect observed, it is necessary to assume that at high pressures the positive point is somewhat more effective in producing voltage lowering than the negative point. One might also conclude that its action is somewhat more erratic since measurements for the positive case were always characterized by some uncertainty. This is in agreement with the results obtained for needle points, which will be discussed in the following section.

Throughout the experiments tiny flashes could be observed on the inner cylinder at the instant of sparking. As with the plane electrodes, currents were associated with roughened surfaces and the current was reduced by conditioning.

Table I shows that only two experimenters have previously studied breakdown as a function of pressure for concentric cylinders and that both have used alternating potentials. The polarity effect reported here is evidently new. The results of Finkelmann,[11] which show that the alternating voltage increase above ten atmospheres is small for air and N_2, agree with those reported here for direct potentials.

NEEDLE POINT TO PLANE

For the needle-point-to-plane arrangement only a few typical results can be included. Measurements were made on a number of gas samples for the electrode spacings, one-fourth inch, one-half inch, one inch, and two inches. Quantitative results will be given only for air and for one spacing.

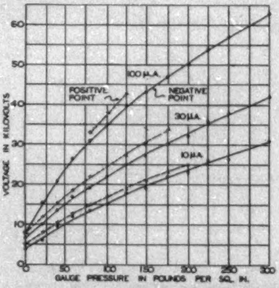

Figure 10. Voltages to produce specified currents in air with point-to-plane electrodes spaced one-fourth inch

The results for the needle-point-to-plane arrangement can best be introduced by considering figure 10. Here the voltages required to spray definite currents of 10, 30, and 100 microamperes are plotted as a function of pressure for both polarities of the point. It will be observed that the voltage does not increase as rapidly as the pressure and that it is not greatly different for the two polarities. Also, the curves for positive polarity do not extend above 200 pounds per square inch. The reason is that above this pressure a steady point discharge cannot be maintained from a positive point; a spark occurs instead. The curves for the negative point are arbitrarily stopped at 300 pounds;

they extend to 600 pounds, and no doubt beyond.

That a critical pressure exists is shown in another way in figure 11. Here the upper curves represent the sparking voltage for a positive point and the lower curves show the normal current flow just before sparking occurs. The voltage rises until a pressure of about 100 pounds is reached, then it becomes uncertain and may have any value between the two curves shown, finally at 150 pounds dropping suddenly to the initial potential curve. The initial potential is the voltage at which the point discharge and, therefore, appreciable current flow start. The current curve behaves in a somewhat similar manner. Two important exceptions are that it falls to zero above the critical pressure and that it exhibits a peak at low pressures. It will also be observed that the critical pressure is not 200 pounds as it was for the set of curves shown in figure 10. The reason is that the former results apply to a new needle point, whereas these apply to one that had been dulled by use. The dull point was employed because it does not change appreciably with a reasonable amount of sparking. It is a significant fact that the critical pressure is lower for a dull point than for a new one.

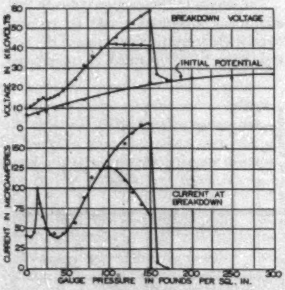

Figure 11. Voltage and current characteristics for positive-point-to-plane electrodes spaced one-fourth inch in air

The sudden rise in the current curve at very low pressures is associated with a change in the appearance of the visible discharge. At atmospheric pressure a dull point maintains a glow just at the tip of the needle, though sometimes a faint beam extends across to the cathode. When the pressure is increased a few pounds, the discharge (usually a glow but sometimes a brush) suddenly lengthens and the current rises. At first it extends across to the cathode, but it shortens and the current drops rapidly as the pressure is further raised. With still higher pressures the current again increases but the dimensions of the discharge continue to diminish. The shortened discharge takes on a variety of forms and each gives a different result; hence measurements are difficult to reproduce.

Visual observation of the sparks from a positive point revealed that they behave in a peculiar way. At low pressures they pass straight across the gap. This continues to the 100-pound point, which marks the start of the uncertain measurements. At this pressure the sparks do not always follow the shortest path but are spread out. The spreading becomes more pronounced as the pressure rises until the critical pressure is reached, then it becomes very slight again. Thus, spreading of the sparks seems always to be associated with uncertain measurements.

The photographs in figure 12 illustrate the type of results that can be obtained; the spreading was observed for all spacings. Each exposure shows a number of single sparks spaced about one-fourth or one-half minute. It will be observed that the sparks leave the point at many different angles and some travel a short distance in the direction away from the plane electrode. Some of the photographs suggest that the sparks never follow the shortest path when the spreading is manifest. The possibility could be investigated by making simultaneous observations from different angles, but this was impossible with the apparatus used.

The results for a dull negative point are shown in figure 13. The voltage curves are typical of all that were obtained for negative points. The current curves for other conditions, however, sometimes exhibited a minimum value. Sparks from a negative point always followed the shortest path and the steady discharge always existed as a glow of approximately constant size.

The current-voltage characteristic for both positive and negative points was studied as a function of pressure. It was found that for either polarity the shape was substantially independent of pressure. Figure 14 shows the results for a one-half-inch separation; six sets of data are plotted for the positive point (0, 15, 30, 50, 100, and 150 pounds pressure), and five sets of data are given for the negative point (0, 100, 200, 300, and 400 pounds). When reduced to a percentage basis the curves for different pressures are in surprising agreement. In making the computations the breakdown voltage was taken to be the 100 per cent voltage and the normal current flow corresponding to a voltage just below the breakdown value was taken to be the 100 per cent current.

The results that have been given for the positive point can be understood by considering the space charge left by the electron avalanche as it moves toward the point. At low pressures the density of the space charge in the avalanche path is too low to cause significant field distortion. Furthermore the ions are quickly moved out of the avalanche path by diffusion. As the pressure increases, the density of space charge in the avalanche path rises and diffusion decreases with the result that at higher pressures the field does become distorted by space charge and the ions remain in the path of the avalanche for relatively long periods of time. Under these conditions the space charge filaments constitute new points toward which other electron avalanches may be directed. The pressure at which the spark spreading starts is that for which the space-charge filament is dense enough to direct electron avalanches to itself. The fact that the space charge concentration remains for a relatively long time permits the effective electrode configuration to be built up in peculiar forms through a series of successive avalanches, and thus when the spark occurs it is led to the point over long and irregular paths. The measurements are uncertain because the electrode configura-

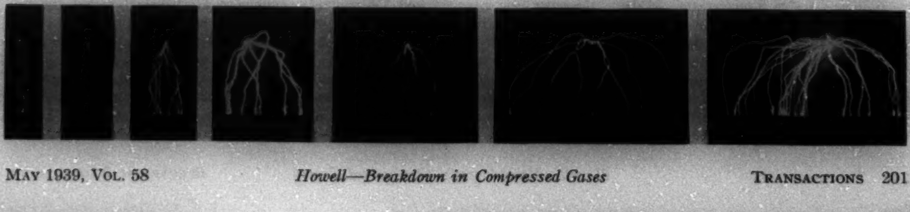

Figure 12. Spark spreading for positive-point-to-plane electrodes spaced one inch in air

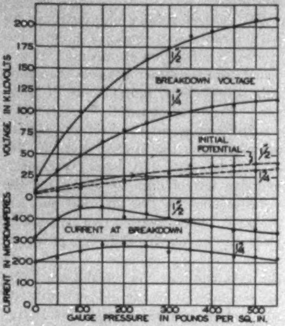

Figure 13. Voltage and current characteristics for negative-point-to-plane electrodes spaced one-fourth inch and one-half inch in air

tion is uncertain. That the pressure at which the uncertain phenomenon begins is lower for the larger electrode separation is understandable. The large number of ions in the interspace reduces the actual field in the vicinity of the point with the result that ion filaments of reduced density are able to direct electron avalanches toward themselves.

The above hypothesis readily accounts for the observed behavior of the sparks in the unstable region, but it does not explain how the chance mechanism is suddenly terminated by the appearance of a stable behavior. A possible explanation of this critical pressure is the occurrence of thermal ionization of the gas. At high pressures the large amount of heat energy developed by the avalanche is not effectively removed through diffusion and thermal conductivity. The temperature in the avalanche path rises with pressure until it becomes sufficient to dissociate and ionize the gas molecules and when this happens a new type of sparking mechanism presumably sets in. The point discharge causes, through progressive thermal ionization, the effective point to be projected toward the cathode until breakdown results. On this sort of basis it is reasonable that the sparking potential should coincide with the initial potential, and that scattering of the measurements and uncertainty of the spark path should cease.

Another possible interpretation of the positive point behavior is that the space charge near the point reduces the gradient at the apex, as previously suggested, until the actual gradient there is less than the gradient in the radial direction from the side of the needle. When this happens the sparks leave the needle radially in order to follow the strongest field. Since they would all tend to avoid taking the shortest path, this would explain the photographs that show no short paths. In this connection it would be helpful to establish whether or not any of the sparks follow the shortest path.

The existence of a critical pressure may also be explained on a space charge basis. Normally the geometric field of a point-to-plane arrangement is such that the point discharge is stable. But as the pressure is raised the amount of space charge in the interspace is increased and this makes the actual field more nearly uniform. Finally the tendency of the space charge to make the field uniform overcomes the stabilizing nature of the geometrical arrangement to such a degree that a stable discharge cannot be maintained.

It has been mentioned that a negative point does not exhibit the uncertain region or the critical pressure which has been discussed. This can be understood since the electron avalanche inherently spreads as it moves away from a negative point and therefore does not permit filaments with a high space-charge density to be realized. Also the field at the apex of the needle is increased by the space charge and not diminished.

In order to test the thermal ionization hypothesis the studies so far described for air were repeated for nitrogen, again for helium with three per cent nitrogen, and finally for a mixture containing equal partial pressures of the two gases. The ions involved in the breakdown are nitrogen ions in all these cases. Since the helium has more than six times the thermal conductivity of nitrogen, the helium-nitrogen mixtures carry the heat energy out of the avalanche paths more rapidly than nitrogen. The nitrogen ions also diffuse more readily through helium than through nitrogen. Because of these two effects, thermal ionization in helium-nitrogen mixtures should not begin at as low a pressure as in nitrogen alone.

The experiments showed definitely that the critical pressure is much higher for the helium mixtures than for nitrogen. The results for a one-fourth-inch spacing showed that the critical pressure was 190 pounds for pure nitrogen, approximately 500 pounds for helium mixed with three per cent nitrogen, and 350 pounds when equal partial pressures of the two gases were used. Similar results were obtained for other spacings. Either thermal conductivity or diffusion or both would cause the results for equal partial pressures of the two gases to fall midway between the others as they do.

The spreading of the sparks was smaller for nitrogen and for helium mixed with three per cent nitrogen than it was for air. But for equal partial pressures of the two gases the sparks spread as much or more than they did for air.

An experiment by Goldman and Wul[13] for pure nitrogen at 100 degrees centigrade showed that the critical pressure at this temperature was greater than it was at 13 degrees centigrade. About half of the rise they observed was due to the change in the density of the nitrogen with temperature. An increase was still manifest and this was probably largely caused by the increased diffusion at the higher temperatures. Their results were also in harmony with the thermal-ionization hypothesis.

One bit of interesting information that came out of this study of points is that the dissociated oxygen which resulted from the discharge caused a very rapid rusting of the electrodes and the tank walls. A brown dusty appearance could be detected after very few hours' operation. The deposit was collected for some time and analyzed spectrographically. It was found to be principally iron, nickel, and chromium, with traces of impurities likely to be found in the metal.

The results of this section indicate that additional difficulties would be encountered if electrostatic generators of the belt type were to be operated in compressed gases at pressures above the critical pressure. It appears that the operating requirement which specifies that the two parts of the belt be equally and oppositely charged cannot be met when positive charge cannot be sprayed on the belt. The results also reveal theoretical information which is of interest in the study of breakdown at high pressures. This is of importance since evidence is at hand

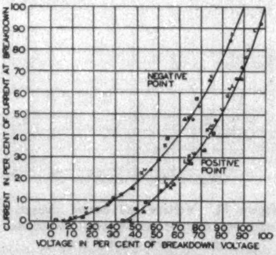

Figure 14. Current-voltage characteristics for point-to-plane electrodes in air at various pressures

to indicate that the point discharge is of particular significance in all high-pressure breakdown, especially when rough electrodes are employed.

Conclusions

Breakdown measurements made with plane electrodes were found to be greatly reduced by surface roughnesses, the percentage of reduction increasing with pressure. Both visual observations and current measurements lend strong support to the belief that the lowering is brought about by point discharges proceeding from the irregularities. Prolonged sparking was found to reduce the detrimental effect of roughening. Experiments demonstrated that this beneficial effect is definitely not brought about by changes produced in the air sample. Both the effect of electrode roughnesses and the conditioning phenomenon are of real practical importance. The significance of electrode roughnesses at high pressures may make measurements under laboratory conditions appear too promising.

The cylindrical electrode arrangement showed a definite and surprising polarity effect; the measurements were lower and more uncertain when the inside cylinder was positive. Further, it was not possible to realize complete conditioning for the larger cylinders. Applications of compressed gas using a cylindrical arrangement should naturally employ a negative polarity on the inner cylinder when possible. This may be accomplished in the case of a d-c cable, but not of course in the common condenser application where alternating potentials are used.

With the needle-point-to-plane electrode configuration the two polarities yielded entirely different results. The voltage and current for a positive point increased with pressure for the first few atmospheres, then became indeterminate, and ultimately a critical pressure was reached at which the voltage fell to the initial potential value and the current to zero. In the pressure region where the measurements were indefinite the path that the spark followed was uncertain. Above the critical pressure the spark path again became regular and the voltage was definite. The results for a negative point showed no irregularity.

The critical pressure for nitrogen and for air was substantially the same, approximately 200 pounds; for helium mixed with three per cent nitrogen it increased to about 500 pounds; for equal partial pressures of the two gases it amounted to 350 pounds.

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Discussion

G. C. Nonken (General Electric Company, Pittsfield, Mass.): Alvin H. Howell is to be commended on his review of the past work done on the dielectrics of high-pressure gases. Likewise, on his own work which has extended the combined voltage-pressure range to a considerably higher value than has previously been recorded.

It is of practical importance to compare the breakdown strength of air at high pressures with the breakdown strength of other typical high-voltage insulation. The important thing that shows up in such a comparison is that whereas the d-c breakdown of compressed air expressed in volts per mil is constant for different spacings as shown by Mr. Howell's figure 5, the dielectric strength in volts per mil of solid and liquid dielectrics drops off very rapidly with increased spacing. (The breakdown strength of solid insulation varies about as the two-thirds power of the spacing.) In making three typical comparisons we find that the d-c breakdown strength of five-mil thickness of good solid insulation is over three times higher than the same thickness of compressed air at 500 pounds pressure; the d-c breakdown strength of one-half-inch good solid insulation is slightly stronger than a half-inch of air at 500 pounds pressure; while 0.1 inch of transil oil is only about one-third the strength of air at 500 pounds pressure with d-c applied voltage. These comparisons would undoubtedly be much different on the basis of alternating or impulse voltages instead of direct voltage, or if the comparisons were made with other than parallel plate electrodes. It appears that if high-pressure gas is to be used as a dielectric, it will be in the ultrahigh-voltage field, where the insulation spacing is an inch or more.

In 1935, I made some tests on the corona current on a point-to-plane gap in air. I observed that the corona current, when the point was negative, was composed of a series of high-frequency current pulses which involved frequencies higher than 1,000 kilocycles. These current pulses built up very rapidly and then fell off exponentially to zero. The frequency of occurrence of these pulses was quite regular and increased with the applied voltage. With a positive point, a pulsating corona current occurred at the first initiation of corona, but at slightly higher voltage, the corona current changed to a steady state. These pulsating corona currents point to the important part which space charge has on the mechanism of breakdown. I would like to ask Mr. Howell if he observed this pulsating-corona-current phenomena in his tests, and if so, would the microammeter which he used record correctly the average value of these high-frequency currents.

I have observed the critical pressure in the breakdown versus pressure curve of sharp-edged gaps; similar to that shown in Mr. Howell's figure 11.

One test setup consisted of rod gaps tested with 60-cycle voltage in nitrogen. The critical pressure for that arrangement was 140 pounds per square inch gauge, and was not typified by such an abrupt discontinuity in the voltage breakdown curve as is shown in figure 11.

G. W. Dunlap (General Electric Company, Schenectady, N. Y.): Mr. Howell has done a very creditable piece of work and his paper presents valuable additions to the growing fund of information about the dielectric strength of gases at pressures above atmospheric.

Several questions arise and perhaps some related findings from the experience of the writer in a similar investigation may be pertinent.

The comments about the mechanism of breakdown are quite interesting and throw a new light on this phase of the subject. A better understanding of this mechanism is, of course, vital to the application of gases under pressure to the insulation problem. Regarding this point, the following questions might be answered.

Does not the same mechanism account for the departure from the Paschen relation with "uniform fields" as well as the critical pressure effect found with the point plane? It would seem to be a matter of degree.

Inasmuch as all the tests reported were made with direct current, it is possible that the full importance of space charge is not appreciated. This effect is more apparent when voltages with different time-amplitude characteristics are applied. For example, a six-centimeter rod gap in nitrogen at four atmospheres absolute was found to have a breakdown value of about 150 kv with 60-cycle voltage and a value of only 120 kv for a $1^1/_2$ x 40 positive impulse. This is, of course, directly opposite to the usual relation between impulse and 60-cycle breakdown.

In the study of dielectric breakdown it is well to be careful in drawing conclusions about the mechanism of breakdown on the basis of currents flowing before breakdown. Insulation studies in general have shown a decided lack of correlation between such current conditions and the actual breakdown.

Also can atoms remain in a metastable or excited state long enough to effect the strength of a given volume of gas for a time in the order of tens of seconds? In tests where positive impulse voltages were applied to sphere and rod gaps in gases under pressure it was found that the strength of a given gap depended to some extent on the preceding voltage application. Almost invariably the application of an impulse at some level within the range of voltages that could produce breakdown would cause a flashover if there had been a flashover on the previous application. Conversely, if there had been no flashover on the preceding application, the same voltage would be withstood and the voltage could be raised to the upper limit before a breakdown would occur. Impulses were applied at intervals of approximately one-half minute so this phenomenon is not satisfactorily explained by the usual conceptions of ionization by impact or electric or thermal stress.

Concerning the effect of roughening the electrodes, was any relation noted between the amount of effect and the spacing of the electrodes? It seems reasonable that the presence of small points could produce changes of the magnitude reported. Experience of the writer in corona studies seemed to indicate a measurable effect from irregularities of microscopic size on bare copper cables for high-voltage transmission lines.

Figure 11 of the paper shows a maximum breakdown voltage at 150 pounds though the conclusions give a value of 200 pounds. Was any relation noted between the critical pressure and electrode spacing? For standard one-half-inch square rod gaps in nitrogen the writer found breakdown voltage maxima at four atmospheres absolute for both positive impulse and 60-cycle voltages. There was some indication that this critical pressure decreased with increasing gap lengths.

The attack on metal parts mentioned as due to dissociated oxygen should be remembered if application is to be made where frequent breakdowns could occur. Similar trouble has been experienced where gases of complex molecular structure have been used. The use of an inert gas such as nitrogen obviates this difficulty.

In the engineering application of gases under pressure it is important to remember the wide range of dielectric strengths obtained by varying such factors as the field configuration and the polarity of applied voltage. This is emphasized by the results given by Mr. Howell. It is impossible to design gas-insulated apparatus on the basis of a pressure-breakdown curve for one gap.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): Mr. Howell has presented a fine piece of work on a very important line of research, and is to be commended on the thoroughness with which the investigation was made. Many of the features of high-pressure gaseous insulation have been determined and the limits within which developments may be expected for an application of gas under pressure for insulation are shown to be elastic but high as compared with practice up to the present. Mr. Howell's discussion of the effects noted with regard to conditioning of electrodes is particularly appropriate. It is obvious that for any commercial development of long lengths of cable, little or no conditioning effect will be realized and that the real practical limit is established by results for rough electrodes, as shown in figure 7 of his paper.

An investigation of pressure relations with gas for a dielectric at Worcester Polytechnic Institute has shown similar results to Mr. Howell's, in that for unconditioned electrodes, there is a distinct disagreement between points on the breakdown curve taken with increasing pressure and those taken with pressure decreasing. The trends are quite definite, and it seems to make little difference as to the order in which the tests are made. The specimen used is a concentric cylinder with a ratio of outer to inner diameter of about four, flashed over with alternating potentials, and of a length so that previous sparking of the gap produces little conditioning, since as soon as one spot becomes conditioned, another point takes the arc under approximately the same conditions as the first. This gives rise to the belief that the effect is more than an electrode effect entirely, yet it is difficult to understand how the gas can be affected directly by the direction of pressure change. The factor of time seems to play no part, as the gas can be left at any pressure for a length of time on either curve and still show a wide separation between rising and falling pressure values. The investigation of this phenomenon would help in arriving at the practical limits for gas-pressure insulation.

I would like to suggest another point of significance from the practical side of the discussion. The ratio of cylinder diameters at very near to ϵ, as used by Mr. Howell, gives results which will seldom be achieved except under laboratory conditions. The interesting values of this ratio are those slightly above ϵ, since the corona-free breakdown given by the curves in this paper is a very special type of spark-over. Further work of the same caliber as Mr. Howell's should be undertaken with a wide range of cylinder diameter ratios. Results from such a set of tests may help to clear up some of the baffling phenomena observed and possibly improve the test procedure to a point where observations will not be affected by sudden bursts of violent sparking, since some corona will be present to initiate slow discharge before complete breakdown ensues.

Mr. Howell has attempted to explain many of his results from fundamental concepts, but has had to throw out many possible explanations because they do not fit one observation, namely, the lack of difference in breakdown voltage for a reversal of polarity. I do not see how the polarity can be definitely biased for plane electrodes, and for cylinders the results are already counter to polarity theory; so that my own feeling is that there may be more to Mr. Howell's explanations than he has allowed on the basis of the one key observation. I hesitate to suggest that his work is in error, for I can certainly do no better than he has done, but it may be that the sensitivity of the measurements does not permit easy detection of a polarity effect which may be present.

R. C. Buehl (Massachusetts Institute of Technology, Cambridge): The excellent paper of Mr. Howell naturally raises the question of the extension of the results to higher pressures and alternating voltages. In this connection, two questions are of importance, namely:

1. Does the breakdown approach a definite maximum with increase of pressure, or continue to rise at a slower rate than at low pressures?
2. Will electrodes condition themselves or deteriorate when breakdown measurements are made with alternating voltages from a transformer so that each breakdown is followed by a short-time arc?

The answer to these questions for plane electrodes is contained in measurements on nitrogen with 60-cycle alternating current made by the author in 1935 with the apparatus sketched in figure 1 of this discussion but not previously published. The nitrogen was partly purified by passing over magnesium heated to 400 degrees centigrade and through a trap cooled by solid CO_2. Errors due to time lag were eliminated by irradiating the gaps with ultraviolet radiation from an iron arc but this had no appreciable effect on the breakdown voltages except to make them more consistent. A resistance of 1.8 megohms was used as a series resistance to protect the electrodes from excessive pitting. After a breakdown, the voltage was removed in a fraction of a second by a relay.

The breakdown voltages are plotted against pressure in figure 2. Below 15 atmospheres there was little spread in the breakdown measurements, all the values falling within two per cent of the average, so that the electrodes seem to have been satisfactorily conditioned. This conditioning required about 50 breakdowns. Above this pressure, the spread on the breakdown voltages became much greater, becoming as great as 15 per cent in some cases, the spread being shown by the shaded areas in figure 2. In general, each successive breakdown voltage would tend to be higher than the previous one until a maximum was reached but occasionally an arc would appear to damage the surface so that the following breakdown measurements would be at the bottom of the spread. Consequently, with the apparatus employed, the condition of the electrodes appeared to be continually changing in the range from 15 to 45 atmospheres. An interesting phenomenon was observed, namely, that sparks which lasted only a fraction of a half cycle often occurred at the lower breakdown voltages. These conditioned the electrodes so that the following breakdown voltage was always considerably higher.

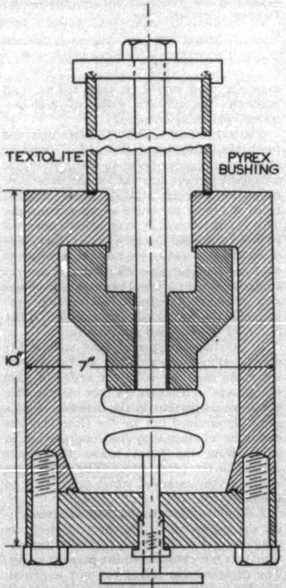

Figure 1. Apparatus employed for breakdown measurements on gas at high pressures

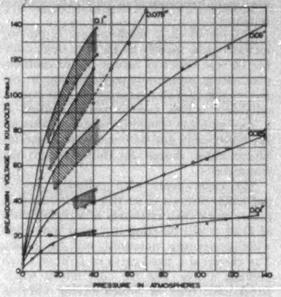

Figure 2. Breakdown voltages for nitrogen with plane electrodes at various constant separations

Above 45 atmospheres the spread in the breakdown voltages again became small except that the sparks of short duration mentioned above occurred occasionally at voltages below those necessary to cause a repeated breakdown during each half cycle. These low breakdown voltages are not shown in figure 2. Since the extrapolations of the curves from high pressures coincide with the minimum breakdown values observed between 15 and 45 atmospheres, it appears that the irregularities on the electrodes—either points or active regions of low work function—become more important at high pressures, and their effects cannot be removed as readily by breakdown above 45 atmospheres as below. Above 45 atmospheres the breakdown voltage increases about linearly with the pressure but the rate of increase of breakdown voltage with pressure is only about one-fifth that at atmospheric pressure. This corresponds roughly to the ratio of the slope of the curves for rough electrodes at high pressures to the slope for conditioned electrodes at atmospheric pressure given by Mr. Howell in his figure 7. From this it would seem that surface irregularities, which act as points of local discharge or sources of electrons, play an important role in determining the breakdown voltages at high pressures. Although the mechanism of breakdown at high pressures is probably different from that at atmospheric pressure, the surface irregularities probably control the breakdown voltage and largely mask the effect on the breakdown voltage of a change in the mechanism of breakdown.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): The author has carried out a most careful investigation of this subject and has gathered together a very good background of past experimental work.

The art of cable manufacture is moving forward quite rapidly and a very large number of new types of cables use or contemplate using pressure-type insulation to obtain increased dielectric strength and greater reliability. This present paper dealing as it does with gas should be of some value for gas-filled cable and of considerable use for future cable design for high-voltage d-c transmission.

The experiments with cylinders are of particular interest to us and it is hoped that future work with cylinders of varying ratio of D to d will be tried and also that tests with alternating potential will be made. The choice of a fixed ratio of $D/d = \epsilon$ was perhaps a wise one for this initial work. Breakdown tests of cables having varying ratios of D/d do not follow the theoretical formula for large ratios of D/d (over value of ϵ) and as yet no good explanation has been offered.

Minimum stress, maximum stress, current density (pyroelectric effects), and corona-type formula have been offered without much success. In connection with this problem we are interested in the erratic behavior of concentric cylinders having the inside one positive and also with the pronounced polarity effect found which is apparently new. If this polarity effect is pronounced it is conceived that for alternating voltage there may be some rectifying action taking place which would allow the building up of space charges and explain the reason why corona-type formula apply fairly well to cables insulated with solid-type dielectrics.

The author is to be congratulated on avoiding many of the pitfalls in dielectric strength tests which sometimes lead to erroneous conclusions.

A. H. Howell: The gentlemen who have offered discussions have presented valuable material and the writer wishes to express appreciation for these contributions.

Mr. Buehl's results with alternating voltages appear to be in agreement with those for direct potentials reported in the present paper. It is especially helpful to have available these quantitative results from his work.

The comments of Mr. Davis are especially significant because of his close association with the commercial aspects of insulation problems.

Mr. Nonken has made interesting comparisons of the dielectric strengths of typical solids and liquids with those presented for compressed gases. Regarding the question he raises concerning the possible pulsating nature of the currents that were observed, this was not checked and there were no observations indicating that the current possessed this pulsating quality. The microammeter used would be expected to read correctly the average value of high-frequency pulsating currents.

The suggestion of Mr. Dunlap that the mechanism underlying the critical pressure effect for a positive point-to-plane electrode arrangement may account for the departure from the Paschen relation with uniform fields at higher pressures seems reasonable. This is not necessarily true since numerous physical processes are at work in the breakdown of a gas, and as yet there is insufficient evidence to determine whether the one responsible for the critical pressure phenomenon is wholly responsible, partially responsible, or not at all responsible for the failure of the Paschen relationship.

It has been suggested that metastable atoms may not play a part in the breakdown when the time lag is of the order of tens of seconds. Whether or not metastable atoms are important in the breakdown of a gas is determined not by the time lag, but rather by whether they live long enough to travel from the point where they were created to the point where they regenerate electrons (the cathode) which contribute to the discharge.

A definite answer to the question concerning the magnitude of the effect of electrode roughening for different electrode separations was not obtained in the present study. The question was raised but not explored. The few results obtained did not show much of an effect with separation.

The effect of electrode spacing on the critical pressure has been raised. The critical pressure for a given positive point was practically independent of spacing for the spacings used (up to two inches). It was observed that the particular point used affected the critical pressure, the pressure being lower for a dull point than for a sharp one. However, the pressure at which spark spreading and uncertain measurements began was lower for the larger spacings.

The remarks of Mr. Dunlap concerning the effect of the gaseous products of breakdown on the apparatus and of the importance of such factors as field configuration and polarity in design are most significant.

Mr. Siegfried raises the question of a polarity effect with plane electrodes. The polarity effect was expected only for the arrangement of one conditioned electrode and one rough electrode. On the assumption that breakdown is initiated at the cathode (and is not influenced by the condition of the anode) as it is with some of the suggested mechanisms, such a gap ought to exhibit breakdown voltages corresponding to rough electrodes when the rough electrode is the cathode and breakdown voltages corresponding to conditioned electrodes when the conditioned electrode is the cathode. This difference in voltage would easily be detected and was the effect expected.

The results of Mr. Siegfried's own work are very interesting and it is helpful to have the comments from his practical point of view.

Bus Protection

AIEE-EEI COMMITTEE REPORT*

THE RECENT occurrence of several serious switch-house fires has occasioned an unusual interest in the matter of bus protection. Acting upon the belief that a comprehensive survey of modern practices in such protection would be of some benefit to those interested, the relay subcommittee of the AIEE protective devices committee and the relay subject committee of the Edison Electric Institute electrical equipment committee jointly prepared and circulated a rather extensive questionnaire on the subject.

It is the purpose of this report to present factual data on the principal types of bus protective schemes as reported by the 34 companies replying to the questionnaire, and also to present a discussion of the design features involved as indicated by the replies to specific questions asked and collateral data supplied.

Classification of Bus Protective Schemes

In order to obtain as much uniformity as possible in the scope of the replies solicited, the three principal types of bus protection were defined as follows in the questionnaire:

A. Differential Protection. The bus protective relay is operated from a differential connection of current transformers located in all sources to and feeds from the bus and therefore distinguishes between bus and external faults by virtue of its connection.

This system offers protection against phase faults, ground faults, or both, dependent upon the use of phase relays, the use of a neutral relay only, or of the latter in combination with phase relays.

Paper number 39-19, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 3, 1938; made available for preprinting December 10, 1938.

* A report prepared under the auspices of the relay subcommittee of the AIEE protective devices committee and the relay subject committee of the Edison Ele[illegible] Institute electrical equipment committee by J. H. Neher, chairman of the AIEE relay subcommittee and engineer in charge of relay design for the Philadelphia Electric Company, Philadelphia, Pa., and H. L. Wallau, chairman of the EEI electrical equipment committee and electrical engineer of the Cleveland Electric Illuminating Company, Cleveland, Ohio.

B. Fault Bus Protection. The normally grounded parts in the station are interconnected through a "fault bus" which is connected to the station ground through one or more current transformers in such a manner that currents due to ground faults occurring on the bus or associated equipment must pass through this fault bus and operate a bus protective relay connected to the above mentioned current transformers. As indicated, this system offers protection only against faults involving ground.

C. Partial Differential Protection. The bus protective relay is connected to current transformers located in one or more of the sources to the bus. Operation on external faults is prevented by:

1. Utilizing an overcurrent relay with a time setting selective with the rest of the system protection under the prevailing conditions.
2. Utilizing a distance relay with a definite ohmic pickup set above the value of reactors located in the sources to or feeds from the bus; or a distance relay with a time-distance characteristic set selectively with the rest of the system protection.

This system offers protection against phase faults, ground faults, or both, according to type, and connections of the relays employed.

Numerical Data

The numerical data obtained from two general questions covering the main constructural and operational features of the installations are given in table I. Because of wide variations in the number of installations reported by individual companies, there have been incorporated both the number of reporting companies and of installations of each class reported. In general, no effort has been made to minimize the discrepancies appearing in the tabulation because of incomplete detail in the replies.

Of the total installations, three companies reported 61 per cent and six companies reported 81 per cent of the type *A* installations; three companies reported 62 per cent of the type *B* installations, while the type *C* installations were much more uniformly distributed among the reporting companies.

On the basis that all similar applications were installed at the earliest date listed by the reporting company and applied to the total bus sections so reported, it appears that type *A* installations have been in use for 11 years, type *B* for 10 years, and type *C* for 7 years to the beginning of 1938. It will be noted that this has a tendency to exaggerate their actual age.

Group phase bus construction is used in 93 per cent of the sections equipped with type *A* and in 87 per cent of those equipped with type *C* protection. The type *B* fault bus systems are primarily favored for use with indoor installations, some 82 per cent being so reported. The margin of the isolated phase installations over the group phase for type *B* is not as great as might be expected.

The voltage ranges were selected to emphasize the grouping points of the numbers of reported installations.

In respect to the type of faults protected against, the answers were tabulated as specified on the individual returns, except that type *B* installations were listed under "ground faults" only. In some cases they were specified in the "both" category and undoubtedly these installations will function properly for phase-to-phase faults involving ground. This is probably true of all the installations listed under "ground faults." Both *A* and *C* types of protection may have installations listed in the "both" category with the same thought in mind.

As regards the matter of system grounding, practically all cases involving an isolated neutral apply to distribution substations. The majority of the fault bus installations are used in connection with limited ground currents since most are installed in generating stations where this is a standard practice.

The balance current relays employed are principally of the percentage-differential type. Distance relays are of the impedance type.

One company lists 172 type *A* installations with instantaneous overcurrent relays. Sixty-six percentage-differential relays listed under "balance current" in type *A* installations are in use by another company.

It is apparent that the type of current transformer used depends primarily on the type of bus construction. The bushing type is heavily favored with the differential class of protection since most of the protected buses are of the outdoor type. In the case of the fault bus type of protection, most installations are indoors, which makes the wound type of transformer convenient.

The "combination" group contains examples of all possible groupings of the three principal classifications.

Four companies reporting about 300 installations practically all of the differential type, use the term "excellent" to indicate performance in lieu of numerical data.

Causes of Incorrect Operations

This information is sufficiently important to justify the inclusion of more

Table I. Summary of the Constructional and Operational Features of the Bus Protective Installations Reported

Classification	Total Installations		Type of Station			Type of Bus Construction				Voltage of Protected Bus (Range in Kilovolts)				Protection Against		
						Group Phase		Isolated Phase								
	Existing	Proposed	Generating	Transmission	Distribution	Indoor	Outdoor	Indoor	Outdoor	2–9	11–22	33–88	110–220	Phase Faults	Ground Faults	Both
A. Differential	26/ 803	9/38	20/172	21/379	14/288	16/245	24/533	5/ 49	1/10	6/100	22/338	14/252	14/153	9/266	8/ 47	22/484
B. Fault bus	11/ 197	2/ 5	9/132	7/ 42	4/ 28	5/ 75	5/ 15	7/ 90	1/22	1/ 6	9/165	4/ 24	2/ 5		12/198	
C. Partial differential	9/ 132	4/37	7/ 62	6/ 53	3/ 54	7/ 77	8/ 70	3/ 21	1/ 1	2/ 32	8/104	1/ 4	4/ 29	4/ 53	2/ 24	8/ 90
Totals	32/1132	15/80	28/366	27/474	15/370	22/397	27/618	14/160	2/33	7/138	28/607	16/280	17/187	10/319	18/269	25/574

Classification	Type of Grounding System				Type of Relay Protection					Type of Current Transformer				Number of Operations	
					Overcurrent										
	Solid	Resistor	Reactor	Isolated Neutral	Induction	Instantaneous	Both	Balanced Current	Distance	Bushing	Through	Wound	Combination	Total	Incorrect
A. Differential	20/415	15/333		5/87	11/258	8/386	3/ 41	16/150	1/ 4	21/613	10/121	10/ 52	6/30	13/155	10/32
B. Fault bus	6/ 62	5/121	1/ 8		4/ 14	7/ 91	3/ 94			2/ 39	2/ 51	7/ 97	1/ 3	10/ 55	5/15
C. Partial differential	7/ 92	6/ 39	2/ 4	2/10	4/ 53	2/ 25	1/ 4	3/ 47	2/19	5/ 69	4/ 37	4/ 37		3/ 4	1/ 1
Totals	22/569	16/493	3/12	6/97	16/325	14/502	7/139	16/197	3/23	21/721	13/209	19/186	7/33	22/214	16/48

Note: The figures to the left of the oblique bars designate the number of reporting companies while those to the right of the oblique bars designate the number of bus sections reported.

detail from the individual returns.

Number of Incorrect Operations	Causes
8	Incorrect wiring.
4	Errors in routine testing, such as applying potential to the ground bus.
1	Differential relay failure.
5	Improper application, principally failure to consider current transformer ratio changes under high fault currents.
3	Construction errors. In one case this was a matter of poor contact and in another construction men inadvertently cut into an operating bus protective setup and the third was caused by vibration from construction on an adjacent panel.
2	Failure of bushing current transformers.
2	Balance circuit grounded.
1	Auxiliary relay failure.
8	Faulty insulation of fault busses. Other cases of this are mentioned.
8	Minor miscellaneous causes.
"Several"	Unbalance created by d-c transients at the time of a fault.

Maintenance

Maintenance of bus protective systems is quite evenly divided between the "negligible" and "reasonable" categories. No reporting companies consider the maintenance of their present bus protective systems as "heavy."

The usual maintenance periods reported by the various companies are as follows:

3 months—reported by 3 companies with 71 installations

6 months—reported by 11 companies with 480 installations

12 months—reported by 15 companies with 434 installations

24 months—reported by 2 companies with 157 installations

Included in the above tabulations are some companies with a relatively large number of installations which divide them into two categories in order of importance and provide maintenance accordingly.

A 6- and 12-month interval is so used by three companies with 227 installations. Two companies with 277 installations use 12- and 24-month maintenance periods.

Four companies reporting 287 installations provide some or all of these installations with instruments on which the condition of the balance is read three times daily.

Several companies provide tripping tests and/or inspections at intervals between the normal maintenance periods given above.

Design Features

A number of specific questions were asked in reference to the three main protective systems, intended to promote a discussion of certain features fundamental in their designs, and the following discussion has been prepared from the replies. Outlines are also included of special bus protective schemes which are believed to be of general interest.

DIFFERENTIAL BUS PROTECTION

(*a*) *Current Transformer Considerations.* The highest ratio of fault current to current-transformer rating varies considerably and attains the remarkably high value of 126 to 1. The conservative limit of 10 to 1 or less is used by five companies reporting 316 installations. The most commonly used limits are within the range of 11 to 20 to 1. Twelve companies report 300 installations in this group. The 21 to 30 to 1 class has a substantial representation consisting of 138 installations reported by seven companies.

Higher limits are used by three companies reporting 66 installations which are probably permissible because of special conditions. A conservative trend is clearly indicated by a consideration of proposed installations. These show that the limiting ratios will be lowered by five companies and raised by one and will exceed 20 to 1 in one case only.

Eight companies reporting 264 installations include an accuracy limitation in current-transformer specifications. Classification of these is difficult since most of the specifications are not complete and lack agreement. The limits range from 0.5 per cent to ten per cent at ten times rating.

Nineteen companies reporting 546 installations use standard transformers with commercial accuracies. However, a growing consciousness of the accuracy factor is apparent as three of the latter companies now include some specification of accuracy in proposed installations.

Opinion favors the independent grounding of current transformers. Eighteen companies reporting 307 installations follow this practice. Seven companies reporting 394 installations use a common ground, generally located at the relay panel and providing a convenient point to megger the relaying circuits.

One company reporting 179 installations uses a single ground for all installations except those on 4.8-kv busses which use independently grounded current transformers. Of particular interest is the fact that these ground connections, in recent practice, are made up of a 300-ohm resistor with a parallel gap. This reduces the hazard of a false operation if the circuit is accidentally grounded and at the same time is believed to afford adequate safety to personnel and equipment.

With a single exception no inclination to change existing practice is indicated in the proposed installations.

A relatively even division of opinion exists on the permissibility of utilizing current transformers of different ratios equated by autotransformers with 14 companies reporting 288 installations subscribing to this practice and 12 companies reporting 523 installations avoiding it. Of the latter, two companies use balancing transformers if a transformer bank is included in the protection. Balancing transformers are all of the autotransformer type with the exception of those used by three companies who prefer the tapped two-winding type.

It is evident that balancing transformers should preferably be avoided whenever possible, but sufficient evidence exists to indicate that it is perfectly feasible to use them if the situation requires.

If protection against ground faults only is provided existing current transformers are generally used according to reports from nine companies with 175 installations. Separate current transformers are used by three companies reporting 11 installations. Both types are used by one company reporting 9 installations.

The preference is decidedly for the common neutral return where existing current transformers are used. This is the practice of six companies reporting 155 installations as compared to three companies reporting 20 installations, which make use of insulating current transformers in the individual neutral circuits. No nonmagnetic conduit is used in any installation.

Proposed installations of this type favor the use of existing current transformers and are quite evenly divided between insulating current transformers and the use of the common neutral return.

A very general freedom from open-circuit current-transformer troubles is indicated by the fact that 19 companies reporting 464 installations find it unnecessary to make any provision to check for this condition, except by reading the various currents at the time of the periodic relay check.

Three companies reporting 124 installations make use of fault detectors in series with the differential relay. This permits of an alarm indication in case the differential relay alone should operate due to an open current transformer.

Three companies reporting 197 installations use one-ampere ammeters in the neutral circuits which are read three times daily. A similar check is made by one company using the harmonic-restraint type of relay by means of a low-reading high-resistance a-c voltmeter connected across the internal current transformer of the relay.

Twenty companies reporting 640 installations test for a short-circuited condition in the current transformers at the time of the relay check, usually annually. The procedure is divided between the companies which read load current values in the various branches of the differential circuit and the companies which apply an external voltage to determine the impedance of the circuit.

One company reporting 110 installations applies to the relay daily a potential which is too low to cause it to trip. The value of the resulting current will determine an open- or short-circuited condition. Another company reporting 11 installations makes a monthly tripping test with a small transformer just adequate to trip the relay under normal conditions.

Five companies reporting 51 installations make no check for short circuits following initial installation.

(*b*) *Differential Relays.* Phase differential relays are usually set above maximum load current of any circuit, according to 13 companies reporting 531 installations, and settings range as high as three times this value. Seven companies reporting 63 installations set phase relays below maximum load values.

In the case of neutral relays, the situation is reversed with 2 companies reporting 17 installations with settings above the maximum load and 11 companies reporting 413 installations using settings below this value.

Considering both neutral and phase relays which are set below maximum load values, 12 companies reporting 343 installations find it unnecessary to make any provision to prevent tripping due to accidental opening of a current transformer. On the other hand, six companies reporting 98 installations use fault detectors in series with differential relays to prevent the occurrence of faulty trippings on this account, and one company uses percentage differential relays for the same purpose.

Both schools of thought find results satisfactory since the majority of the proposed installations involve no change in practice.

Percentage differential relays are employed in 107 installations reported by 13 companies. Considerable diversity of opinion exists as to the most desirable grouping of current transformers.

Four companies reporting 77 installations group sources to one winding of the relay and loads to the other. This number includes the largest user of percentage differential relays for bus protection, a company with 66 installations. Similarly two companies with 7 installations use three-winding differential relays and group sources to one winding, loads to another, and bus ties to the third.

Two companies with 11 installations connect the principal source or sources to one winding and other sources and loads to the second. One company with 4 installations groups both sources and loads to each winding. Three companies with 5 installations include transformer banks in the differential protection and naturally group current transformers according to potential of the bank windings.

(*c*) *Prevention of Relay Operation on Transients.* Eleven companies reporting 483 installations make no intentional provision to prevent operation of the differential relay on through faults due to transients set up in the differential circuits by iron saturation in the current transformers. It is of interest to note that all of this group with the exception of two companies reporting 238 installations make use of induction relays which inherently introduce some time delay.

The largest user of instantaneous relays for bus protection, reporting 182 installations, keeps the ratio of fault current to current transformer rating within the conservative limits of ten to one. This should be of considerable assistance in providing for an iron-saturating transient. The remaining 16 reporting companies make provision for this factor in a number of ways.

Ten companies provide time delay in one form or another. Three companies use high relay settings while three companies provide high capacity or special transformers. Transient shunts are in use by one company and the harmonic restraint type of relay is installed for this purpose by another.

(*d*) *Effect of Abnormal Setups on Protective System.* As to means provided to maintain the effectiveness of bus protection when the "normal" circuit setup is temporarily changed, practice is evenly divided on the need for such protection. Fifteen companies report 362 installations with no provision for this eventuality. Comments from this class indicate that many remove entire groups of equipment from service for maintenance and others include current transformers from all possible connections. Other companies operate with inadequate protection if the weather is favorable.

Eight companies reporting 187 installations follow the practice of opening the trip circuit of the differential relay manually in case of an abnormal setup.

A small group of three companies reporting the substantial number of 249 installations use auxiliary switches on disconnecting switches which operate to open the trip circuit of the differential relay in case the normal switching setup is disturbed. In some cases one company switches current circuits to rebalance the protective system for the abnormal setup.

An interesting treatment of this problem is provided at one high-capacity generating station. Ten generator and step-up bank units feed into the 110-kv bus. Considerable flexibility is required for various combinations of units and outgoing lines. Each section of the double bus system is split into independent sections of variable lengths. To provide protection for these varying numbers of busses, the currents of all units and outgoing lines are combined through a differential overcurrent relay. The operation of this relay applies potential to a tripping bus. The individual switches on the incoming and outgoing feeds are then tripped by the individual phase and ground fault detectors with the potential from the tripping bus, thus making the protection independent of any possible grouping of circuits. An operation of the differential relay alone provides an alarm.

FAULT BUS PROTECTION

Most of the existing installations were built integral with the station. Eight companies so report 75 installations. Four companies report 35 installations added later. Three companies with a large group of 70 installations specify both categories without providing a detailed breakdown of the installations.

The preponderance of fault bus installations with special insulation is noteworthy. Four companies report 98 indoor installations and two companies report 26 outdoor installations with special insulation. Two companies report 8 metal-clad installations which naturally fall in this group. Natural concrete insulation is used by three companies reporting 45 indoor installations and two companies reporting 5 outdoor installations.

The importance of adequate insulation for satisfactory fault bus operation is obvious. In one case a group of 4 installations was discontinued and another group of 12 installations reinsulated after a series of faulty operations due to inadequate insulation.

Practically all installations are tested initially with various high voltage and current values. Seven companies report 165 installations which are checked for insulation in periods varying from four months to two years. Where the insulation is high a high-voltage test is made and where it is relatively low a check of the current through the relay is made and compared with the total supplied at the test point. Two companies reporting 20 installations make no regular insulation tests.

In all cases the setting of the relays is based on the minimum anticipated fault current, together with a desirable factor of safety. In some installations using natural concrete insulation it has been found necessary to determine the percentage of fault current which appears at the relay for the various faults.

For similar reasons it has been found necessary at times to determine the maximum stray currents flowing through a relay for faults in adjacent sections in order to provide a setting in excess of this value.

Although two cases of faulty operation have been experienced due to accidental contact of low-voltage circuits, in general, this eventuality is not considered to be a serious hazard and no provision is made for preventing relay operation under this condition.

As a whole, experience with fault bus installations is considered satisfactory and further applications are in order for the reporting companies.

This approval is qualified for one group of 4 installations which were decommissioned because of inadequate natural concrete insulation. Another group of 12 installations was reinsulated after a series of faulty operations and are now scheduled to be changed over to a combined differential and fault bus scheme. It is thought that future installations would be of this order.

A number of companies experienced insulation difficulties with initial installations of the fault bus system, but felt that the condition was now satisfactory and that additional installations are in order.

PARTIAL DIFFERENTIAL PROTECTION

Where distance relays are utilized, these are of the impedance type, employing wye-connected currents and phase-to-phase voltages for protection against phase faults or phase-to-neutral voltages for protection against ground faults.

Fault detectors are used in practically all installations. These consist of instantaneous overcurrent phase or ground relays, residual-voltage relays, and polyphase directional or directional ground relays polarized by station neutral current.

No especial difficulty is apparently experienced in providing an adequate safety factor in the settings of the impedance relays if arcs of reasonable lengths are considered.

SPECIAL BUS PROTECTIVE SCHEMES

Two companies propose to install bus protection by a scheme based upon the "directional comparison" method of pilot protection of lines. The scheme provides a directional ground relay on every bus tie and feeder and an overcurrent relay in the neutral of the generator. The tripping is initiated by the operation of the neutral overcurrent relay and the trip circuit is routed in series through the front contacts of all directional relays on incoming circuits and the back contacts of all directional relays on outgoing circuits.

If the outgoing circuits have no source of ground current backfeed, overcurrent relays may be substituted for directional relays.

One company reports seven installations in which both incoming and outgoing circuits from the bus are provided with impedance relays. The incoming impedance relays are provided with a slight time delay and can be kept from functioning to trip all incoming circuits by the operation of the instantaneous impedance relays on the outgoing circuits.

GENERAL CONSIDERATIONS

(*a*) *Hazards of Inadvertent Relay Operation.* Twenty-nine reporting companies believe that the advantages to be obtained from bus protection are adequate to justify the hazard of an incorrect operation. Two companies feel that no possibility of load loss should be incurred.

A large majority of existing installations are arranged to trip on the operation of any differential relay alone. This group includes 850 installations reported by 23 companies. Ten companies report 65 proposed installations in this classification.

Ten companies reporting 288 existing installations require or favor the use of some checking device such as a fault detector which must operate in addition to the differential relay before tripping can be accomplished. Twenty-three proposed installations reported by four companies will likely be in accordance with this policy.

One installation on an important 230-kv application makes use of two completely separate sets of differential protective equipment, including the current transformers. Both sets of relays must function to clear the bus. Either set operating alone will provide an alarm only.

The requirement of this safeguard appears universal with the partial differential type of installations.

A preponderance of existing and proposed installations favor the use of a single multicontact relay for tripping as opposed to the use of a separate auxiliary relay for each switch tripped. Twenty companies report 757 installations following this practice while 11 companies reporting 437 installations use a number of auxiliary trip relays. Among some of these latter companies it is evident that the consideration involved was not a matter of principle, but the fact that a preferred type of auxiliary relay did not have a sufficient number of contacts in a single unit.

(*b*) *Supervision of Trip Circuit.* About one-half of the companies reporting on about two-thirds of the installations use supervision of the tripping circuit through the differential relay and the remainder do not. It is probable that many of the latter did not consider the supervision of the trip circuit of the oil switch alone as belonging to this classification and it is certain that a considerable number of the former group did include these cases.

Several companies use complete supervision of the differential trip circuit if it is routed through a number of relays or through interlocks on disconnecting switches.

(*c*) *Equipment Tripped and Locked Out.* The common practice is to trip all switches on the bus, as 25 companies so report 895 installations. Six companies reporting 107 installations trip all feeds to the bus only. Two companies reporting 22 installations trip only the major feeds to a bus.

One company reporting 13 cases of partial differential protection for ground faults trips only the sources of ground current feed to the bus.

The last three companies referred to also report 220 installations which were included in the first group with all switches to be tripped.

Breaker closing circuits are not generally locked out by the operation of differential relays. Twenty-four companies with 796 installations report against this practice. However, out of this group, eight companies with 407 installations use hand reset auxiliary relays and since relays of this type are commonly used in oil-switch control practice, it is probable that many other similar cases were not considered within the scope of the question.

Six companies reporting 107 installations follow the practice of locking out the breaker closing circuit with a differential relay operation. Three companies allow one or two reclosures at automatic substations so equipped prior to lockout.

(*d*) *Hazards of Interconnected Current Transformers.* The necessary interconnection of current transformers usually involved in any bus protective scheme is not considered a hazard by 20 companies reporting 839 installations.

Nine companies reporting 341 installations believe such a hazard exists, but that it may be mitigated with insulating current transformers in the opinion of five companies with 139 installations and by separate current transformers according to two companies reporting 186 installations.

The danger referred to consists of an accidental contact of the balance circuit with high voltage and the possibility of false tripping incurred in the testing of the interconnected equipment. Several companies believe the hazards will exist in any case.

Twenty companies reporting 883 installations use independent current transformers for bus protection. Twelve companies report 133 installations where current transformers are used in common with other equipment. Three companies report 196 installations in both categories.

(*e*) *Backup Protection.* Backup bus protection is universally favored where full differential protection is not warranted. Twenty-eight companies reporting 1,011 installations are in favor of this practice while three companies with 52 installations do not follow it.

Opinion is evenly divided on the desirability of using backup protection in addition to full differential or fault bus protection. Sixteen companies reporting 500 installations follow this practice while 12 companies reporting 420 installations do not. Five companies report 285 installations in both categories.

Conclusions

From the factual data and discussion presented, the following conclusions may be drawn.

1. There is conclusive evidence that operating companies have generally accepted the idea of providing some type of protection for busses in installations of major importance.
2. Adequate bus protection can be obtained by any of the three major methods described.
3. Maintenance of bus differential relay systems is in general no different from that of other protective relay systems.
4. While complete current differential protection is predominantly the favored method, a number of apparently successful protective schemes have been developed which are applicable to old installations, and which avoid the excessive expenditures which would otherwise be required for complete current differential protection.
5. The considerable number of installations of fault bus protection reported, particularly as applied to existing installations, contradicts the general belief that this method of protection is limited to special cases specifically designed for the purpose.
6. While considerable complexity has been introduced into the protective scheme in certain cases in order to prevent inadvertent tripping of the bus, there is considerable opinion to the effect that such refinements are unnecessary.
7. While opinion favors the use of separate current transformers for bus differential protection, it is apparent that current transformers in common with other equipment can be successfully used.
8. When instantaneous or high-speed differential relays are employed, special care should be taken to prevent the operation of the relays due to transients.

Discussion

E. H. Bancker (General Electric Company, Schenectady, N. Y.): While the factual data are interesting in indicating what is in use and in showing that there is a considerable amount of bus relaying in existence, it should be borne in mind that not everything being done is what should be done in the future. A tire worn to the cords may never have blown out before but if used long enough it will. To illustrate, take the practice of grounding differential-current-transformer secondaries at all current transformers versus at one point only. Safety rules may make the multiple grounds desirable but freedom from falsely tripping a bus from potential gradients around the station dictate a single ground. It may be years before a case of false tripping appears, but it is quite probable that in most instances the single ground was selected by those companies using it because multiple grounds gave trouble. Just one case of trouble will change the practice of the company having it. So it is with some of the other procedures in which there seems a diversity of practice. Hence in utilizing this report it is highly desirable to apply some intelligent sifting process to determine which of several courses is the better.

W. K. Sonnemann (Westinghouse Electric and Manufacturing Company, Newark, N. J.): This report presents an interesting résumé of design, application, and operating data on the three principal means for obtaining bus protection at the present.

The report indicates that a great deal of thought has been spent on the problem from the standpoint that, in most cases, each bus presents its own particular problem. This is true because of the great variety of factors present, and we believe that it will continue to be so for some time to come.

The operating records shown are somewhat disappointing in that they indicate a rather high percentage of incorrect operations. However, if we deduct those false operations attributed to incorrect wiring, errors in testing, improper application, and the like, all of which should tend to disappear from the picture as experience is gained, the record looks more favorable.

The problem of the adverse effects of d-c transients is one which has not always been thoroughly appreciated in the past, but which is now receiving increasing attention. "Several" false operations are attributed to d-c transients, and it is quite probable that the most of these were associated with the generating station busses, approximately 30 per cent of the total installations. The solutions presented in the paper are interesting and represent solutions designed to fulfill particular operating conditions and protection requirements. Where high-speed operation is desired, the solution of introducing time delay is not possible, of course. In such a case, the proper co-ordination of current transformer performance together with the relays applied must be given the most careful consideration. It is felt that much is yet to be learned where time of relay operation of from one to three cycles is desired.

G. B. Dodds (Duquesne Light Company, Pittsburgh, Pa.): This paper contains a wealth of information on present practices regarding bus differential installations and several interesting conclusions can be drawn from it.

With respect to the ages of the type *A* differential installations which the report lists as having been in use for 11 years, it is wondered if a mistake has not been made in the length of time and that it possibly should be 21 years instead. It is known that in the company with which the writer is associated, bus differential installations were in use as early as 1924 and it is possible that other companies had them in even earlier.

In regard to the number and causes of incorrect operations, it is felt that the number as given does not constitute a bad record considering the number of installations in service and the length of time they have been in service, even taking into consideration that it is known that the number is inaccurate and probably should be increased.

Consideration of the causes reveals that a number of them could have been prevented by more thorough checking after installation and others by better maintenance. The record of performance for the ensuing years should be better than the past because of increased attention being given to this type of installation and interchange of ideas as provided by this paper.

It is interesting to note the diversity of design features used by the different companies and also that each company seems to be well satisfied with its own methods of installation. In other words, they have been able to solve the problems that have arisen in a manner that is suitable to them.

J. H. Neher: While relay engineers are agreed that from a protection standpoint some form of bus protection should be applied to all busses of major importance, nevertheless the relative infrequency of faults on protected busses requires serious consideration in connection with the economic justification of any such installation.

Based upon the data obtained from the questionnaire which represents the experience of a probable minimum of 5,000 bus-section years of operation of bus protective systems, only one out of every $3^{1}/_{2}$

reported installations has experienced an operation and 22 per cent of these have been incorrect.

Messrs. Sonneman and Dodds have commented on this operating record which would seem to be very disappointing. As they have pointed out, in a large number of cases the reasons given for incorrect operations are such that if proper care had been taken in making and maintaining the installations, these operations would not have occurred.

In regard to the matter of incorrect operation due to transients, it must be realized that the present intense interest in such matters is only a little over a year old, and was just starting at the time that the questionnaire was circulated. It is reasonable to assume therefore that when fuller knowledge is available in this respect, a number of changes will have to be made in existing installations of bus protection.

Mr. Dodds has questioned the figures given for ages of the various types of protective systems. We wish to point out that the word "average" was unintentionally omitted in this connection. The figures given represent average ages computed on the assumption that all installations of the same type reported by any company were in service at the earliest date reported.

Mr. Bancker's point in respect to the use of multigrounded current transformers is well taken and although no incorrect operations were reported as a result of this, nevertheless it seems that this practice should be avoided.

In conclusion we wish to express our thanks to the engineers of the 34 reporting companies who filled out the rather long questionnaire and particularly to Mr. Tesar of the Cleveland Illuminating Company who examined the returns and compiled the data on which this report has been based.

Study of the Performance of the 3,600-Horsepower New Haven Electric Locomotives

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IN June 1938, the New York, New Haven and Hartford Railroad placed in operation six new 3,600-horsepower a-c d-c passenger locomotives of the same general type as the ten 2,800-horsepower engines which were put in service in 1931, but incorporating numerous improved features to give considerably increased performance to meet the more exacting demands of modern high-speed transportation.

Motive power equipment, for the electrified main-line service of the New Haven Railroad, presents many problems, decidedly different from any other metropolitan district railroad electrification in this country, which are of interest to the design and to the application engineer. It is the purpose of this paper to point out the special requirements that had to be met, to give a brief description of the locomotive and of its equipment and to show by records of service tests how the engines are applied.

New Haven Requirements

The new locomotives were designed primarily for operation in the New Haven's normal express passenger service, as represented by a maximum duty of 1,200 tons trailing (15 80-ton Pullman cars) between Grand Central Terminal or Pennsylvania Station, in New York City, and New Haven, Conn., with three intermediate stops.

A-C D-C OPERATION

From Grand Central to Woodlawn Junction, about 12 miles, operation is over the New York Central's 660-volt d-c third-rail system. From Woodlawn to New Haven (60 miles), power is supplied from an 11,000-volt single-phase overhead contact system. Trains are operated also from Pennsylvania Station to New Haven (75 miles) over the Hell Gate Bridge route, operating on an 11,000-volt single-phase supply throughout. The requirement of third-rail operation necessarily complicated the equipment as combination a-c d-c units had to be provided.

AXLE LOADING RESTRICTIONS

The Park Avenue Viaduct in New York City presents another problem in the design of New Haven locomotives. This structure has specially restrictive loading limitations which could not be exceeded.

SPEED RESTRICTIONS

Numerous speed restrictions which are necessitated by junctions, drawbridges, and curves, make the demands upon the equipment more than ordinarily severe by requiring many accelerations. An examination of the speed charts for express service between Pennsylvania Station and New Haven, included in this paper, shows the saw-tooth nature of the resulting operation.

GRADE OPERATION

From a grade standpoint, the Hell Gate Bridge route to the Pennsylvania Station is more severe than the run over the New York Central tracks to the Grand Central Station. On the eastbound run over this route the road climbs from 75 feet below sea level in the East River Tunnel to 150 feet above sea level over Hell Gate Bridge within a distance of 5.6 miles, equivalent to an average grade of 0.76 per cent and with a maximum grade of 1.50 per cent. Westbound over Hell Gate, there is a uniform average grade of 1.18 per cent for two miles.

SEASONAL VARIATIONS

The New Haven's passenger traffic is markedly affected by seasonal conditions. During July and August, week-end travel to the summer colonies on Long Island Sound puts heavy demands on the local service between New Haven and New York, while at the same time through traffic is boosted greatly by the vacation movements to New England.

New Locomotives

It will be realized, from these special conditions of the New Haven system, that the new locomotives had to be suitable for:

Severe normal duty
Still higher seasonal peak duty
A-c d-c operation
Limited axle loading

To meet these conflicting requirements, it was imperative that advantage be taken of every possible increase in material utilization to realize the maximum performance possibility of the locomotive.

Paper number 39-21, recommended by the AIEE committee on transportation, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 28, 1938; made available for preprinting December 12, 1938.

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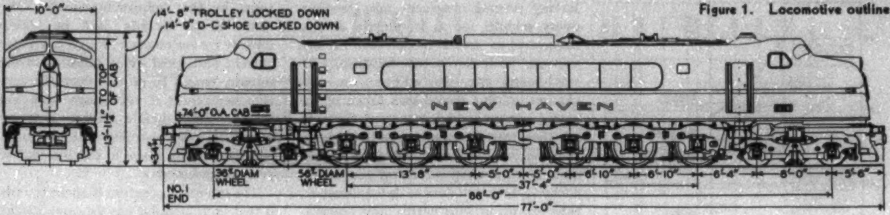

Figure 1. Locomotive outline

Performance Capacity

Whereas the continuous capacity of the new locomotives is only 800 horsepower greater than that of the 0351–0360 engines, their peak output of 7,600 horsepower exceeds by 2,800 horsepower the corresponding figure for these earlier engines.

This considerable increase in peak performance capitalizes this most important asset of electric locomotives in general: their ability to draw abundant energy from the external power supply and transform it into excess performance by virtue of their overload capacity. This permits handling loads in excess of normal, on certain trains, without alteration of schedule time and, within limits, without detracting from the life of the equipment.

Because of this intermittent character of the maximum service, a locomotive designed specifically to handle the peak load continuously would be larger, and more expensive than warranted. On the other hand, to assign locomotives continuously to duty demanding more than their normally rated capacity is a questionable application, although it is being done deliberately in some cases with older equipments and with apparent success. However, to take advantage of the overload capacity of a locomotive to handle infrequent peaks, is unquestionably justified and allows realization of the maximum usefulness of a given locomotive.

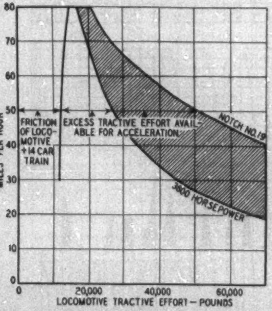

Figure 2. Surplus power available for rapid acceleration

The overload capacity of these electric locomotives is also normally used in everyday service to maintain high schedule speeds by quickly accelerating the train to the maximum permissible speed after slowdowns.

This is brought out in figure 2 which compares the maximum performance characteristic of these new engines with that corresponding to 3,600-horsepower axle output. The shaded area, representing extra capacity made available to the electric locomotive by the external power supply, will increase two- to threefold the rate at which the train will be brought back to top speed, thereby procuring faster service and a better utilization of the track capacity.

Heating and Commutating Capacity

The ability of the generating, transmission, and contact system to supply the surplus power required for peak performance, must be matched by the ability of the locomotive equipment to transform it efficiently and reliably into mechanical energy. To this end, adequate heating capacity has been provided in all parts of the electrical equipment. Also, ample commutating capacity has been built into the traction motors so that they can transmit these high outputs without excessive duty on the commutators and on the brushes. In fact, the consideration of commutation influences the design and determines the size of the traction motors to a far greater extent than the heating capacity requirement, because the commutating capacity is largely determined by the fundamental constants of the motor, while the heating capacity can be increased by the use of more ventilating air. Thus, conservative commutating conditions can only be secured by conservative design constants and the six twin-armature 12-pole traction motors with which these locomotives are equipped, are especially favored in this respect.

Control of Commutating Performance

With the basis thus laid for safe commutating conditions, special attention was given to obtaining an accurate control of the traction motor commutation throughout the entire load range.

Reduced exciting field strength is used in the new locomotives to minimize the sparking at starting, up to approximately ten miles per hour, at which speed the exciting field shunt is disconnected by the action of a phase-angle type relay.

Two commutating field settings are available in the new locomotives as in the earlier 0351–0360 units, but a new connection of a phase-angle type relay was developed to control the changeover from the low-speed to the high-speed commutation setting. The performance of this relay is truly matched to the motor. It ignores such variables as line voltage and wheel diameter and its internal characteristics insure that the change in commutating field connection is determined solely by the two factors, current and speed, which together determine the commutating condition, thereby resulting in the minimum commutator and brush maintenance.

To further insure as complete a control of commutation as possible, three indicating lights are provided, which light up in sequence as the various field contactors function and thus furnish the operating crew with a continuous check on the correct performance of the motor control equipment (figure 3).

Finally, periodic inspections and gradings established by the appearance of the commutators provide a timely warning in case some abnormal conditions might tend to increase the commutation duty.

Ventilation

With the size of the traction motors largely determined by considerations of commutation, the amount of ventilating air was fixed by the requirement of meeting the 3,600-horsepower continuous rating of the locomotive and this, in turn, determined the pressure available for the ventilation of other parts of the equip-

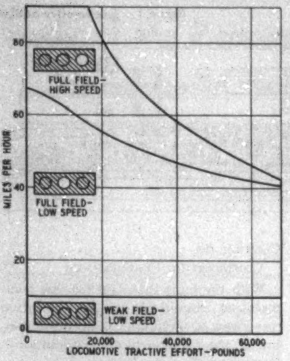

Figure 3. Operation of indicating lights

ment. Out of the total amount of air required on the locomotive, the traction motors use 61 per cent and the transformer, the blower motors, and the motor control units divide the remaining 39 per cent.

Because the ventilating horsepower requirement increases as the third power of the amount of ventilating air, it is very important that the amount of air to be supplied to each piece of equipment be accurately determined and adjusted.

Commutator-Type Blower Motors

Commutator-type blower motors as used on the New Haven locomotives are especially favorable to a close adjustment of the ventilating horsepower. They can be designed for the most suitable blower speed and the availability of various voltage taps on the transformer secondary gives the possibility of a final adjustment after all the equipment is tested and assembled. Commutator type blower motors also lend themselves to considerable energy savings by being operated on a lower voltage tap at a reduced speed, consequently at considerably less power, when full ventilation is not required. Thus, when the New Haven locomotives are energized but idle, the blower speed is reduced by 30 per cent resulting in a 65 per cent saving in blower kilowatt demand.

Service Tests and Records

Shortly after these locomotives were delivered, a series of road tests was conducted by the New Haven Railroad to verify performance in revenue service or with special test trains operating on regular passenger schedules.

Very complete records were secured by having several observers take readings every minute (or as frequently as was necessary) of speed, motor current, line voltage, controller notch, location, kilowatt-hours, etc. In addition, a continuous graphic record was taken of the motor current, which was used throughout these tests as a measure of the locomotive tractive effort available at the wheels. This is equivalent to using the series-excited traction motors as dynamometers, thus procuring with hardly any preparation an accurate record of the locomotive tractive performance.

Since the train speed variations take place gradually, it is possible to obtain a very good record of it without a recording instrument. With these two records, tractive effort and speed, synchronized by timing watches, the locomotive horsepower can be readily plotted as shown on the accompanying charts.

The same data have been plotted in a condensed, but perhaps more striking form, in the "minute-by-minute" records. These records can be taken directly by one observer, who reads motor current and speed every minute, on the minute, or can be plotted from the continuous records if such have been obtained. Each double reading is transcribed as a speed-tractive effort point, which takes its place in the locomotive performance range.

The interesting feature of these records is that each little dot has the same weight as any other little dot, in so far as the time element is concerned and any grouping of points indicates a zone where the engine is operated a large fraction of the time.

Records in both the continuous and the "minute-by-minute" form are shown for

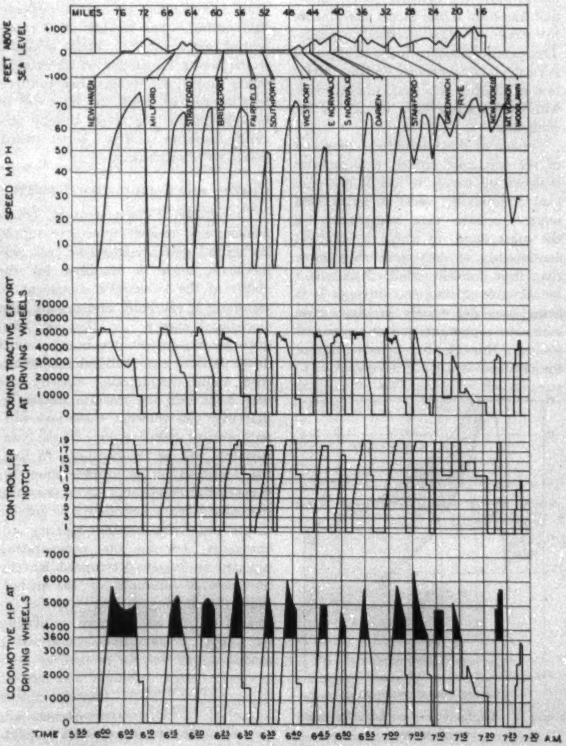

Figure 4. Continuous record, train number 367 with 15 cars, New Haven to Woodlawn

Blackened areas show use of horsepower in excess of continuous rating

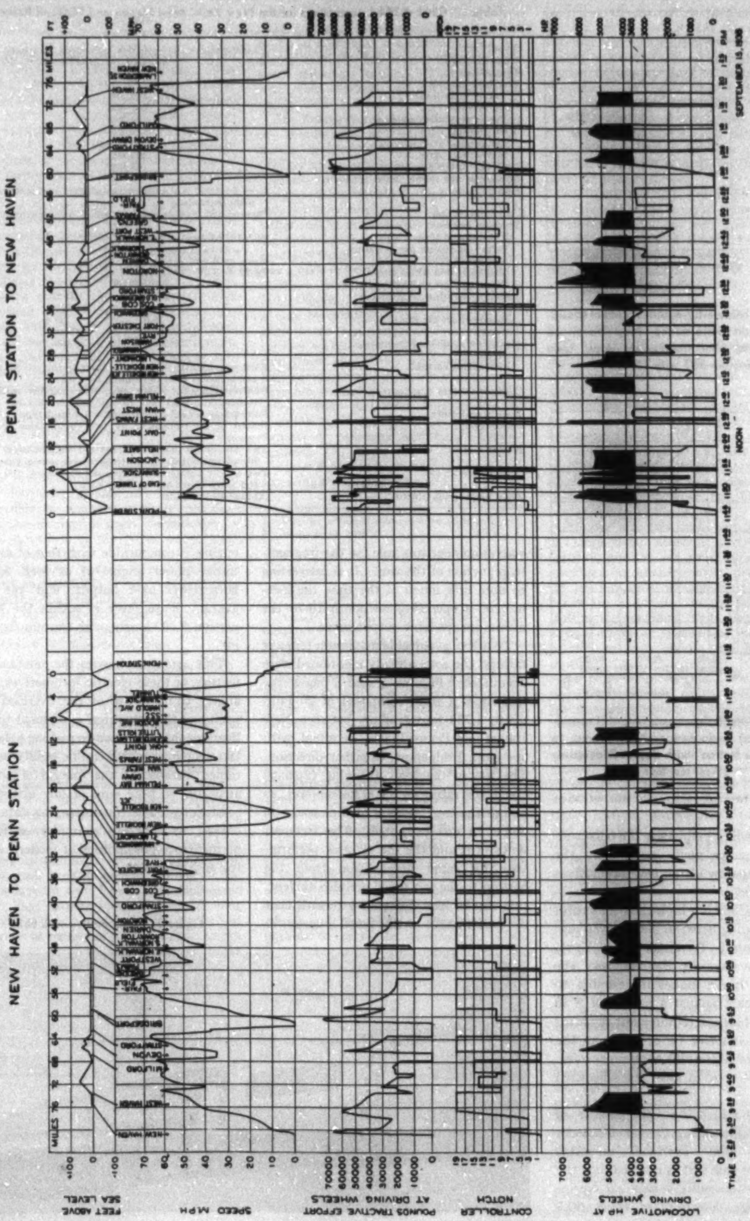

Figure 5. Continuous record, 20-car test train

Blackened areas show use of horsepower in excess of continuous rating

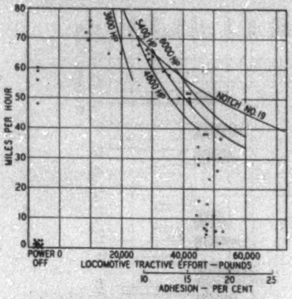

Figure 6. Minute-by-minute record of speed-tractive effort performance, New Haven to Stamford with nine intermediate stops; train number 367 with 15 cars

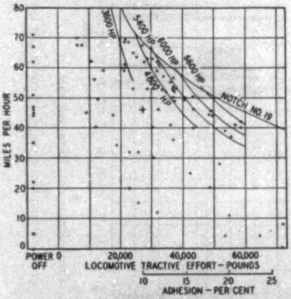

Figure 7. Minute-by-minute record of speed-tractive effort performance, New Haven to Pennsylvania Station* with stop at Bridgeport, 20-car test train

* Readings beyond Hell Gate not included

Table I. 0361–0366 Locomotives for the New York, New Haven and Hartford Railroad

Type	11,000-volt a-c, 660-volt d-c high-speed passenger	
Nomenclature	2 – *C* + *C* – 2 432/270, 6*GEA*622*A*, 11,000/660 volt	
Dimensions	See figure 1	
Weights—pounds:		
Total—fully loaded	433,200	
On drivers	272,400	
Per driving axle—average	45,400	
On guiding	160,800	
Per guiding axle—average	40,200	
Mechanical parts	269,400	
Electrical equipment	163,800	
Maximum safe speed—miles per hour	93	
Running gear	2 – *C* + *C* – 2 wheel arrangement, cast steel trucks. plain journals	
Cab construction	Streamlined, double end, all welded, outside truss type	
Traction motors:		
Number and type	6*GEA*622*A* twin armature	
Volts—maximum per armature	300	
Method of drive	Gear, quill and spring cup	
Gear ratio and wheel size	78/23 = 3.39; 56 inches	
Tractive effort at 25 per cent ad	68,100	
Locomotive rating—continuous:	A-C	D-C
Tractive effort—pounds	24,100	26,900
Speed—miles per hour	56.0	39.6
Horsepower	3,600	2,840
Maximum available horsepower	7,600	5,500
Control:		
Alternating current	Electropneumatic, single unit, three circuit, single motor combination with 19 running notches	
Direct current	Electropneumatic, single unit, series-parallel with two running notches	
Main transformer	Forced-flow forced-cooled synthetic-liquid type	
Ventilation	Forced to all equipment by two combination blower-cleaners	
Blower motors	Two two-speed a-c, one-speed d-c commutator type, forced ventilated, each with overhung control generator	
Compressors	Two a-c d-c commutator type, self-ventilated, 100 cubic feet per minute displacement each	
Train heating equipment	Oil-fired steam boiler	

two trains which emphasize the outstanding performance made possible by the overload capacity of the new locomotives (figures 4 to 7).

Train No. 367 is a high-class commuter train, operating between New Haven and Grand Central Terminal, New York. On certain days in the summer, the amount of traffic makes it desirable to handle 15 cars against a normal of 8 to 12 cars. This peak requirement demands that the locomotive be operated at the equivalent of 22 per cent continuous overload for one hour to meet the scheduled time.

Figures 4 and 6 show the records of such a run, illustrating the use of reserve horsepower and overload capacity. Operation of this sort is approaching the performance of multiple-unit car equipment, energy consumption being 60.5 watt-hours per ton mile for the frequent-stop portion of the run. It is interesting to note how much of the time the locomotive is operating on notch 19 or the maximum available output curve.

To arrive at suitable maximum tonnage ratings, the tests already mentioned were conducted. Figures 5 and 7 show the records of a train composed of 20 Pullmans, 1,756 tons trailing, between New Haven and Pennsylvania Terminal, with one stop, Bridgeport, in either direction. The use of the high available capacity above the rating of 3,600 horsepower, as well as the large amount of time spent on notch 19 will be noted. The frequent slowdowns and the use of peak performance for rapid acceleration from the bottom to the top speed are also evident.

It can be seen from these records that the performance of this 3,600-horsepower engine is comparable with that of an internal power engine of at least 5,400-horsepower axle output, and yet the average horsepower is within the locomotive 3,600-horsepower continuous rating.

This again emphasizes the outstanding feature of these electric locomotives, the ability to make use of the overload capacity of their electrical equipment to deliver surplus performance making possible fast schedules which can be rigidly maintained because of the margin of power available to take care of any abnormal conditions. The accompanying data are offered as concrete examples of these performance possibilities of modern electric motive power.

Figure 8. Locomotive with train

Discussion

Sidney Withington (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): This interesting paper presents the importance of qualifying electric locomotive horsepower rating figures to co-ordinate them with the service to be performed by the equipment. The quantity given in the paper indicating a maximum horsepower of 7,600, while undoubtedly permissible on such service as that of the New Haven, might well be too high a figure in connection with service on some other electrified territory.

The advantages in thermal capacity of the motors are obvious in this connection, in permitting short-time overloads. It is to be noted that in the overload rating of equipment under the circumstances described, the capacity not only of the main traction motors, but of the transformers, preventive coils, etc., must be taken into account. In this connection, a comparison of the characteristics of such equipment with the characteristics of the Diesel locomotive is of interest. A Diesel engine rated at 3,600 horsepower would have no overload capacity normally, and while the 3,600 horsepower would be generally available under all conditions, it could not be exceeded even for short periods. It is, therefore, impossible to compare, without qualification, the ratings of such locomotives as those described in the paper and Diesel-electric equipment.

The modern control for single-phase motors in providing excess voltage taps, permitting thus the utilization of increased horsepower ratings at higher speeds, is one of the most notable advances in the art of locomotive design in recent years.

E. W. Brandenstein (General Electric Company, Erie, Pa.): You have heard Mr. Konn tell us that these a-c locomotives are so designed, from a heating and commutating standpoint, that they will absorb the huge blocks of power furnished by the transmission and distribution system.

Now what do these overload capacities give us from a practical operating and economic standpoint? Mr. Konn has explained that the total number of locomotives required for passenger service depends upon a few peak days throughout the year. On those days the train length can be increased from, say, 12 cars to as high as 20 cars, thereby taking care of a possible 60 per cent increase in passenger traffic. That is not enough to accommodate passenger peaks which come on football days, holidays, inauguration, etc. To take care of these peaks, it is necessary to add many trains and locomotives in spite of the increase in train length from 12 to say 20 cars.

Here is a real traffic overload problem. A 60 per cent increase in train weight, with the same high schedule speed, is not enough. How are we to take care of the additional trains required without increasing the total number of passenger engines? I will tell you. This overload and commutating capacity solves the problem for us. We will resort to our freight locomotive pool for the additional passenger engines required. The peak freight traffic and peak passenger traffic do not, fortunately, come on the same day. "Oh," you will say, "you can't do that. In order to do a thing like that you must have a universal engine, one that is good for slow freight and also high-speed passenger traffic." That is exactly what this overload and commutating capacity does for us. Let me illustrate it.

The Pennsylvania Railroad have a group of 70-mile-per-hour engines which are purely freight locomotives. They have another group which are 100-mile-per-hour engines that are used only in passenger service. There is still a third group of 90-mile-per-hour engines which are used in either freight or passenger service, depending upon whether the traffic peak is passenger or freight. Last October, when there was a peak in freight service, a large number of these 90-mile-per-hour engines was in freight service. During the recent holidays, a high percentage of the 90-mile-per-hour engines was in passenger traffic. The same engines which were hauling 4,500 flat ton coal trains in October were handling the high-speed passenger trains in December. You can readily appreciate that these 90-mile-per-hour universal engines are responsible for a great decrease in the total number of passenger and freight engines required.

What does this high overload and commutating ability give us?

1. Faster schedules.
2. Heavier trains at these fast schedules.
3. A universal engine.

What does this mean from an operating and economic standpoint?

1. Fewer engines.
2. Lower capital expenditure.
3. Lower fixed charges by virtue of the lower capital expenditure.
4. Lower maintenance costs by virtue of fewer locomotives.

In other words, gentlemen, I believe this overload capacity for this type of motive power is completely changing the complexion of railway electrification.

P. H. Hatch (New York, New Haven and Hartford Railroad Company, New Haven, Conn.): One of the inherent features of electric equipment operating from an external power supply is the ability to operate for short periods considerably in excess of continuous ratings. There is nothing new in this, and the immediate extent to which such overload operation can be carried is usually limited only by weight on driving wheels and the ability of the electrical apparatus to stand the overloading. There is, however, a long term factor which may be decidedly limiting. This is the cost of maintenance. If overloading is carried to the point where its advantages are offset or exceeded by cost of additional maintenance, then the overload characteristic of electrical equipment can be a liability rather than an asset.

In the paper under consideration, the authors describe at considerable length instances of short-time overloading of the new locomotives, but except for rather general paragraphs on heating and commutating capacity, very little is said about the effect of overloading on maintenance of the equipment.

To make the story complete, it would be interesting to know specifically what has been done to obtain high overload capacity. With a locomotive of fixed maximum axle loading, was such capacity obtained by increase in ventilation, additional iron and copper, by improvement in design, or by a combination of any of these? Was a definite overload capacity planned on, or was the determination of this left for road tests or service experience? Has any relationship been established between overloading and life of apparatus, or is this, too, left for determination in service?

Providing the high overload capacity is not seriously at the expense of maintenance, it unquestionably has very great possibilities in the design of electric locomotives. These possibilities, it is believed, warrant a full and complete discussion of the means employed to obtain such capacity.

Felix Konn and **F. H. Craton**: We agree with Mr. Hatch that, from the standpoint of heating as a limitation, electrical equipment has always had overload capacity at speed. We wish to point out, however, that, in the single-phase locomotives which were built up to about ten years ago, full advantage could not be taken of this feature because the output of the earlier types of traction motors (of the resistance-lead or doubly-fed type) was decidedly limited by destructive sparking at the brushes, especially at the higher speeds.

To overcome this limitation, a new type of single-phase traction motor was developed, offering not only very favorable commutating characteristics (by virtue of the low flux made possible by a large number of poles), but also the possibility of controlling the commutating performance over a wide range of speeds and currents, especially at high speeds and high horsepowers.

As pointed out by Mr. Withington, the possibility of wide range control of the traction motor voltage which is fundamental in single-phase locomotives—but which had to be held in check with the earlier types of motors—found a vigorous outlet in the modern single-phase traction motors which permit the modern single-phase locomotives, in which full advantage has been taken of their inherent fitness for "overvoltage" and "overload" operation, to deliver the higher speeds and horsepowers demanded by progressive transportation.

Mr. Brandenstein has brought to your attention some of the tremendous service advantages of these modern engines which operators have been quick to recognize and to develop. The ample horsepower reserve and the inherent flexibility of this type of locomotive offer many more possibilities in faster schedules with heavier trains and fewer locomotives and the way is open for further advances for still better and faster service to the public and the industry.

In emphasizing the importance of qualifying electric-locomotive horsepower-rating figures to co-ordinate them with the service to be performed by the equipment, Mr. Withington has brought out a glaring deficiency in our present practice of rating electric locomotives primarily on the basis of their continuous capacity.

Undoubtedly the use of the continuous rating provides a very definite "yardstick" for one of the features which determine the

Continuous Processing for Automobile Tire Fabrics

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PROGRESS is evident to an unusual degree in the fabric-processing operations at the new automobile-tire plant of the Ford Motor Company.* The fabrics and rubbers which are run through these operations to form the rubber-fabric structures used in building the automobile tire pass through processes and machinery which clearly reflect the continuous production idea developed by the automobile industry. The electrical equipment in this plant introduces ideas which make the drives fundamentally different from those in general use.†

In these processing operations four types of rubber-fabric structures are produced: (1) ply stock, the material for the plies in the tire, (2) breaker strip stock, which goes between the plies and the tread, (3) chafer stock, used between the plies and the side walls, and (4) bead stock, from which strips surrounding the beads are made.

Fabric for the ply stock originates in a creel room from which 1,500 to 1,600 separate cords are guided from spools into parallel positions to form a broad sheet of cords without weft. Fabric for the breaker stock is the same as that for ply except that the cords are farther apart and the sheet, prepared at textile mills, is in the form of a woven fabric having a weft of very small threads. Fabrics for the chafer and bead stocks are special square-woven fabrics similar to duck in construction. From these starting points, these textile raw materials pass continuously through production lines and combinations of machines, as indicated in figure 1, to emerge as the four finished stocks.

From an electrical standpoint, the drives may be segregated into those for (1) main calendering operations, (2) bias-cutting operations in which the previously rubberized fabric is cut on the bias, and (3) slitting and coating operations in which some of the bias-cut material is slit into strips and further treated with rubber. The main calendering operations present most of the electrical problems associated with the several drives and these problems may, therefore, be

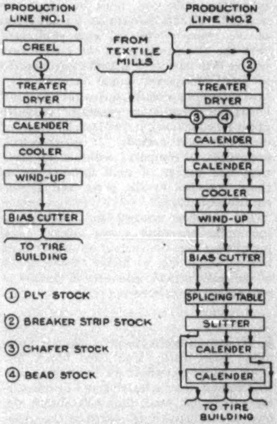

Figure 1. Diagram of sequence of processing operations

In production line number 1 the parallel cords from the creel are dipped in liquid rubber, passed through a dryer, and then through a four-roll calender which simultaneously adds rubber to both sides of the sheet. The calendered sheet passes through a cooler and then, with a liner to prevent sticking, is rolled up on a mandrel. The roll is transferred to a cutter where the fabric is cut on the bias and sent to the tire builders. In building up the tire the several plies are laid with the bias in alternate directions

In production line number 2 the fabrics pass through two calenders instead of one, the first calender working one side of the material and the second calender the other. The bias-cut material is spliced end-to-end on the splicing table. If the slit material passes through the final calenders a layer of rubber is laid on it at each calender

This paper discusses production line number 2 through the wind-up

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Paper number 39-42, recommended by the AIEE committee on industrial power applications, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 18, 1938; made available for preprinting December 20, 1938.

G. E. CASSIDY is employed by the General Electric Company at Schenectady, N. Y.; W. A. MOSTELLER is employed by the same company at Detroit, Mich.

* Because of the numerous advanced manufacturing ideas which it embodies, this $5,600,000 plant located at the Ford Motor Company's Rouge Plant, Dearborn, Mich., is of exceptional engineering interest throughout. Operations are designed to keep materials moving steadily forward from the moment the bales of crude rubber are cut up and started on their way through plasticators, mixers, mills, and so on, until the finished tires emerge from final inspection. A description of the complete plant is in the *India Rubber World*, June 1, 1938.

† Acknowledgment is made of assistance from E. F. Wait, J. T. Mortley, and A. J. Walrath of the Ford Motor Company in the development of these drives.

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value of an electric locomotive. So many of the other features (such as commutating capacity or overload capacity) are open to varying interpretations that it is very comforting to find in the continuous rating of an electric locomotive a feature which can be rigidly determined and which can form a definite basis of comparison.

However, the continuous rating should be supplemented by a service rating taking into account the peak capacity, say, 7,600 horsepower, of the locomotive which is more nearly representative of the amount of work which the locomotive can do than the continuous rating, say, of 3,600 horsepower.

Mr. Hatch has asked if a definite overload capacity is planned on. We will answer that the peak capacity of the locomotive is actually the most important factor in the design of the locomotive and that this consideration far outweighs the continuous capacity in the design of the various parts of the equipment.

To safeguard the use of the peak capacity in service, improvements in design and manufacture have been backed up by a careful system of checks in the testing, assembly, and servicing of these locomotives to insure, as much as possible, that, year in and year out, these equipments will deliver their high outputs under the conditions most favorable for the life of the equipment.

As a result of this increase in service capacity and of the efforts made to secure the best service performance, the modern single-phase locomotives are covering more and more mileage every year and with low maintenance cost per mile.

Mr. Withington also brings out the difficulty of comparing a Diesel-electric locomotive and an electric locomotive, both of which have a continuous rating of 3,600 horsepower measured at the driving wheels. The Diesel electric will deliver continuously 3,600 horsepower at the wheels and will never be able to exceed this output. The electric locomotive we have discussed will deliver 3,600 horsepower at the wheels continuously and up to 7,600 horsepower for short periods of time. The difference in accelerating performance between the two types of locomotive, which is illustrated in figure 2 of our paper, will be further emphasized by the greater weight of the Diesel electric locomotive which will require a greater part of its output for its own propulsion.

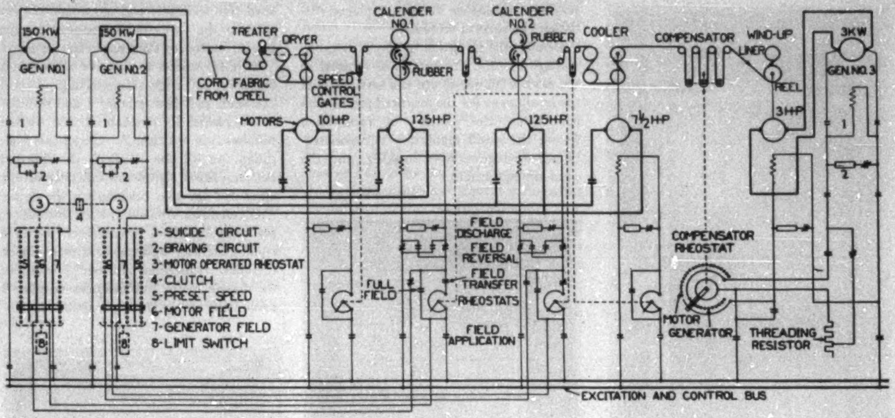

Figure 2. Schematic diagram of the armature and field circuits for the main calendering operations

described by considering one of the main calendering drives, schematically illustrated in figure 2.

Requirements of Main Calendering Line

The production speed to be 5 to 35 yards per minute with the design of the drive such that the top speed can be raised ultimately to 60 yards. Operation to be continuous at any speed within the range. While starting, stopping, and operating at any rate of production, positive and automatic speed regulation to be maintained between machine sections.

Equipment to be such that production combinations are available as follows: (1) material through the entire sequence of operations, or through this sequence with any one or more of the machine sections by-passed, (2) equipment operating as two independent drives, one drive consisting of calender number 1 and the sections ahead of it and the other consisting of calender number 2 and the sections following. In this setup, also, it must be possible to by-pass individual sections.

Equipment to be such that any combination of skim-coating or frictioning can be obtained on the calenders. By way of explanation, skim-coating consists of working rubber onto the surface of the fabric while frictioning consists of working rubber down into the structure of the fabric. The calenders have three equal-diameter rolls. The motor on each calender drives the middle roll which in turn drives the two outer rolls through gearing. The fabric passes between the middle and one of the outer rolls. When the middle and outer rolls are geared so that the roll surface speeds are equal, the fabric, which passes through the calender at the roll surface speed, is skim-coated. When the outer rolls are geared to rotate slower than the middle roll, say in the ratio two to three, the fabric takes the speed of the slower outer roll and is frictioned. Therefore, for a given fabric production speed, assuming a frictioning ratio of two to three, the driving motor operates 50 per cent faster when frictioning than when skim-coating.

Lastly, calenders to be reversible; quick stopping to be available during starting and running as a safety requirement; all control operations to be magnetic and to be actuated from simple controlling stations.

Constant or Adjustable-Voltage Drives?

Prevailing practice for calendering drives is to use adjustable-speed motors operated from a constant-voltage d-c plant system, frequently three-wire to permit doubling speed ranges by means of two armature voltages.

In the present instance, the adjustable-voltage or Ward-Leonard system of drive appeared to be particularly suited to the requirements. The smooth, relatively stepless, and rapid acceleration possible with this form of drive, the ease with which it lends itself to wide ranges in production speeds, its effectiveness where it is desired to maintain a controlled speed relation between separately driven machines, its flexibility in making the desired several combinations of the driven machines—all pointed to its adoption. In addition it offered an effective method of safety stopping, new to the rubber industry, namely, regenerative braking instead of the heretofore used dynamic braking.

A cost study of the two systems showed the adjustable-voltage system to be economically sound. In fact, a-c distribution and d-c adjustable-voltage drives proved less costly by an important margin than the common practice of d-c substation generation, d-c distribution, and constant-voltage drives.*

Driving Equipment

Most of the basic elements of the drive are shown in the sketch, figure 2. The generators are of standard shunt-wound construction, liberally rated and with a low saturation factor to enhance regen-

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* It should be noted that this statement refers to a new plant in which economic questions are not influenced by existing equipment. With a constant-voltage system already established the same economic balance would not necessarily hold.

In the present instance, the cost of three-wire motor-generator sets, duplicated to insure continuity of service, high-voltage a-c switchgear, three-wire d-c switchgear and distribution, and constant-voltage driving motors and control were compared with that of high-voltage a-c switchgear, step-down power transformers, low-voltage a-c switchgear and distribution, and adjustable-voltage motor-generator sets, motors, and control. A two-wire constant-voltage d-c power system to take care of certain constant-voltage drives which would otherwise receive power from the main three-wire system was also included in the latter. No account was taken of possible differences in operating efficiency or improved production.

It may be of interest to note that having adopted the adjustable-voltage system, future expansion continues to be economical and, because of flexibility, is easily accomplished.

Figure 3. Motor-operated generator, calender motor, and preset speed rheostats with multiple limit switches and interconnecting clutch mechanism

erative braking and stable operation at low speeds. The exciter is compound wound and supplies all control-circuit power as well as excitation.

The motors are also shunt wound. They are selected with due regard to the fact that the adjustable-voltage drive is a constant-torque drive while the calendering operations require somewhat increased torque as speed is reduced. In the present instance full armature voltage is attained at approximately half of full production speed so that the drive is constant horsepower from full to half production speed and constant torque below half production speed. This results in a normal torque rating over the entire operating range that is amply above that required by the load. Due regard also is given to the relatively greater armature resistance of the small motors in that they are selected to have sufficient capacity to prevent them from breaking down at low armature voltages from this cause.

The control equipment consists of (1) main control board on which are mounted all the control elements that are not located at the drives, (2) master control stations placed at the calenders and at the wind-up for co-ordinated but independent control of the calendering and wind-up operations, (3) an operations set-up panel, and (4) "gate"-operated speed-relation regulating rheostats placed between the several sections.

Besides the adoption of the adjustable-voltage system, the points of interest in the sketch (figure 2) are the setting up of the equipment for the required production operations, the control of production speed, the speed regulation between sections, the regenerative braking, and the wind-up equipment.

Figure 4. Typical main control panel. The relays on the bottom section control the setup operations of the rheostats, figure 3

Setting-Up Operations

To obtain the desired operating combinations, power is supplied by two independent generators. Figure 3 shows the two generator field rheostats with compound limit switches and with a clutching mechanism between the rheostats which permits independent or unit operation. Figure 4, which is typical of the main control panels, shows the rheostat setting-up relays. Figure 5 shows the master setting-up switches available to the operator.

Referring to figure 5, two of the switches control field contactors for forward and reverse operations of the calenders. Several other switches cut individual sections in and out of service. Three switches operate in the manner shown on the index plate.

Referring to the index plate, switch number 3 is placed in position *A* to operate the two calenders and their respective sections independently. In this position switch number 3 declutches the two generator rheostats so that they can be operated independently and, as explained later sets up the two calender motors as the master motors of their respective sections.

If it is desired to operate all sections together, say with skim-coating on each calender, switches number 1 and number 2 are placed in position *A* and switch number 3 in position *B*. Under this condition, as will be recalled, the relation between fabric speed and calender motor speed for the two calenders is the same. The generator rheostats, therefore, automatically position themselves to give equal generator voltages and then clutch together. Calender number 1 becomes the master motor and calender number 2 a following motor the same as the remaining motors. All of these setting up operations are automatically accomplished with the rheostat relays and rheostat limit switches.

Figure 5. Master operations set-up panel. The index plate on the panel gives the switching direction for the desired production operations

Index Plate				
24" Train		Switch Position		
Calender		Switch	Switch	Switch
No. 1	No. 2	No. 1	No. 2	No. 3
Single	Single	A or B	A or B	A
Skim	Skim	A	A	B
Fric	Skim	B	A	B
Fric	Fric	B	B	B
Skim	Fric	A	B	B
Complete train must be shut down before changing switch positions.				

Instead of the skim-coating operation on both calenders assume that it is desired to skim-coat on the first calender and friction on the second. Under this condition, calender number 2 must operate faster than calender number 1, in the ratio three to two for the frictioning ratio previously assumed. The generator rheostats, therefore, position themselves before clutching so that the voltage of generator number 2 is higher than that of generator number 1 in the ratio three to two.

Control of Production Speed

Starting, stopping, and speed control of the drive are accomplished with a stop-thread-run-hold master switch and a preset-speed rheostat, figure 6. Stopping may also be accomplished from several switches located about the driven machinery.

With the master switch in the stop position, generator excitation is removed and the generator rheostats assume the minimum generator voltage position. Turning the master switch to the thread position applies field to the generators which brings the drive to minimum speed. During this operation, full field is maintained on all motors for a definite time to insure positive breakaway and starting. Turning the switch to the run position operates the generator rheostats to raise the generator voltages to full value and then to weaken the field of the master motor.

Figure 6. Master control desk with starting and stopping control stations, speed preset rheostats, and tachometers

The speed to which the generator rheostats bring the drive is determined by the setting of the preset-speed rheostat. This rheostat has a companion rheostat of duplicate characteristics built into the generator rheostat units. The voltage drops across these two rheostats are balanced against each other and when they are equal, relays function to stop further travel of the generator rheostats. Thus, if the drive is stopped or slowed down, it will always return to the original production speed as determined by the setting of the preset-speed rheostat when the master switch is again turned to the run position.

At any time during the speeding up or slowing down of the drive, the speed can be maintained constant by turning the master switch to the hold position.

Upon stopping, the generator rheostats automatically assume the minimum generator voltage position. The rate of travel on the rheostats is adjustable for either direction. It is made fast on the down stroke to prevent delay in restarting after a stop.

Speed Regulation Between Sections

Either the motor of calender number 1 or of calender number 2 is the master motor, except when the calenders are operating independently, in which case both motors are masters. Whichever motor is the master determines the speed of production. Referring again to figure 2, it will be seen that the generator rheostats are also master motor field rheostats, that is, the master motor speed is first raised by increasing armature voltage with constant motor field and then further raised by decreasing motor field with constant armature voltage. In other words, the master motor must operate at the speed corresponding to the setting of its generator-motor rheostat.

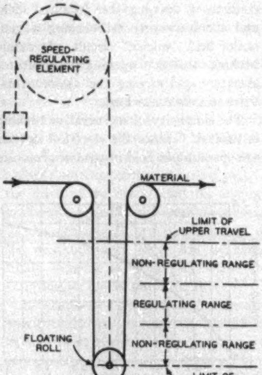
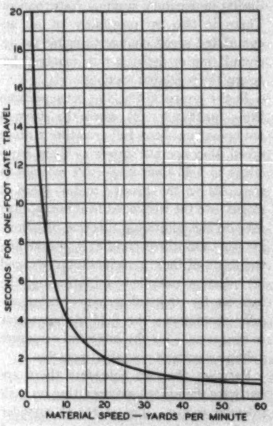


Figure 7. Schematic diagram of typical gate or dancer roll for regulating motor speed

All other motors are regulated to follow the speed of the master motor. This is accomplished by gates or dancer rolls which operate motor field rheostats. This form of regulation has been used for many years, particularly in the rubber and textile industries. It is discussed briefly here to show how well it works in connection with the adjustable-voltage drive and particularly where such a drive is operating in conjunction with speed control by motor field.

Figure 7 schematically illustrates a typical gate. Within its stops, the center roll is free to move up and down and in this travel it actuates the speed control element, in this case the field rheostat of the controlled motor. If the motor delivering material to the gate delivers ma-



Yards Output	Per Cent Difference 5	10	15	25	35	50	75	100
60	3	6	9	15	21	30	45	60
55	2.7	5.5	8.2	13.7	19.2	27	41	55
50	2.5	5	7.5	12.5	17.5	25	37	50
45	2.2	4.5	6.7	11.2	15.7	22	33	45
40	2	4	6	10	14	20	30	40
35		3.5	5.2	8.7	12.2	17.5	26	35
30		3	4.5	7.5	10.5	15	22	30
25		2.5	3.7	6.2	8.7	12.5	18.7	25
20		2	3	5	7	10	15	20
15			2.2	3.7	5.2	7.5	11.2	15
10				2.5	3.5	5	7.5	10
5						2.5	3.7	5

Figure 8. Tabulation and curve to illustrate the stability of gate control

In the tabulation, for a given yards output and percentage difference in speed between sections the figure applying gives the yards output for 100 per cent difference in speed. For example: yards output 50, speed difference 10 per cent equals 5 yards output 100 per cent difference. From the curve eight seconds are required for one foot gate travel at 5 yards 100 per cent difference terial faster than the material leaves the gate the regulating roll will be lowered and conversely, if it delivers material slower, the roll will be raised. In either event, this motion is transmitted to the regulating rheostat to bring the motor speed to exactly that required.

If the material to be worked has sufficient strength to permit passing through the gate without injury, if it will stand the required flexure, and if its surface can be permitted to come in contact with rolls or similar structures this form of regulation is one of the best. It is extremely stable as indicated in figure 8 but more important its storage capacity permits momentary differences in speeds of relatively large magnitude which is valuable during starting and stopping or during momentary running disturbances. There is also no possibility of an accumulated speed change to cause an accumulation or deficiency of material between the regulated sections.

In the present drive, the gates perform three functions: (1) when the drive is not operating above full armature voltage speed, they operate merely as speed regulators to hold their respective sections in step, (2) as the master motor speed is raised into the weakened field range, they take on the additional function of following up and correspondingly weakening the fields of their associated motors, and (3) when a calender is frictioning, the gate on its companion motor automatically strengthens the field of that motor to compensate for the increased armature voltage accompanying this operation.

Between the two calenders there are two gate rheostats, one operating on each calender motor. The motor of calender number 1 is the master and the motor of calender number 2 is regulated to follow it for all operations except when frictioning on calender number 1 and skim-coating on calender number 2. For this particular operation, the motor of calender number 2 is made the master because this motor is operating below maximum generator voltage after the motor of calender number 1 has gone up to weakened field speed. The generator and motor field rheostats do not produce an operating speed strictly proportional to their travel. When the two rheostat units are offset, the differences in characteristics of the offset positions are compensated for by the gate speed-regulating rheostat. Wide range regulation of the regulated motor is more effective in the weakened-field range than in the armature-voltage range and the regulated motor is therefore set for this condition by making the motor of calender number 2 the master.

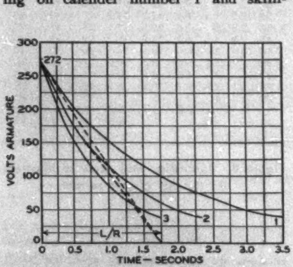


Figure 9. Typical field decay characteristics for 150-kw generator

For any armature voltage, inductance divided by field circuit resistance (*L*/*R*) is a measure of speed of field decay

Regenerative Braking

Regenerative braking was adopted instead of the customary dynamic braking because of its greater simplicity and because it produces as quick, if not a quicker, stop than by dynamic braking. Regenerative braking as referred to here consists of opening the generator field and simultaneously establishing a generator field "suicide" circuit. Dynamic braking consists of opening the generator armature and closing the motor armatures on a resistance load.

The simplicity of regenerative braking is twofold. Since the electrical devices are operating in field instead of armature circuits, they are simpler and less bulky. Again, as regenerative braking stops are so easily accomplished, all stops can be made regenerative braking stops and it is not necessary to design the controls for two forms of stopping as is customary in the case of dynamic braking when normal stopping is a coasting stop and an emergency requires a dynamic braking stop.

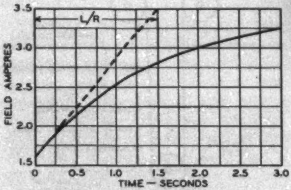


Figure 10. Typical field build-up characteristics for 125-horsepower motor

The regenerative braking stop also helps in another way. Since this system is applied to all stops, the several sections of the drive are brought to rest quickly and they therefore cannot depart far from their regulated positions with respect to each other. This, coupled with the fact that the adjustable-voltage drive quickly brings the sections into regulating range upon starting, results in an unusually effective speed-regulating system.

Neglecting the "suicide" circuit which is used to destroy residual and which may be considered as not affecting the results practically, the selection of generator characteristics for regenerative braking can be illustrated by the curves in figure 9. The rate of field decay is inversely proportional to the field inductance and directly proportional to the resistance of the discharge circuit. The instantaneous values of field current are a function of these two factors and the third factor—time. With these factors and the generator saturation curve, the expected performance shown in the curves can be calculated.

Having determined a reasonable inductance for curve 1, faster curves such as 2 and 3 can be obtained by increasing the discharge resistance. If it is decided that say 1.75 seconds is a desirable maximum rate of field decay, the control equipment can be arranged to operate on curve 1 at approximately 150 volts armature and above and on curve 2 below 150 volts.

Figure 11. The main d-c panels for all processing operations are housed in these cubicles. The machinery is on the floor directly above

After having determined the generator characteristics the motor characteristics are checked to see that they fit into the cycle established by the generator. This is accomplished in somewhat the same manner as the calculation of the generator except that the field is rising due to application of full field for the stop and except that an additional factor, the inertia of the system,* must be taken into account. In this instance the resistance is the resistance in the motor field circuit. For practical purposes each motor may be treated independently and when so treated curves of which figure 10 is typical will result.

Figure 13. Material from calender number 2 passing through the cooler and compensator. The material at the right is going to the wind-up. The rheostat mounted on top of the compensator controls the wind-up speed

Figure 12. Material passing from calender number 1 into calender number 2 through the speed regulating gate. The dark surface of the material shows the rubber added by calender number 1

If the rate of motor field build-up does not differ appreciably from the rate of field decay previously calculated for the generator or in other words, if the motor becomes a generator, at approximately the same rate that the generator becomes a motor, a stable braking system may be expected. In general, if the motor does not operate over a speed range by field control greater than approximately two to one the motor will likely have inherently the desired field characteristics. For a greater speed range it may be found desirable to control the rate of motor field build-up.

Wind-Up Equipment

The essential differences between the wind-up operations and the operations previously described are first, that the wind-up cycle is intermittent due to the necessity of shutting down the wind-up to remove a full mandrel and insert an empty one and second, that the required operating speed range is appreciably increased because the difference in driving speed between an empty and full mandrel is added to the speed range of the continuous-production part of the system.

The matter of intermittent operation is provided for by inserting a material storage or compensator ahead of the wind-up. The compensator is shown in figure 2 from which it will be seen that if the wind-up has a production speed higher than that of the preceding part of the drive to the extent that the average production speed of the wind-up is equal to the continuous production speed of the preceding part, there will be eventually as much material taken out of the compensator as is put into it.

Intermittent operation and increased speed range of the wind-up are taken care of most effectively by again resorting to the adjustable-voltage system operating in conjunction with adjustable-speed motors. The wind-up motor is driven by an independent generator. The wind-up operator has at hand an interlocked-independent control station. In the interlocked position the wind-up starts and stops simultaneously with the preceding main drives. In the independent position, independent control of the wind-up is secured from a stop-thread-run control station.

Referring again to figure 2, it will be seen that if this control station is moved from stop to thread, full field is applied to the motor and field is applied to the generator through a generator-field threading resistor to give the armature voltage required for the desired threading speed.

Turning the control further or to the run position, transfers control from the threading resistor to the compensator-controlled rheostat. This rheostat is a combined generator and motor field rheostat designed to operate in the same manner as the combined rheostats for the calender motors. The setting of the rheostat is determined by the position of the compensator rolls from which it is operated in the same manner as the gate speed-regulating rheostats. From full to approximately half full compensator, the wind-up operates at full speed. From half full compensator to empty compensator, the speed is reduced from maximum to minimum. Limit switches are placed at the extreme compensator travels to shut down the drives should the normal operating range be exceeded.

All starts are made automatically through the threading resistor so that all slack in the equipment is taken up before the drive is thrown over to operating speed. As with the remainder of the drive, all stops for the wind-up are regenerative braking stops.

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* Ascertain if the horsepower necessary to stop within the required time is within the capacity of the motor. This should be calculated at maximum speed which is the worst condition.

$$\text{Torque} = \frac{WR^2N}{307\,t} \text{ and horsepower} = \frac{N\ \text{Torque}}{5{,}250}$$

in which WR^2 is the inertia of the system, N is the operating speed, and t the required seconds to stop.

The Permatron—A Magnetically Controlled Industrial Tube

By W. P. OVERBECK
ASSOCIATE AIEE

THE Permatron is a new gas-filled control tube in which the initiation of a discharge may be controlled by an externally applied magnetic field. The possibility of this type of control has been recognized during the past few years,[1] but little attention has been paid to proper design or to the interesting applications of tubes built specifically for magnetic control. The present paper has been written as the result of research work which has shown that magnetic-control tubes are a reliable and useful addition to the field of electronics. The trademark "Permatron" has been chosen to indicate the use of magnetic control.

Construction and Operation

Figure 1 shows a typical Permatron construction. In addition to the anode and cathode, which are conventional, the bulb contains a cylindrical collector electrode which surrounds the discharge path. The controlling magnetic field is applied as shown through the poles of an electromagnet located outside the bulb. Nonmagnetic materials are used for all parts of the tube which might affect the proper distribution of the magnetic field. The most practical materials are graphite, for machined parts, and 18.8 stainless steel, which has a permeability of 1.1 when properly annealed. The nickel-alloy materials used for cathodes are operated at temperatures sufficiently high to reduce their permeability.

When a positive anode voltage and a magnetic field are applied to the Permatron, electrons emitted by the cathode are deflected and strike the collector. Due to the presence of the collector, which is usually maintained at a potential close to or equal to that of the cathode, this deflection takes place in a region of weak electrostatic field intensity and the electrons are removed from the discharge path before acquiring sufficient velocity to cause ionization. If the magnetic field is removed, the electrons proceed toward the anode, gaining velocity and producing ionization. Conduction continues at a low-voltage drop between anode and cathode until the anode voltage is removed or reversed. Thus, the initiation of a heavy current discharge may be controlled by diverting electrons which represent but a microscopic portion of the same current. For each value of anode voltage, there is a critical value of magnetic field intensity above which the discharge will not start. A theory of the relationship of this critical magnetic force to anode voltage and to the pressure of the gas within the tube has been developed and shows an interesting agreement with actual results.

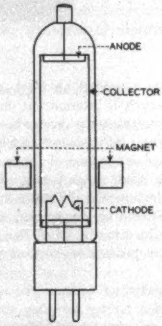

Figure 1. RM-201 Permatron

An electron in an electrostatic field and a transverse magnetic field moves in a series of cycloid paths as shown in figure 2, and in a general direction at right angles to both fields. The values of magnetic and electrostatic fields normally employed in the Permatron are such that the dimensions of the individual cycloids are small and the electrons may be considered as moving sidewise in a straight line. However, due to the presence of gas molecules, the sidewise motion is broken up by collisions between electrons and molecules. At each collision, the electron may gain a little in progress in the direction of the electrostatic field. If we take, as an index of the effective sidewise motion of the electron, the number of cycloid paths in a mean free path, or average distance which an electron may travel without collision, and assume that this number must be a constant to prevent conduction in a given Permatron design, we obtain the following relation:

$$\text{magnetomotive force} = K\sqrt{EP}$$

giving the critical magnetomotive force as a function of anode voltage E, and gas pressure P. K is a constant which includes proportionality factors between pressure and mean free path, magnetomotive force and magnetic field intensity, anode voltage and electrostatic field intensity, and the assumed constant ratio between mean free path and length of the cycloid paths. Figure 3 shows values of the constant K (magnetomotive force/$\sqrt{EP}$) plotted against magnetomotive force for the full range of control of a mercury-vapor Permatron. The deviation from the theoretical constant value at low values of magnetomotive force corresponds to conditions under which the dimensions of the individual cycloid paths become comparable in size to the internal dimensions of the tube.

When a straight cylindrical collector such as that of figure 1 is used, the mag-

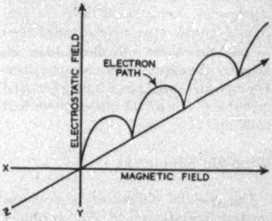

Figure 2. Path of an electron in electrostatic and transverse magnetic fields

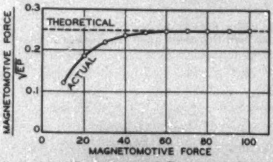

Figure 3. Comparison of Permatron theory with actual test data

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Paper number 39-20, recommended by the AIEE subcommittee on electronics, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 3, 1938; made available for preprinting December 10, 1938.

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Development of the Permatron and its methods of application is the result of co-operation of the members of the research and development department of the Raytheon Production Corporation.

[1] For all numbered references, see list at end of paper.

netic sensitivity of the Permatron increases approximately as the cube of the ratio of distance between anode and cathode to diameter of the collector. This is due partly to the resulting decrease in electrostatic field intensity in the control region and partly to the fact that a greater length of collector surface is available to catch electrons. The practical limits of sensitivity are reached when the shielding effect of the collector becomes so great as to prevent starting of the tube at reasonably low anode voltages when no magnetic field is present.

When other requirements such as the proper distribution of heat in a mercury-vapor tube must be met, a construction such as that of figure 4 is convenient. Here, the effective region of the collector is a cylindrical constriction formed by holes in a pair of graphite disks. The magnetic field is led into this region through iron armatures from a part of the bulb where location of the external magnet is most convenient. The use of two graphite disks, one above and the other below the iron poles, helps to shield the iron, which might otherwise tend to produce secondary electrons, and increases the length of the effective region. The outer cathode heat shields of this tube are made of nonmagnetic steel.

Characteristics

There are three distinctly different types of operation obtainable with the Permatron. Characteristics are shown in figure 5 for simple magnetic control in which the collector is directly connected to the cathode. These curves indicate the variation in control characteristics caused by changes in condensed-mercury temperature in the tube of figure 4. For circuit design purposes, it is most convenient to express the magnetic field intensity in terms of ampere turns applied to an electromagnet located in the most effective position. To give an approximation of the power needed for control, the value of 300 ampere turns in figure 5 represents 2.34 watts consumed in the magnet used for obtaining data for the curves. The tube of figure 1 requires about 0.075 watts for control of its maximum allowable anode voltage. These control power requirements are small in comparison to the power which the tubes will handle. The tube shown in figure 4 will control 16 kw while that of figure 1 will control 10 watts.

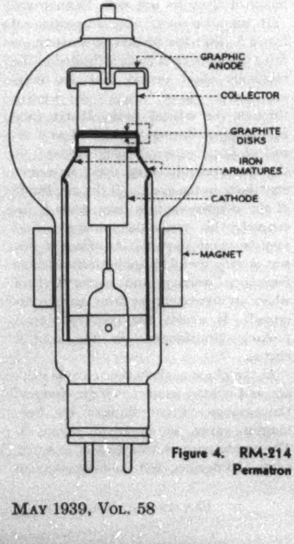


Figure 4. RM-214 Permatron

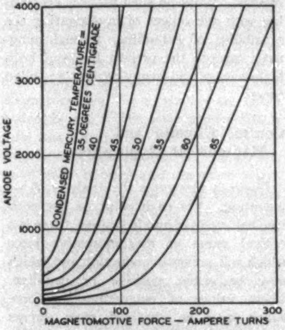


Figure 5. Characteristics of RM-214 Permatron

A second type of operation is that in which varying collector potentials are employed. This is similar to grid control except for the fact that the controlling effects of collector potential and magnetic field are interrelated as shown by the curves of figure 6. These particular curves indicate that the negative collector potentials and magnetic field intensity have an additive effect. This is not always true because it has been found that, in some other tube constructions, the minimum anode firing voltage may occur at a negative collector potential or at some value of magnetomotive force greater than zero. Such unusual effects are possible because the region where the magnetic field is applied may be located in a position where collector potential has a great effect on electrostatic field intensity or in a position where the electrostatic field intensity does not vary appreciably with collector potential. Also, the magnetic and electrostatic fields may be used to produce focusing effects which counteract one another.

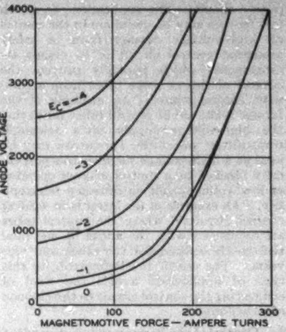


Figure 6. RM-214 Permatron characteristics showing effect of varying collector potential

Condensed mercury temperature = 45 degrees centigrade

The third type of operation is that in which ionization may be present in the tube without producing a discharge between anode and cathode. A Permatron such as that of figure 4 may be operated with a discharge between collector and cathode; in which case, the magnetic field may be used to prevent the discharge from spreading through the holes in the graphite disks to the anode. When the cathode-collector discharge is small, of the order of one milliampere, this has the same effect as would be obtained by moving the cathode closer to the holes in the graphite disks and produces anode control characteristics similar to those of figure 5, except that greater values of magnetomotive force are required and the anode firing voltage at zero magnetomotive force is greatly reduced. This type of operation has been used to make a tube designed for high-voltage applications more useful in circuits where low anode voltages are employed.

Field of Application

The Permatron performs the same type of service as has been obtained from thyratrons[2] and ignitrons.[3,4] Thus, its field of application overlaps to some extent. In the same field, it has three features which are useful in certain types of applications:

1. An electromagnetic control may remain insulated from the tube and its associated

power circuit. The applications in which this feature may be useful are in the control of high-voltage circuits from a safely grounded control circuit or in circuit arrangements which normally prevent the control of two or more tubes from a common control source. An example of the former is the use of control tubes for keying the high-voltage supply of a telegraph transmitter[5] where the Permatron may be used as a high-voltage rectifier and periodically blocked by a control magnet operated at low voltage from an ordinary telegraph key. An example of the latter is in welding control circuits[6,7] where the control tubes are connected with the anode of one tube tied to the cathode of the other and vice versa. The use of the Permatron for this type of application avoids the need of connecting the control circuit to the cathode of either tube.

2. Control of the Permatron is independent of the polarity of the magnetic field. In cases such as the welding circuit mentioned above or in full-wave rectifier circuits where the anode voltages of two tubes are 180 degrees out of phase, the voltages applied to their control magnets may have the same polarity or a single magnet may be used to control both tubes.

3. The Permatron produces unusual phase-shift control characteristics which are described in detail later.

The usefulness of the Permatron extends to a new field of applications due to other factors which are listed below:

1. New combinations with moving machinery are possible because the Permatron may be controlled by moving permanent magnets. An example of this is in the problem of elevator leveling or control where permanent magnets might be mounted on the cage to operate Permatrons at various levels in the shaft.

2. A wider application to communication circuits or measuring instruments is possible because of the lack of reaction between the tube and control magnet. The control impedance remains the same and is linear whether the tube is conducting or not. Thus, the Permatron may be operated directly from a transmission line without danger of reflection or may be operated from carefully balanced measuring circuits without danger to instruments.

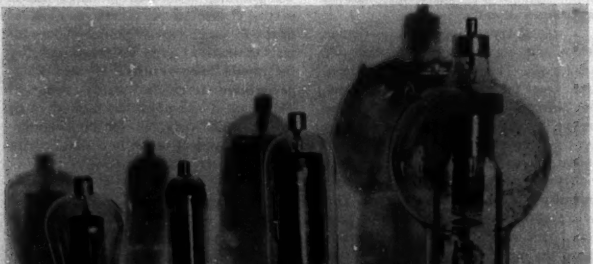
3. Operation of the control from extremely low voltages is possible, although heavy control currents are required. As an example, the tube illustrated in figure 1 can be controlled by a voltage lower than six microvolts when a single-turn control magnet is used.

Most of the development of Permatrons has been confined to the small tubes and medium power industrial tubes illustrated in figure 7. The ratings of the tubes range from an average current of 0.1 to 8 amperes and up to 3,500 volts. There is, however, no factor which prevents application of the same principles to tubes of higher voltage and current ratings. Work on such tubes is proceeding with the object of investigating the possibility of extending, through magnetic control, the present limits of both high-voltage and heavy-current gas-filled tubes.

Control Circuits and Methods of Application

Figure 8 illustrates the analysis of a-c operation. For each point on the positive half cycle of anode voltage, there is a critical value of magnetomotive force which will prevent conduction and which may be either positive or negative. These values of magnetomotive force which may be obtained from curves similar to those in figure 5, form a closed oval as shown by the dotted line. If an alternating magnetomotive force is applied to the control magnet, conduction starts in each cycle at the point where the magnetomotive force wave intercepts the critical oval and continues until the anode voltage becomes negative. This period of conduction, indicated by the shaded portion of the diagram, may be increased or decreased by varying the phase relation between anode voltage and magnetomotive force.

Figure 7. Experimental Permatrons



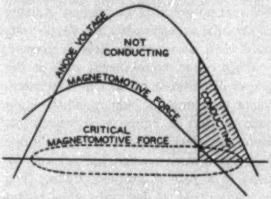


Figure 8. Oscillographic analysis of a-c operation

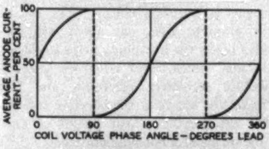


Figure 9. Output characteristic of Permatron with phase-shift control

Phase shift control of this type has been described in connection with other types of control tubes[3] so it is possible to limit the present discussion to unusual results obtained from its use with Permatrons.

It may be seen, from inspection of figure 8, that the instant of starting, or firing angle, in each cycle is limited to the relatively short period that the magnetomotive force wave is passing through the critical oval. If the tube fails to start during this period as a result of some external condition such as the anode circuit being open, it cannot start later in the cycle. If the amplitude of the magnetomotive force wave is increased, this available starting period may be made shorter. An effect of this sort is very useful in applications such as resistance welding and motor control where an accurately set firing angle is required. It avoids the need of special peaking transformers in the control circuit.

As the phase angle between anode voltage and control-magnet voltage changes, the average current through the Permatron varies as shown in figure 9. The anode current variation is repeated every 180 degrees, and the discontinuous

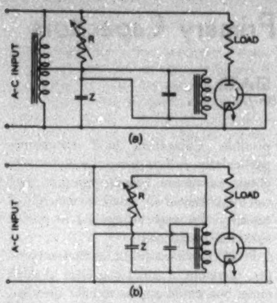


Figure 10. Permatron phase-shifting circuits

points occur at the power-factor angle of the control magnet which is usually close to 90 degrees. In welding or in full-wave rectifier circuits where tubes are connected with their anode voltages 180 degrees out of phase, the two control-magnet voltages may be in phase or a single magnet may be used to control both tubes.

Two circuits which may be used for phase-shift control of the Permatron are shown in figure 10. The first (*a*) is an adaptation of the conventional type of phase-shift circuit. The reactive element *Z* may be either a capacitor or an inductor. When the resistor *R* is varied from zero to infinity, the phase angle of the control magnet varies from zero to 180 degrees with respect to the anode voltage. A capacitor whose capacitance resonates with the control-magnet inductance at the power-line frequency may be connected in parallel with the control magnet to reduce the current drawn from the phase-shift circuit. The second circuit (*b*) avoids the need of a center-tapped control-voltage source by using a center-tap on the control magnet. This circuit is easy to understand when it is realized that it is the same as the one above except that the connections are reversed. The magnet takes the place of the center-tapped source and the terminals which would normally be the output of the phase-shift circuit are connected to the power lines. The impedance *Z* and the mean value of the resistor *R* should be large compared to the impedance between the center-tap of the control magnet and the two outer terminals when the latter are short-circuited and should be small compared to open-circuit impedance between the outer ends of the control magnet. Use of a resonating capacitor across the magnet makes the latter value larger.

In automatic control circuits, it is usually preferable to obtain phase-shift control from direct current. This preference is due to the fact that such control devices often include a vacuum-tube amplifier which produces a varying d-c output. A very useful circuit for this type of control is shown in figure 11. Two coils are wound on each control magnet, one to be operated from alternating current and the other from direct current. When two tubes are used, these coils may be arranged so that the alternating voltages induced in the two d-c coils are balanced out. The same result may be accomplished with single-coil magnets if the coils are connected in a balanced bridge circuit with the alternating current applied to one pair of opposite corners and the direct current to the other corners. The oscillographic drawing below the circuit diagram shows the results obtained. Due to the balancing connection, the d-c magnetomotive force shifts the two a-c magnetomotive force waves in opposite directions. These magnetomotive force waves are illustrated as sine waves, each drawn with a solid line during the half cycle that its associated anode is positive. Since one wave shifts upward and the other downward, the firing angles of the two tubes shift in the same direction and their output currents remain balanced. If the connections to the a-c coils are reversed, the direction of shift in firing angle with a given change in d-c magnetomotive force is reversed.

A smooth control of output current of a Permatron may be obtained with moving permanent magnets by applying the same principle as the circuit of figure 11.

Although several complete circuits using Permatrons have been developed, a detailed description of all of them would require more space than could be alloted to one paper. However, the full wave rectifier circuit of figure 12 is shown in order to give an example of the combination of a few of the principles described above in a complete circuit. The control magnet system is a combination of figure 11 and figure 10 (*b*). The d-c coils are operated by an amplifier tube whose plate current is controlled by variations in output voltage of the rectifier. The phase-shift circuit of the a-c coils is adjusted to give the maximum rate of change of output voltage for a given change of current in the d-c coils and to set the mean output voltage at a value which produces the optimum performance of the amplifier tube. A resistance and capacitor time-delay circuit is used in the grid circuit of the amplifier to prevent hunting. This particular circuit is designed to give a constant output voltage regardless of input voltage or load variations. If the d-c coils are of low impedance and connected in series with the load, the rectifier may be made to have a drooping characteristic or a rising characteristic depending on the phasing of the a-c coils.

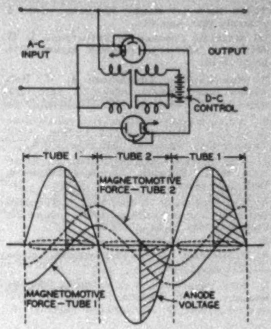


Figure 11. Circuit diagram and analysis of combined a-c and d-c magnetic control

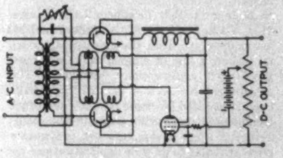


Figure 12. Fundamental circuit diagram of Permatron-controlled rectifier

Conclusion

Magnetic control as described above has proved itself both reliable and efficient and, having so many useful features, it should find many industrial applications. Moreover, since it is possible to obtain both grid control and magnetic control effects in the same tube, there are many applications in which the desirable qualities of both types of control may be obtained simultaneously. In circuit combinations and in possible variations of control characteristics, the Permatron should provide a fertile field for new ideas.

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Discussion

S. B. Ingram (Bell Telephone Laboratories, New York, N. Y.): Mr. Overbeck and his associates have done the industry a service in making a line of magnetically controlled gas tubes commercially available. The advantages of devices of this type have been recognized for some time as the author has pointed out in his paper. The availability of a new device, however, is always a great stimulus to its wide application and we shall now have an opportunity to see the magnetically controlled tube prove its mettle in competition with other types of gas-filled control tubes.

There is one characteristic of mercury-vapor-filled Permatrons which, I believe, will be a considerable impediment to its circuit application. This is the variability of its characteristics with condensed mercury temperature. The characteristics of the thyratron or grid-controlled gas-filled tube also vary with temperature, but extreme shifts in the characteristic take place only below condensed-mercury temperatures of about 40 degrees centigrade. In the ordinary operating temperature range from 40 degrees centigrade to 80 degrees centigrade the characteristics are relatively independent of temperature. In the Permatron, the variability extends over the whole temperature range as Mr. Overbeck's figure 5 shows and as, in fact, his theory of breakdown demands. Rare-gas-filled tubes would be free from this disadvantage although their use would necessarily be confined to low-voltage circuits. I should like to ask if any work has been done on Permatrons with rare-gas filling.

W. P. Overbeck: Mr. Ingram's discussion is greatly appreciated. As pointed out in the paper, our work has led to the discovery of previously unrecognized advantages of magnetic control and it is natural to expect that, with special tubes available, other engineers will be able to add important contributions. In addition to the competitive situation mentioned by Mr. Ingram, it is to be hoped that the unusual features of the Permatron will create a new field of application.

In contradiction to the theory of control which I have proposed, I believe that there is a possibility of reducing variations of characteristics with temperature. Nonvarying characteristics of the thyratron in the range of temperatures mentioned by Mr. Ingram are not true of all types. For example, the *FG*-43 and *KU*-628 show considerable variation in this range. We are thus led to the conclusion that refinements of design are responsible for the improved characteristics of types such as the *FG*-29. A similar refinement in design of the Permatron would be such that tube construction, rather than vapor pressure, would determine the effectiveness of magnetic deflection. On the other hand, it should be noted that in an experimental industrial installation, mercury-vapor Permatrons have operated over 5,000 hours without difficulty from characteristic variations. This is, of course, due to proper circuit design.

Considerable work has been done on Permatrons with rare-gas filling. The two tubes shown in the center of figure 7 are made with argon gas. This removes variation of characteristics with ambient temperature.

Secondary Versus Primary Capacitors

By F. M. STARR
MEMBER AIEE

Synopsis: Secondary capacitors cost more than primary capacitors but they offer some additional benefits which may offset the additional costs. There are presented herein the results of a comprehensive economic study covering a wide variety of circuit and load conditions on both network and radial distribution systems to establish within reasonable limits the probable field of application of the secondary capacitor.

From the results of the study it appears that secondary capacitors can be justified in practically all cases on secondary networks but have a limited application on primary radial distribution systems.

During the past two years shunt capacitors have been applied to distribution systems in increasing quantities. Continual increases in reactive loads due to refrigerators, air conditioning, fans, and other small motor loads have had their destructive effects and corrective measures have more than ever become necessary.

Reactive amperes originate at the load and they carry their destructive effect in the form of excessive voltage drops, thermal overloads, and energy losses, back to the generator. Naturally enough, corrective measures to affect the maximum benefits should be located as close to the load or source of reactive as possible. Capacitors lend themselves particularly well as corrective devices to obtain maximum benefits because they can be obtained in small increments of capacity and may be applied in proper sizes close to the load.

From the viewpoint of maximum benefits, therefore, a small capacitor at each meter box would appear to offer the ideal correction. On the other hand, from the viewpoint of maximum benefits commensurate with maximum economy, the relative costs of capacitor correction applied at various points in the distribution system must be factored.

The cost of capacitors varies principally with the size of the individual unit and with the voltage rating of the unit. At the present stage of the art, design and manufacturing limitations are such that the capacitor having the lowest cost per kilovolt-ampere is a 10- or 15-kva unit having a voltage rating of 2,300 or 4,000 volts. This, of course, means that the lowest cost correction is that which can be applied to the primary feeder. The difference in cost between primary and secondary capacitors is quite substantial (see figure 3) and it probably is for this reason that practically all capacitors which have been applied by utilities for reactive correction have been primary-feeder capacitors.*

Although secondary capacitors cost more, they do offer some additional advantages over primary capacitors. Consider for a moment what these additional advantages consist of:

Comparative Benefits of Secondary Versus Primary Capacitors

The elimination of reactive kilovolt-amperes from a system releases system

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Paper number 39-62, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted September 28, 1939; made available for preprinting December 30, 1938.

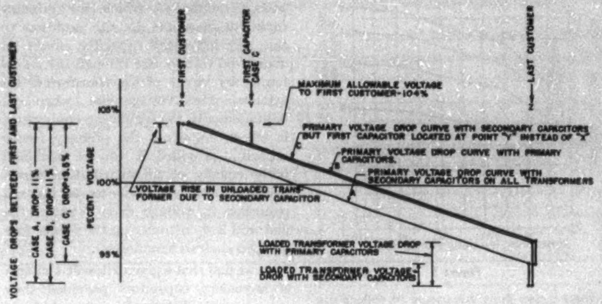
F. M. Starr is a member of the central station engineering staff of the General Electric Company, Schenectady, N. Y.

The author wishes to acknowledge here the large amount of work by T. F. Skinner in making all of the calculations and preparing all of the curves used in this paper and also the contribution of O. B. Falls in preparation of the manuscript.

* Capacitors applied by industrials, of course, are necessarily low-voltage capacitors since the utilization voltage is in most cases the only voltage available in an industrial plant.

Figure 1 (right)

These curves show that secondary capacitors do not give any additional reduction in voltage drop between first and last customer if all transformers are equipped with the secondary units. To gain additional reduction in voltage drop, the secondary capacitor installations should be confined to the section of line between *Y* and *Z* above. Point *Y* is that point at which the primary feeder line drop is equal to the voltage rise in an unloaded transformer due to a secondary capacitor. In the above curves, it is assumed that the first transformer may be unloaded at peak feeder load


X—FIRST CUSTOMER
FIRST CAPACITOR CASE C
Y
LAST CUSTOMER
Z
MAXIMUM ALLOWABLE VOLTAGE TO FIRST CUSTOMER-104%
PRIMARY VOLTAGE DROP CURVE WITH SECONDARY CAPACITORS BUT FIRST CAPACITOR LOCATED AT POINT "Y" INSTEAD OF "X"
C
PRIMARY VOLTAGE DROP CURVE WITH PRIMARY CAPACITORS.
B
PRIMARY VOLTAGE DROP CURVE WITH SECONDARY CAPACITORS ON ALL TRANSFORMERS
A
VOLTAGE RISE IN UNLOADED TRANSFORMER DUE TO SECONDARY CAPACITOR
105%
100%
95%
PECENT VOLTAGE
VOLTAGE DROPS BETWEEN FIRST AND LAST CUSTOMER
CASE A, DROP=11%
CASE B, DROP=11%
CASE C, DROP=9.5%
LOADED TRANSFORMER VOLTAGE DROP WITH PRIMARY CAPACITORS
LOADED TRANSFORMER VOLTAGE DROP WITH SECONDARY CAPACITORS

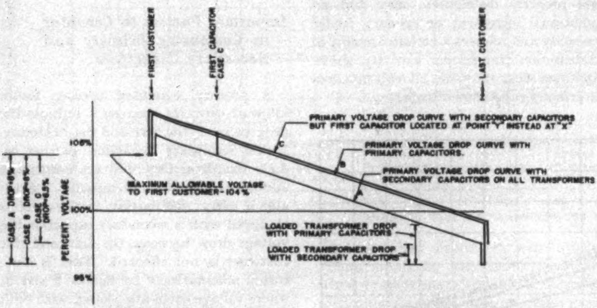

X—FIRST CUSTOMER
FIRST CAPACITOR CASE C
Y
LAST CUSTOMER
Z
PRIMARY VOLTAGE DROP CURVE WITH SECONDARY CAPACITORS BUT FIRST CAPACITOR LOCATED AT POINT "Y" INSTEAD AT "X"
C
PRIMARY VOLTAGE DROP CURVE WITH PRIMARY CAPACITORS.
B
PRIMARY VOLTAGE DROP CURVE WITH SECONDARY CAPACITORS ON ALL TRANSFORMERS
A
MAXIMUM ALLOWABLE VOLTAGE TO FIRST CUSTOMER-104%
105%
100%
95%
PERCENT VOLTAGE
CASE A DROP=8%
CASE B DROP=8%
CASE C DROP=6.5%
LOADED TRANSFORMER DROP WITH PRIMARY CAPACITORS
LOADED TRANSFORMER DROP WITH SECONDARY CAPACITORS

Figure 2 (above)

A similar comparison is made in the above curves as in figure 1 except that it is assumed here that all transformers will be loaded at peak feeder load. In this case, however, it is still true that secondary capacitors do not give additional reduction in voltage drop over primary capacitors unless they are confined to that section of the feeder between points *Y* and *Z*. For either of the conditions assumed in figures 1 or 2 it is necessary properly to set the line-drop compensator to obtain the indicated results

capacity in one or both of two ways. Correction of reactive kilovolt-amperes reduces voltage drop and it also reduces net kilovolt-amperes, so depending on whether voltage drop or thermal capacity is the limiting factor, the value of released system capacity will be calculated in one of two ways.

The kilowatt carrying capacity of overhead primary feeders is practically always limited by voltage drop in the feeder so the advantage of the capacitor in releasing feeder capacity is measured as a direct function of the degree of re-

Figure 3 (below)

These curves show the relative costs of primary and secondary capacitors. The cost per kilovolt-ampere of primary capacitors is about constant since they are always multiples of 10- or 15-kva units. In this study primary 4-kv capacitors had a fixed installed cost of $7.55 per kilovolt-ampere and 13.8-kv capacitors had a fixed installed cost of $12.60 per kilovolt-ampere. The installed cost of secondary capacitors varies considerably as noted above. On secondary networks, however, the cost was fixed at $22.40 per kilovolt-ampere since these were all multiples of 5-kva units

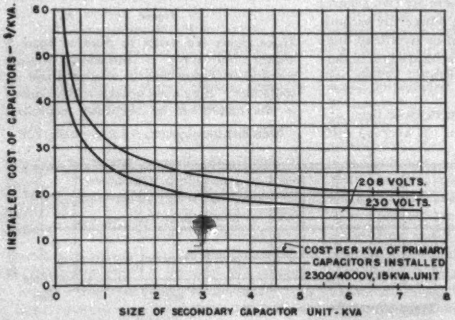
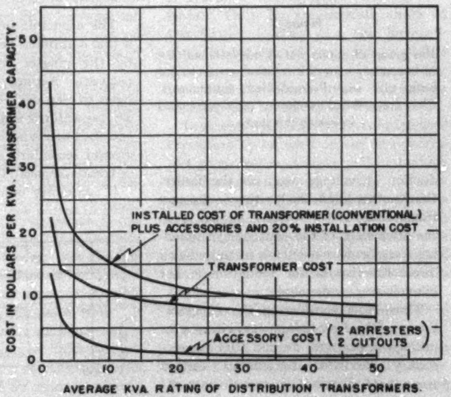


Figure 4

The curves below summarize the unit distribution transformer costs for the conventional type of transformer on overhead lines using two lightning arresters and two fuse cutouts, as used in this study



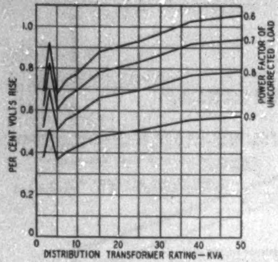


Figure 5

These curves show the amount of voltage rise which secondary capacitors produce in the distribution transformers on overhead lines. It is assumed here that the feeder power factor is corrected to unity and that the ratio of total transformer kilovolt-amperes to feeder kilovolt-ampere demand is two

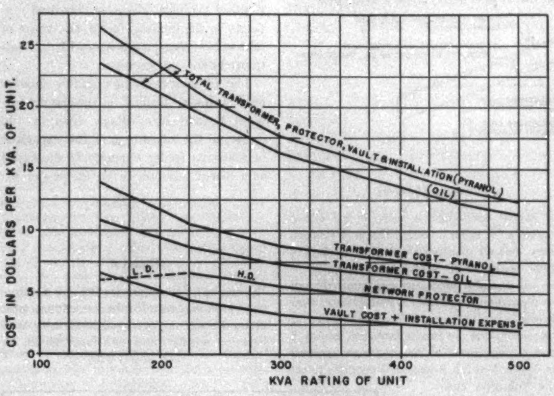


Figure 6

This group of curves shows the installed cost of secondary network transformer vaults for both oil- and Pyranol-filled transformers. Only the oil-filled transformer costs were used in making this study

duction of voltage drop on the feeder. On the other hand, the limiting factor on a transformer is its thermal capacity, so the advantage of the capacitor in releasing transformer capacity is measured as a direct function of the reduction in net kilovolt-ampere demand.

Thus, now considering the comparative merits of secondary capacitors over primary capacitors, we find that the secondary capacitor releases a certain amount of distribution transformer capacity (thermally), which the primary capacitor does not do. In addition to this, the secondary capacitor effects an additional voltage rise through the transformer by virtue of the transformer reactance. This voltage rise, when coordinated with the regulating equipment, in effect, augments the primary feeder capacity, provided it can be obtained fairly equally on all distribution transformers, since it contributes to the net reduction in voltage drop between the first and last customer on the feeder (see detailed analysis hereinafter).

So we find that a given kilovolt-amperes of secondary capacitors provided they are properly distributed, may add an additional increment of primary feeder capacity and releases a certain amount of distribution transformer capacity above and over what the same kilovolt-amperes in primary capacitors can offer.

The economic problem is to determine if this additional benefit is or is not offset by the greater cost of secondary capacitors. Actually, there are two general cases to consider—one where the secondary capacitor is located on overhead lines and the other where it is located on underground secondary networks. These will be treated separately.

If secondary capacitors have been applied in such a way that the average voltage level is higher than could be obtained with primary capacitors there will probably result an increase in revenue which may properly be considered an additional benefit. Although most engineers agree that there will be some increase in revenue with a higher voltage level, there is some difference in opinion as to the amount of increase. In the economic comparison of this study the additional revenue has been evaluated by a method acceptable to most engineers and the results have been prepared alternatively considering the additional revenue as a benefit and ignoring it. The details are given in the appendix.

Important Factors to Consider in Comparing Primary and Secondary Capacitors

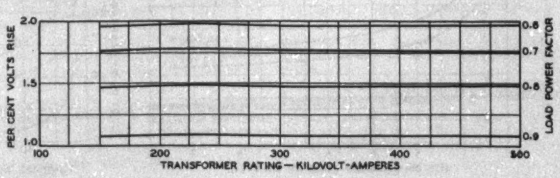
A primary capacitor releases feeder kilowatt capacity because it reduces the drop between the first and last customer. Unless secondary capacitors further reduce this drop they will not contribute additional kilowatt line capacity. Actually if every distribution transformer is equipped with a secondary capacitor the voltage drop between the first and last customer is not affected. This is illustrated schematically in figures 1 and 2, where voltage drops are plotted with both primary and secondary capacitors and considering alternatively that the first transformer may or may not be loaded at peak feeder load. It is assumed (arbitrarily) in these diagrams that each distribution transformer has $2^1/_2$ per cent voltage drop when fully loaded and $1^1/_2$ per cent rise when equipped with secondary capacitors.

On most feeders the allowable voltage to the first customer (in this case assumed to be 104 per cent) determines the amount of boost permitted the feeder regulators at peak load. If a voltage rise of 1.5 per

Figure 7

These curves are similar to those of figure 5 except they apply to secondary network transformers rather than to overhead-line installations. They show voltage rise produced in the transformer by secondary capacitors

Network transformers, 13.8 kv—120/208



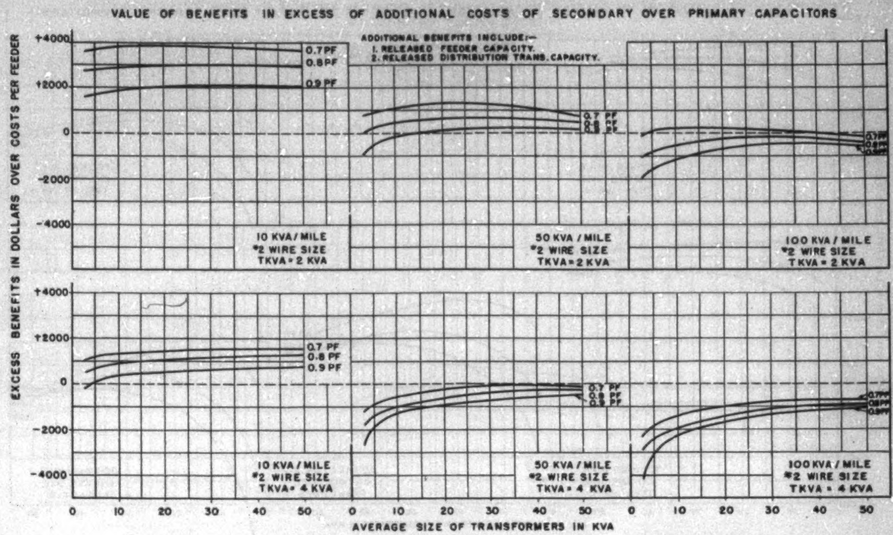


Figure 8

Here are plotted the values of additional benefits over costs of secondary over primary capacitors for overhead lines of a rather low load density—that is, 10, 15, and 100 kva per mile. The dollar values are total capitalized benefits in excess of or less than additional costs of secondary over primary capacitors per feeder. In all cases, any specific set of data plotted here applies to a feeder having such a length and loading as to give exactly 10 per cent voltage drop at peak load

The additional benefits tabulated here include:

1. Released feeder capacity
2. Released distribution transformer capacity

The ratio of transformer kilovolt-amperes to feeder kilovolt-ampere diversified demand was assumed alternatively two and four. The power factors assumed were 0.7, 0.8, and 0.9. The average size of distribution transformers ranged from 5 to 50 kva

It may be noted that for the conditions considered here the secondary capacitors cannot be justified except at the light load densities and low power factors

cent is introduced in each transformer by means of secondary capacitors, in effect the voltage level of the whole feeder has been raised 1.5 per cent; and to avoid giving the first customer 1.5 per cent overvoltage it is necessary to readjust to the regulator to give 1.5 per cent less voltage which leaves the feeder with exactly the same voltage to all customers as before the secondary capacitors were applied. Thus equipping all transformers with their proportionate amount of secondary capacitor correction does not result in any increase in kilowatt feeder capacity over that which can be obtained with the same kilovolt-amperes in primary capacitors.

However, if the secondary capacitors are not applied proportionately to all transformers but only to those between points *Y* and *Z* in figures 1 and 2 (point *Y* is that point at which the feeder voltage drop at peak load is equal to the voltage rise produced in the transformer by the capacitor), there will result in effect a reduction in the voltage drop between the first and last customer, in this case 1.5 per cent, and a corresponding increase in kilowatt capacity of the feeder.

In figures 1 and 2 there is illustrated rather simply the comparative voltages on the primary feeder and transformer secondary for the conditions of *A*—primary capacitors uniformly distributed, *B*—secondary capacitors uniformly distributed, and *C*—secondary capacitors distributed only between points *Y* and *Z* of the feeder. In figure 1 it is assumed that the first transformer may be unloaded at peak feeder load, and in figure 2 it is assumed that all transformers will have their peaks at the same time. It may be noted that under the last assumption there is less voltage difference between first and last customer but with either assumption of transformer loading the secondary capacitors contribute exactly the same amount of improvement. Note also that the only voltage benefit obtained by applying secondary capacitors to all transformers, as indicated in curve *A*, would be a reduction in the necessary voltage range of the regulator.

Figures 1 and 2 demonstrate further that in applying secondary capacitors, careful consideration must be given to the loading of the transformers. If all transformers have their peak loads at about the same time, the results are different than if any particular transformer is unloaded during peak load on the feeder. Only by an accurate study of transformer loading can the line-drop compensator be set properly to obtain maximum benefits from the secondary capacitors and also to avoid overvoltage conditions on specific transformers.

In general the total amount of capacitor correction to be used on the secondaries of a given feeder should equal that which would be used on the primary. This is usually made equal to the average reactive kilovolt-amperes of the diversified feeder load. Any deviation from this rule, plus or minus, results in a partial sacrifice in the maximum reduction in feeder energy losses otherwise obtained.

If the capacitor correction is equal to the average feeder reactive kilovolt-amperes and a proportional amount of it is applied to the secondary of each transformer, because of diversity, the average

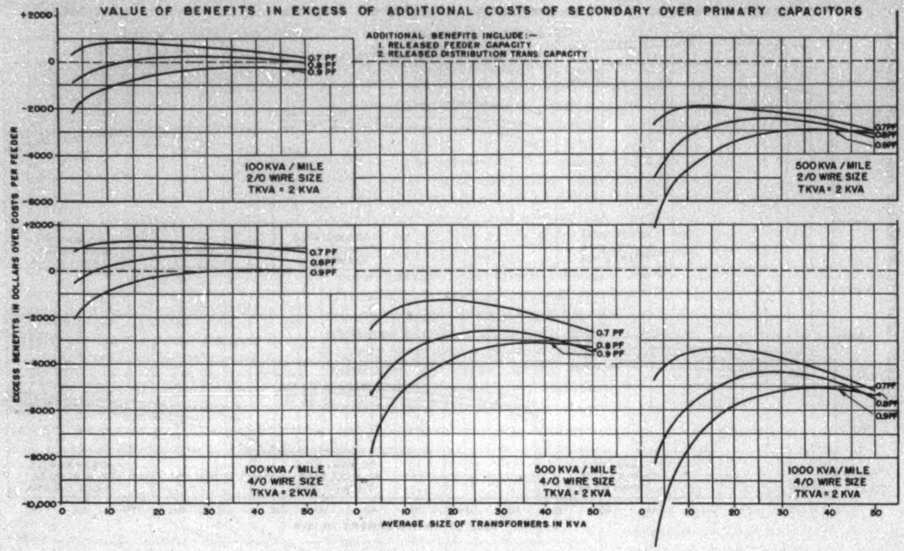


Figure 9

Here are plotted the values of additional benefits over cost of secondary over primary capacitors for overhead lines of somewhat higher load densities than those considered in figure 8. The load densities considered here were 100, 500, and 1,000 kva per mile. In keeping with these densities the conductor sizes assumed were 2/0 and 4/0. The dollar values are total capitalized benefits in excess of or less than additional costs of secondary over primary capacitors per feeder. In all cases any specific data plotted here applies to a feeder having such a length and loading as to give exactly 10 per cent voltage drop at peak load

Here again the additional benefits capitalized include (1) released feeder capacity, and (2) released distribution transformer capacity. The ratio of transformer kilovolt-amperes to diversified feeder demand was two. Here again the average size of distribution transformer was assumed to vary from 5 to 50 kva

Note that under these conditions secondary capacitors cannot be justified, even at the light load density of 100 kva per mile unless the power factor is fairly low

reactive load on any one transformer will only be partially corrected. Moreover, it is important to note that whereas the reactive kilovolt-amperes is reasonably constant on most feeders, this is not at all true of loads on specific transformers.

From the viewpoint of voltage control it is perhaps preferable to distribute the capacitor correction in direct proportion to transformer rating with the first installation located at a point on the feeder at which the peak-load voltage drop is equal in magnitude to the voltage rise produced in the transformer by the secondary capacitor. This procedure would insure uniform voltage rise in all transformers and also would insure the maximum additional feeder kilowatt capacity. On the other hand, with specific transformers having particularly bad power-factor or unusual load conditions, it may be desirable to concentrate more than a proportionate amount of capacitors.

It should be emphasized that unless considerable care and study is given to the application of secondary capacitors, particularly on radial secondaries, the calculated additional benefits will not be realized. In the case of banked secondaries or secondary networks the problem is not so acute, because load diversity is obtained in the network itself, which approaches that obtained in the primary feeder with radial distribution.

Scope of Economic Analysis

To obtain a broad picture of the economic merits of secondary capacitors, it was necessary to consider a great variety of radial overhead feeders as well as secondary networks. Actually 33 overhead lines were analyzed and 24 variations in networks. The variables in circuit design and load conditions were:

Radial Overhead Feeders:

Conductor sizes	numbers 2, 2/0, 4/0
Load density, kilovolt-amperes per mile	10, 50, 100, 500, 1,000
Power factor	0.7, 0.8, 0.9
Average transformer size	$1^1/_2$ kva to 50 kva
Ratio of transformer kilovolt - amperes to total kilovolt-ampere demand	2, 4

In each case the primary voltage was assumed to be 4,160 volts, since this is the voltage rating of the lowest-cost primary capacitor.

Network Systems:

13.8-kv underground cable	4/0, 350,000 circular mils
Load density, kilovolt-amperes per mile	500, 1,000, 3,000, 5,000
Power factor	0.7, 0.8, 0.9
Average transformer size	150 kva to 500 kva
Ratio of transformer kilovolt-amperes to total kilovolt-ampere demand	1.5

The primary voltage was assumed to be 13.8 kv in each case.

In all cases the allowable primary voltage drop on 4.16-kv overhead lines

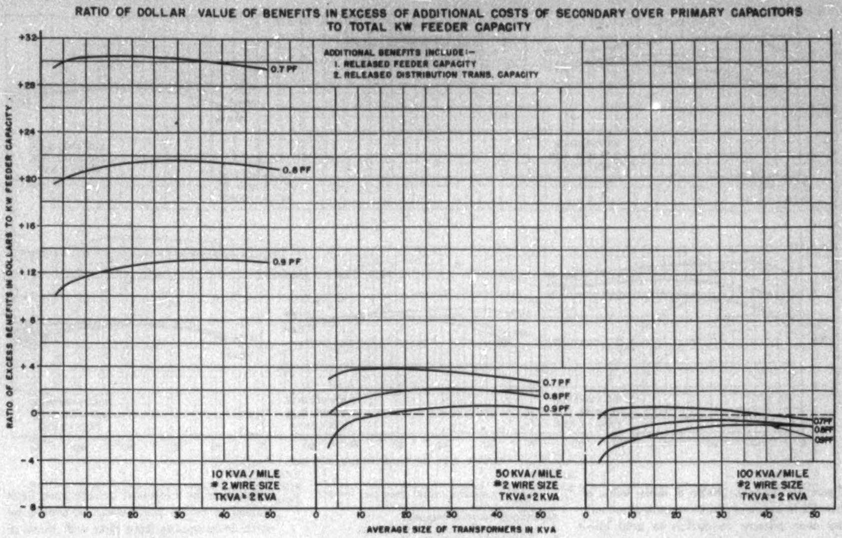


Figure 10 (above)

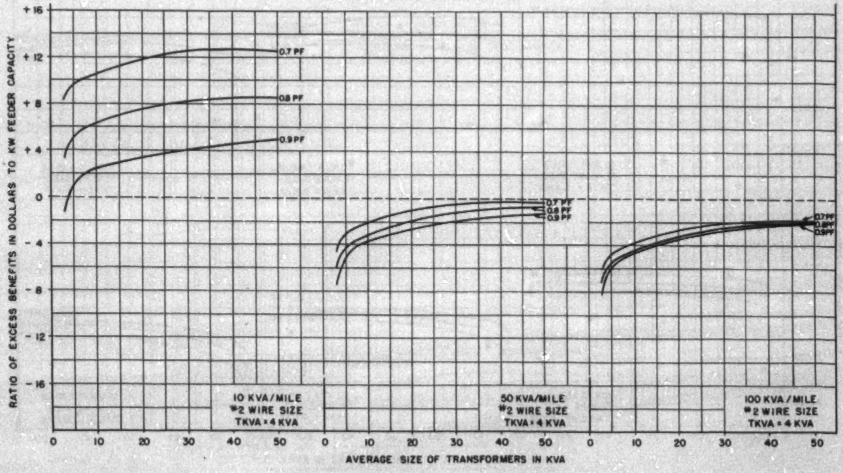
The data plotted in these curves cover similar conditions to that of figure 8 except that the additional benefits of secondary over primary capacitors have been plotted as a ratio of dollars benefit to kilowatt feeder capacity rather than dollar benefit per feeder. These data cover the set of conditions in the upper half of figure 8, that is, number 2 conductor size, load densities of 10, 50, and 100 kva per mile, and a ratio of transformer kilovolt-amperes to feeder demand of two

Figure 11. Ratio of dollar value of benefits in excess of additional costs of secondary over primary conductors to total kilowatt feeder capacity

These curves show the value of additional benefit of secondary over primary capacitors for the condition in figure 8, but the benefit is plotted as a ratio of dollars benefit to feeder kilowatt capacity rather than dollars benefit per feeder. These data cover the same conditions as the lower half of figure 8, that is, number 2 conductor size load densities of 10, 50, and 100 kva per mile, and a ratio of transformer kilovolt-amperes to feeder demand of four

Additional benefits include:

1. Released feeder capacity
2. Released distribution-transformer capacity



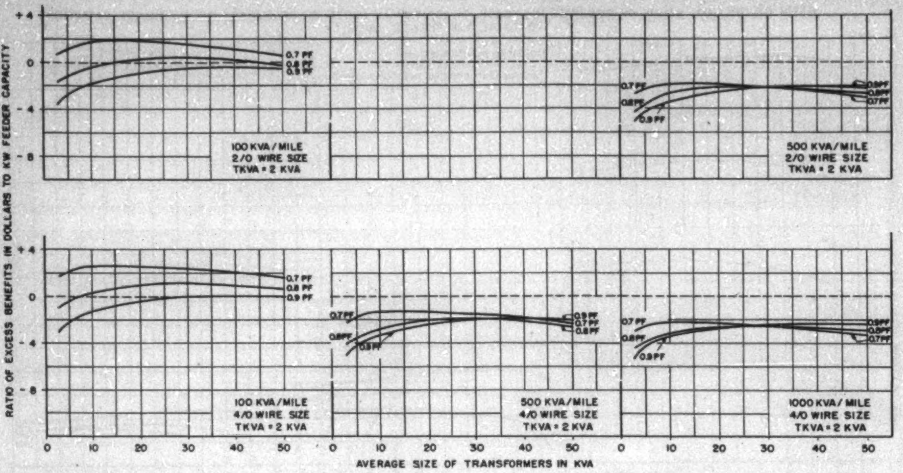


Figure 12 (above). Ratio of dollar value of benefits in excess of additional costs of secondary over primary conductors to total kilowatt feeder capacity

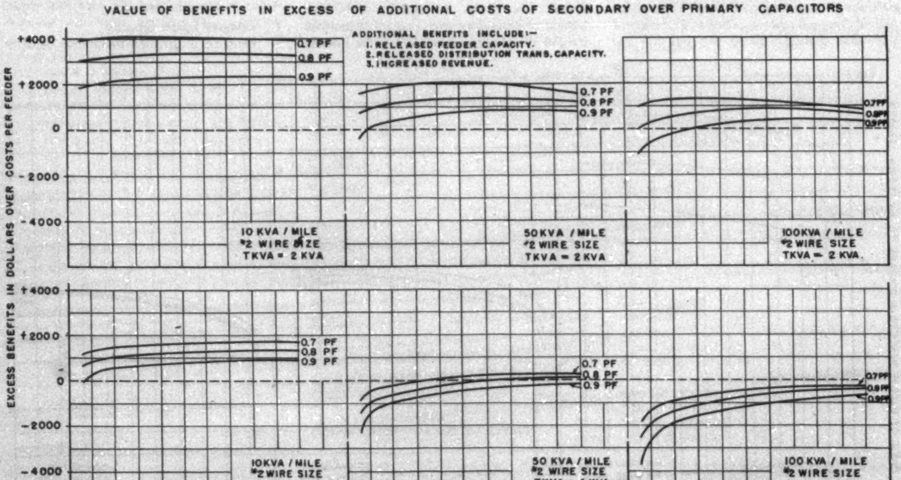
These curves cover the same conditions as those of figure 9, that is, load densities of 100, 500, and 1,000 kva per mile, conductor sizes of 2/0 and 4/0, etc., but (similar to figures 10 and 11) the additional benefits are plotted as dollars benefit per kilowatt of feeder capacity rather than per feeder

Additional benefits include:

1. Released feeder capacity
2. Released distribution-transformer capacity

Figure 13

These curves cover the same set of conditions as curves of figure 8 except that the additional benefit of increased revenue has been added. In this case the additional benefits have been plotted in dollars per feeder. It should be noted in comparing these data with those of figure 8 that the item of additional revenue still leaves the application of secondary capacitors somewhat doubtful except at light load densities. It may be noted, however, that for a ratio of transformer kilovolt-amperes to feeder demand of two, the secondary capacitors appear to somewhat of an advantage at all three of the load densities considered



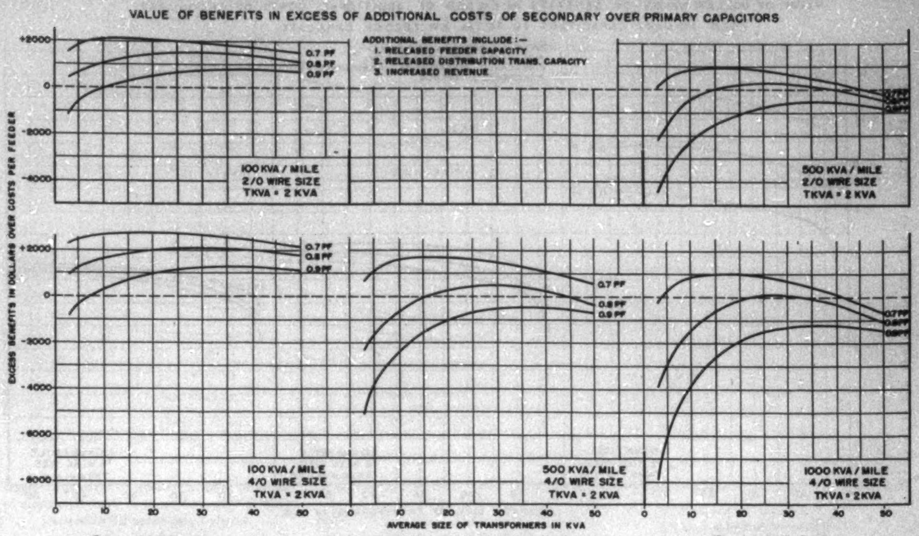
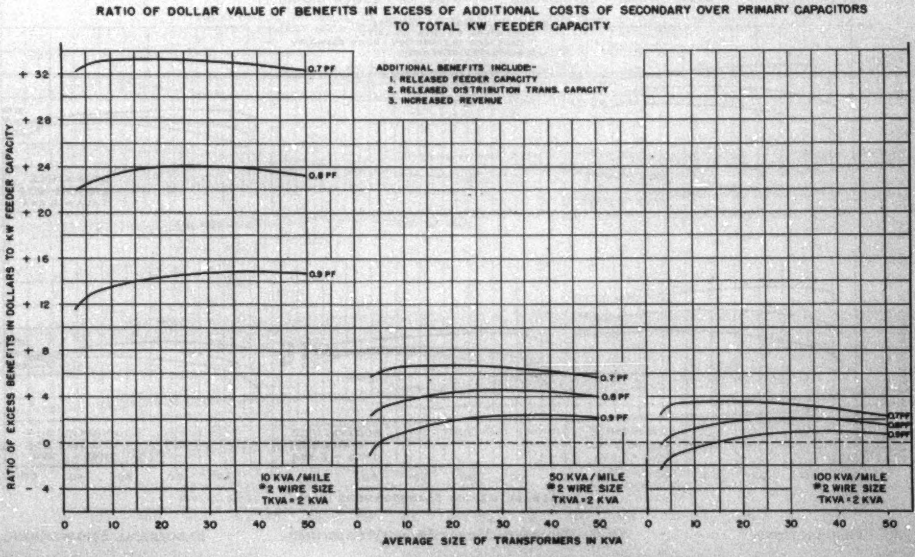


Figure 14 (above)

The data plotted here are entirely similar to those of figure 9 and cover a similar set of conditions, but include the additional benefit of increased revenue. The additional benefits of primary over secondary capacitors have been plotted in this case as dollars benefit per feeder. Here again it will be noted that the item of additional revenue does not greatly change the over-all picture as shown in figure 9. Secondary capacitors here appear to be attractive only at the load density of 100 kva per mile, except at low power factors

Figure 15 (below)

The data plotted here cover a similar set of conditions as those plotted in figure 10, except the additional item of increased revenue has been included in the benefits. As in figure 10 the value of additional benefit of primary over secondary capacitors has been plotted as total dollars benefit per kilowatt of feeder capacity, rather than per feeder



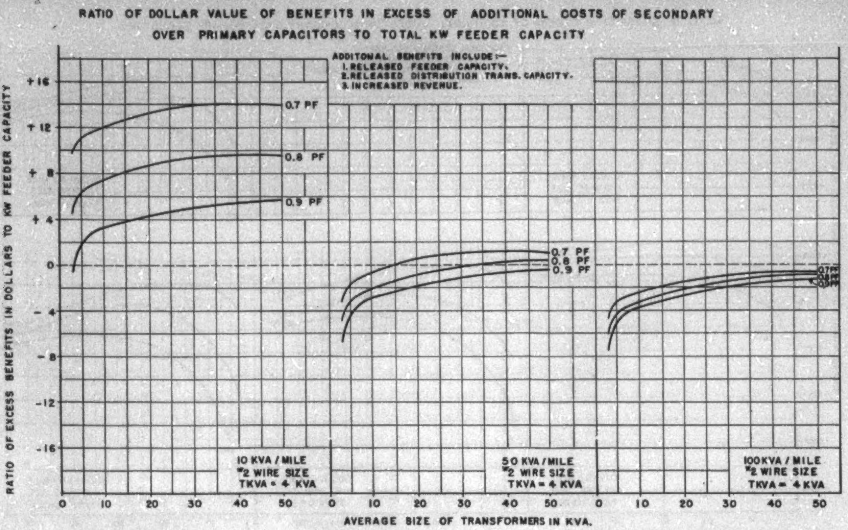
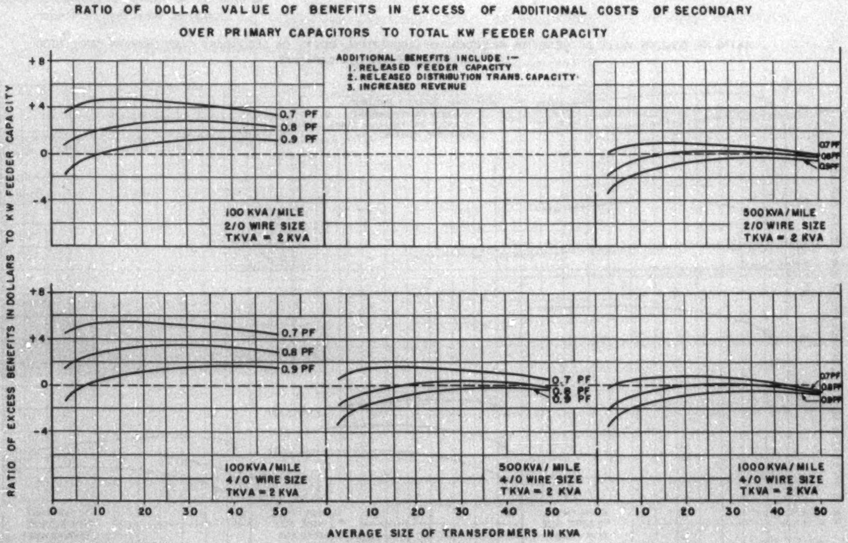


Figure 16 (above)

The data plotted here cover the same set of conditions as figure 11 except that the item of additional revenue has been included as a benefit. As in figure 11, the value of additional benefit of primary over secondary capacitors has been plotted as total dollars benefit per kilowatt of feeder capacity, rather than per feeder

Figure 17

The data plotted here cover the same set of conditions as considered in figure 12 except that the item of additional revenue has been included as an additional benefit. The value of additional benefit of primary over secondary capacitors has been plotted as total dollars benefit per kilowatt of feeder capacity, rather than per feeder



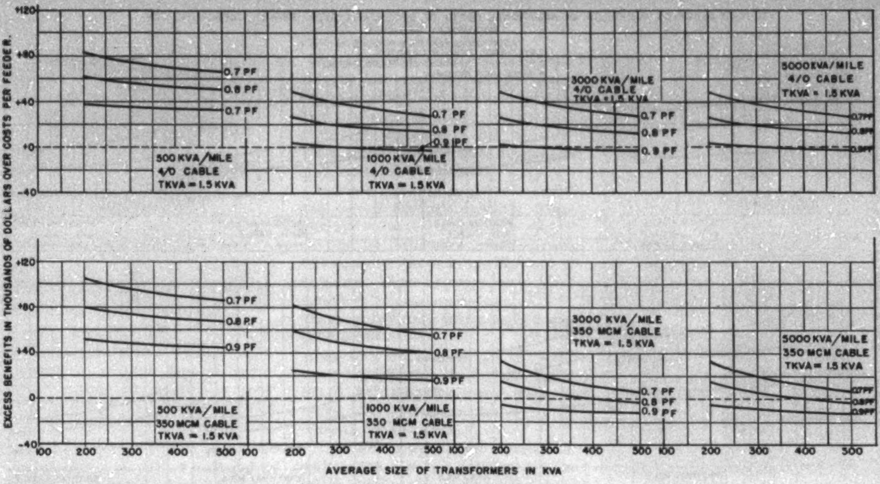


Figure 18 (above). Value of benefits in excess of additional costs of secondary over primary capacitors for secondary network

Figures 8 to 17 have summarized the results of a comparative analysis of primary over secondary capacitors on overhead lines covering a wide range of conditions. The data plotted here are similar to the foregoing but cover the secondary network application. Total additional benefits include (1) released feeder capacity, (2) released distribution transformer capacity, and have been plotted in dollars per feeder. Load densities were assumed to be 500, 1,000, and 3,000 and 5,000 kva per mile. The primary 13-kv cable was assumed to be alternatively 4/0 and 350,000 circular mil. Ratio of transformer kilovolt-amperes to diversified demand was assumed to be 1.5

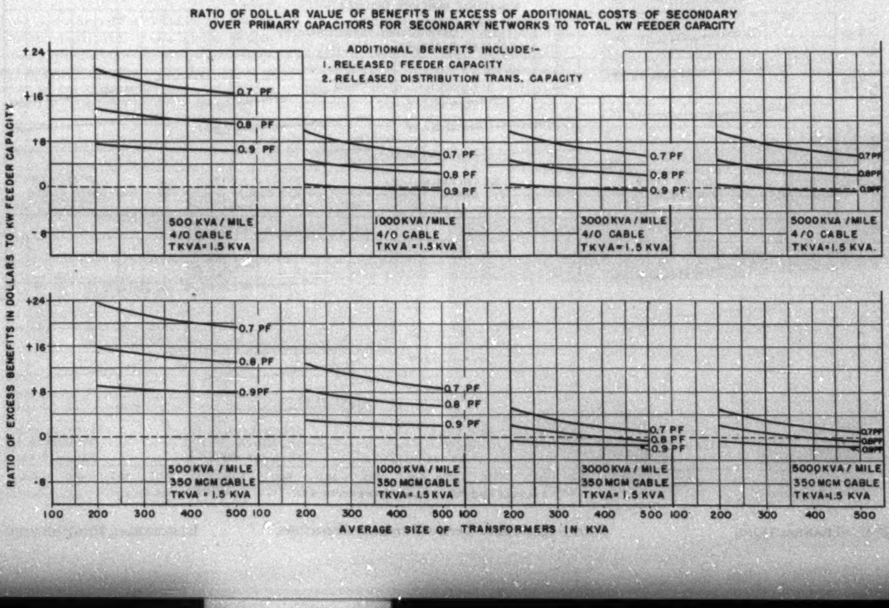
Average size of distribution transformers ranged from 200 to 500 kva. It may be noted that, except at a load density of 500 kva per mile the secondary capacitor appears to be at somewhat of an advantage over the primary capacitor

Additional benefits include:

1. Released feeder capacity
2. Released distribution-transformer capacity

Figure 19

The set of data considered here is the same as that considered in figure 18 except that the value of additional benefit of secondary over primary capacitors has been plotted as dollars benefit per kilowatt of feeder capacity rather than dollars benefit per feeder



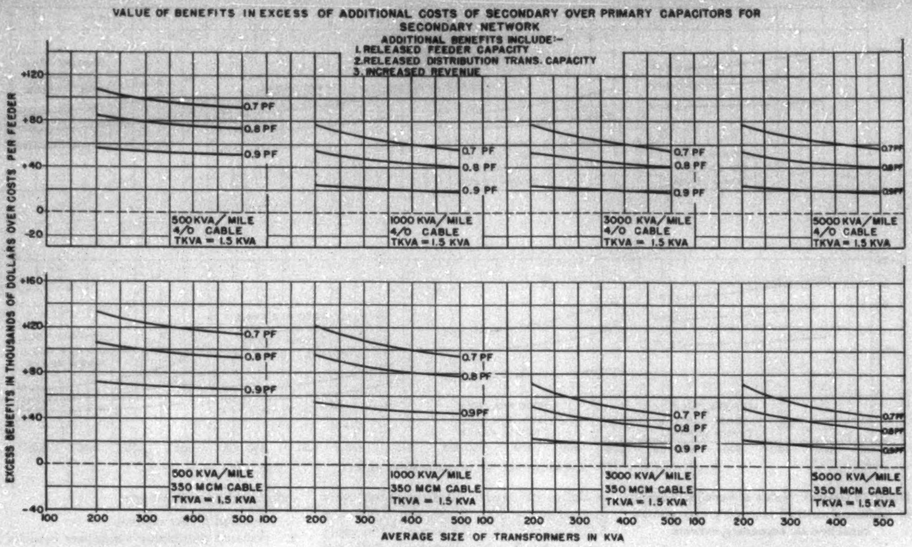


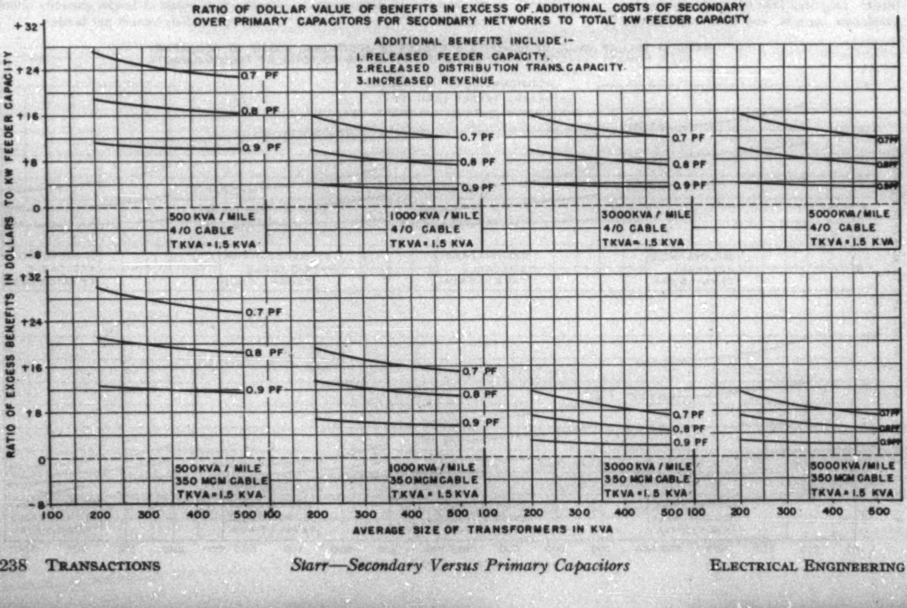
Figure 20 (above)

This set of curves is similar to those of figure 18 except that the item of increased revenue has been included as a benefit. Here the value of additional benefits of secondary over primary capacitors has been plotted in total dollars benefit per feeder

It may be noted that the item of increased revenue increases the total benefit to such an extent that the secondary capacitors appear to be of a considerable advantage in all cases considered over the primary capacitors

Figure 21 (below)

The data plotted here are similar to that of figure 20 except that the item of increased revenue has been included as a benefit. Similar to figure 20 the data here have been plotted as total dollars benefit per kilowatt of feeder capacity



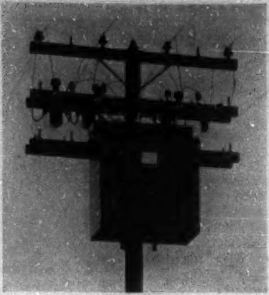


Figure 22

This illustration shows a 180-kva primary line capacitor for urban type of feeders. This type of equipment consists of a number of indoor capacitor units mounted inside a ventilated weatherproof housing

was assumed to be ten per cent and on the 13.8-kv underground secondary network feeders five per cent except where the thermal capacity of the cable would be exceeded. With this criteria for line capacity and the range of load densities used, the overhead lines varied in length from 2.2 miles to 17.8 miles while the underground network feeders varied in length from 3.5 miles to 12.7 miles. Similarly kilovolt-ampere peak loads varied from 173 kva to 2,520 kva on the overhead lines and from 3,500 to 9,000 kva on the network feeders.

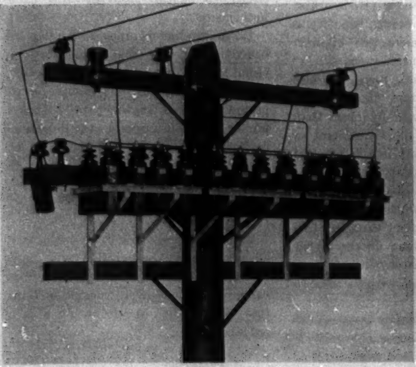


Figure 23

Here is shown another 180-kva primary-line capacitor bank made up of a number of individual outdoor units. This type of installation is functionally identical with that shown in figure 22 but is frequently preferred because it has a somewhat lower installed cost

For each case considered, additional kilowatt line capacity, released transformer capacity, and increased revenue were evaluated in dollars per line and dollars per kilowatt of line capacity. The methods used for these evaluations are tabulated in the appendix.

Figure 3 gives the capacitor installed costs used in making this study. These were approximately selling price plus 20 per cent for installation. On this basis 4-kv capacitors have an installed cost of $7.55 per kilovolt-ampere and 13-kv capacitors have an installed cost of $12.60. The secondary units have a variable cost per kilovolt-ampere, since the unit cost varies with size, but on the secondary network application the cost is fixed at $22.40 per kilovolt-ampere, since all banks of 208-volt capacitors are in increments of 5-kva units.

The value of transformer capacity also varies with size of installation. The values used in this study for the radial overhead line and underground network transformer costs are summarized in figure 4 and figure 6, respectively.

The unit line costs used are:

Three-phase number 2 4-kv overhead—$2,763 per mile
Three-phase number 2/0 4-kv overhead—$3,540 per mile
Three-phase number 4/0 4-kv overhead—$4,450 per mile
Three-phase number 4/0 13.8-kv cable—$17,400 per duct mile
Three-phase 350,000-circular-mil 13.8-kv cable—$20,100 per duct mile

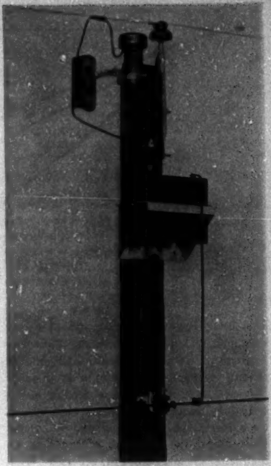


Figure 24

This is a typical installation of a 15-kva primary-line capacitor for single-phase rural service

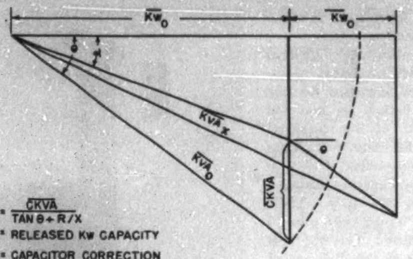
These values are based upon averages of actual installed costs. Their absolute values may be in error for specific localities but their comparative values are accurate. These line costs were used to determine the value of a kilowatt of line capacity in each case.

Examination of Results

In order to obtain any reasonable appraisal of the merits of the secondary capacitor on the distribution system, it was necessary to analyze a fairly wide range of circuit and load conditions. To present adequately the results a rather

Figure 25

This illustration shows a 150-kva three-phase bank of 208-volt capacitors installed in a building vault for reactive kilovolt-ampere correction on a secondary network



$$\overline{Kw}_O = \frac{\overline{CKVA}}{TAN\,\theta + R/X}$$

$\overline{Kw}_O$ = RELEASED Kw CAPACITY
$\overline{CKVA}$ = CAPACITOR CORRECTION
R = LINE RESISTANCE
X = LINE RESISTANCE

Figure 26

The above diagram shows the vector relations existing between the pertinent quantities when capacitors are used to release feeder capacity where voltage drop is the limiting factor. For any particular capacitor having a **kva** rating of **Ckva** the above relation for Δkw_0 is the additional key which can be carried on the line without exceeding the voltage drop on the feeder existing before the capacitors were applied. In the above relation values of **kva**, **kw**, or **Ckva** may be expressed either as such or as percentages

large number of curves were necessary since five variables were involved—that is, power factor, load density, conductor size, diversity, and average transformer size. Moreover, it seemed necessary to prepare the results both considering and not considering increased revenue as a benefit since this item is controversial.

To show clearly when the secondary capacitor is to be preferred, it is necessary only to plot the difference in dollars between the capital value of the additional benefits which secondary capacitors have over primary capacitors and the capital value of the additional costs of secondary over primary capacitors. If the additional benefits exceed the additional costs, the secondary capacitor is a sound investment.

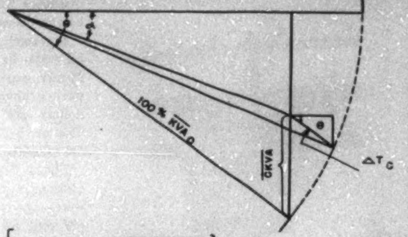
In figures 8 to 21, therefore, this difference between additional benefits and additional costs has been plotted as a function of the various circuit and load conditions. It has been plotted, first in dollars per feeder, and second, in dollars per kilowatt of feeder capacity. It seemed advisable to present the picture both ways since the feeders varied considerably in length and loading, they being alike only in maximum allowable voltage drop.

In examining figures 8 to 21, note that the dollar benefits or deficiency consist of the sum of the following items:

1. Capital value of additional distribution transformer capacity.

2. Capital value of additional feeder capacity.

3. Capital value of additional revenue (capitalized on the basis of 12.5 per cent annual carrying charge).



$$\overline{\Delta T_C} = \left[\left(\frac{\overline{CKVA}}{\overline{KVA}_O} SIN\,\theta - 1\right) + \sqrt{1 - \frac{\overline{CKVA}}{\overline{KVA}_O}^2 COS^2 O}\,\right] \overline{KVA}_O$$

$\overline{\Delta T_C}$ = RELEASED KVA CAPACITY
$\overline{CKVA}$ = CAPACITOR KVA CORRECTION
$\overline{KVA}$ = NOMINAL KVA RATING

Figure 27

The above diagram shows the vector relations existing between the pertinent quantities when capacitors are used to release capacity when kilovolt-ampere demand is the limiting factor. For any capacitor correction having a rating of **Ckva**, the above relation for ΔT_c is the additional kilovolt-ampere which can be carried at the initial load power factor without exceeding the nominal 100 per cent **kva**. In the above relation the values of **kva**, or **Ckva** may be expressed either as such or as percentages

As stated above, the data were also calculated omitting the third item of revenue. Figures 8 to 17 cover the overhead lines studied, and figures 18 to 21 cover underground secondary networks. Figures 13, 14, 15, 16, 17, 20, and 21 include the item of additional revenue as a benefit and the others omit it.

Consider the comparative effect of the several variables individually. First consider the network versus the overhead line. With a few minor exceptions all networks favor the secondary capacitor whether the item of revenue is allowed or not. With many of the overhead lines, on the other hand, the secondary capacitor cannot be justified. The reasons for this are more or less evident. The value of released transformer capacity in a secondary network, in most cases, is worth considerably more than transformer capacity on poles. Also underground primary feeder capacity is worth considerably more per kilowatt than overhead lines in many cases. Accentuating these effects there are also the contributing factors that 13.8-kv capacitors cost substantially more than do 4-kv units. Also the lowest cost of all secondary capacitors is the five-kva unit used on the secondary network (see figure 3).

In virtually all cases the lower power factors favor the secondary capacitor to a much greater extent than do the higher power factors. This is particularly true at light load densities and with the smaller conductor sizes.

The effect of load density is especially interesting. On the overhead lines, it is only at the very light load densities that secondary capacitors can be justified, in many cases even though additional revenue is allowed. In the case of the network, while secondary unit nearly always is justified, the maximum benefits likewise occur at the lighter load densities.

The influence of transformer size varies. On overhead lines, in many cases the additional benefits increase with size of transformer, while in other cases there is an optimum size varying from 10 to 30 kva. With the networks the value of additional benefits decrease with transformer size.

Large conductors appear to offer less benefits than do small conductors on radial feeders, but on the network feeders the reverse is true. As would be expected, the greater the ratio of transformer kilovolt-amperes to diversified peak kilovolt-ampere demand, the less attractive are secondary capacitors.

Conclusions

While the results of this study, as in all economic studies of this type, have greater comparative value than absolute value when applying to specific systems, they do establish the following general conclusions:

1. On underground secondary networks,

particularly those served from primary voltages of 11-kv or greater, the secondary capacitor appears to have an important field of application.

2. On conventional overhead systems, particularly those having primary voltages of 6,900 volts or less, the secondary capacitor has a very limited field of application. It is doubtful if secondary capacitors can be proved in on any overhead line except in cases of rather low power factor. However, on overhead systems incorporating banked secondaries, since many of the inherent advantages of secondary capacitors on low-voltage networks are duplicated, the secondary capacitor in many cases may easily be proved in.

3. Low power factors establish conditions on any distribution system—network or radial—which strongly favor secondary capacitors.

4. Secondary capacitors must be applied with considerable care—much more so than primary capacitors—if the hypothetical calculated benefits are to be realized. Accurate setting of the contact-making voltmeter and the compensator together with proper location and size of the secondary units are very essential to obtain the calculated additional benefits.

5. If economic analysis shows that a secondary capacitor adjacent to a distribution transformer cannot be justified, only a cursory analysis is necessary to show that, under the same circuit and load conditions, a large number of smaller units adjacent to meter boxes likewise cannot be justified. This is owing to the rapid increase in cost per kilovolt-ampere as the size of the unit comes down (see figure 3). It is somewhat doubtful under any circuit or load conditions if a plan incorporating a number of small units located at meter boxes could ever be justified.

Appendix

Releasing System Capacity With Capacitors Where Voltage Drop Is the Limiting Factor

The kilowatt carrying capacity of any part of a system, such as a distribution feeder, where the limitation on capacity is voltage drop, is:

$$\boldsymbol{kw}_0 = \frac{10\ \boldsymbol{kv}^2\ v}{R + X \tan\theta} \tag{1}$$

v = allowable per cent volts drop
R = total resistance in ohms
X = total reactance in ohms
θ = uncorrected power factor angle
$\boldsymbol{kv}$ = line kilovolts
$\boldsymbol{kw}_0$ = kilowatt capacity

If the allowable drop v is increased by an amount Δ_v per cent for one reason or another, the increase in kilowatt capacity is

$$\Delta \boldsymbol{kw}_0 = \frac{10\ \boldsymbol{kv}^2\ \Delta v}{R + X \tan\theta} \text{(refer to figure 26)} \tag{2}$$

If a primary capacitor is applied at the end of the line (or at a distance corresponding to R and X), the per cent voltage rise due to the capacitor is

$$\Delta v = \frac{\boldsymbol{Ckva}\ X}{10\ \boldsymbol{kv}^2} \tag{3}$$

$\boldsymbol{Ckva}$ = kilovolt-amperes of capacitor

and the resultant kilowatt capacity of the line is:

$$\boldsymbol{kw}_x = \frac{10\ \boldsymbol{kv}^2\,(v + \Delta v)}{X \tan\theta_0 + R} = \frac{10\ \boldsymbol{kv}^2\ v + X\ \boldsymbol{Ckva}}{X \tan\theta_0 + R} \tag{4}$$

A specified amount of primary capacitor correction gives a definite voltage rise on the feeder. If this same kilovolt-amperes of primary correction is applied to the secondary side of the distribution transformers and if it is uniformly distributed in proportion to the distribution transformer capacity, there will be an additional per cent voltage rise due to the transformer reactance of

$$\Delta v_t = X_t \frac{\boldsymbol{Ckva}}{\boldsymbol{Tkva}} \tag{5}$$

X_t = transformer reactance in per cent
$\boldsymbol{Ckva}$ = total capacitor correction in kilovolt-amperes
$\boldsymbol{Tkva}$ = total installed transformer kilovolt-amperes

This additional voltage rise is not effective in increasing the line kilowatt capacity, however, unless it is confined to that portion of the feeder beyond the transformer location at which the feeder voltage drop is equal to the voltage rise in the transformer effected by the capacitor. If this method of applying the secondary unit is adopted, the additional voltage rise will be:

$$\Delta v_t = X_t \frac{\boldsymbol{Ckva}}{(\boldsymbol{Tkva})K} \tag{6}$$

where K is defined by the relation

$$K^2 - K + \frac{(\boldsymbol{Ckva})}{(\boldsymbol{Tkva})} \frac{X_t}{10} = 0 \tag{7}$$

The additional increase in primary line kilowatt capacity accruing from secondary over primary capacitors is therefore:

$$\Delta \boldsymbol{kw}_t = \frac{20\ \boldsymbol{kv}^2\ \boldsymbol{Ckva}\ X_t}{\boldsymbol{Tkva}\ K(\tan\theta + R/X)X} \tag{8}$$

If the peak diversified demand on the feeder is $\boldsymbol{Kva}_0$ before correction, then

$$\boldsymbol{Tkva} = n\ \boldsymbol{Kva}_0 \tag{9}$$

where n is the ratio of installed transformer capacity to diversified demand. Also:

$$\boldsymbol{Ckva} = \boldsymbol{Rkva} \tag{10}$$

where $\boldsymbol{Rkva}$ is the average reactive kilovolt-amperes on the feeder during a 24-hour period.

Figures 5 and 7 are plots of equation 5 for transformers on overhead lines and secondary networks, respectively. These curves show the voltage rise effected by capacitors for various amounts of capacitor correction. It is assumed in each case that the feeder power factor has been corrected to unity and that $n = 2$, a fairly representative value for most distribution feeders. It may be noted that the maximum voltage rise is about one per cent for transformers on overhead lines and about two per cent for transformers in secondary networks.

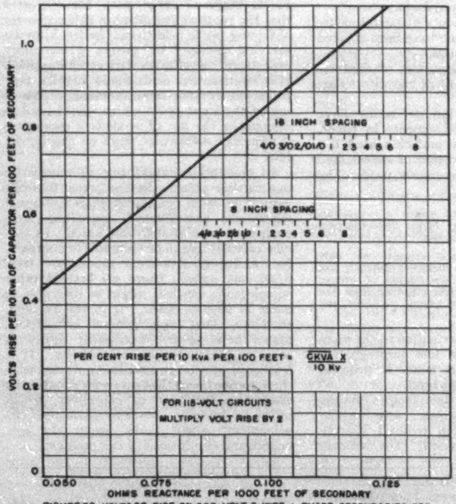


FIGURE 28 VOLTAGE RISE ON 230-VOLT, 3-WIRE, 1-PHASE, SECONDARIES PER 10 KVA OF 230-VOLT CAPACITOR

Figure 28

This curve shows the voltage rise on 230-volt three-wire single-phase secondaries per ten kva of 230-volt capacitor. For 115-volt secondaries the voltage rise should be multiplied by two

Releasing System Capacity With Capacitors Where Temperature Rise Is the Limiting Factor

The capacity of generators, transformers, etc., is usually limited by temperature rise, that is, kilovolt-ampere demand, rather than by voltage drop. Figures 26 and 27 illustrate the difference in these two limiting factors vectorially. In figure 26, Δkw_0 is the additional kilowatts that can be carried after adding capacitor correction **Ckva**, with exactly the same voltage drop as obtained before correction. Δkw_0 is always assumed to be added at the uncorrected power factor angle, θ. In figure 27 Δkva is the additional kilovolt-amperes at power factor θ which can be added after correction without the net resultant kilovolt-amperes exceeding the initial.

In general, for a given amount of capacitor correction, the released capacity, where voltage drop is the limiting factor, usually substantially exceeds that where kilovolt-ampere demand is the limiting factor.

In figure 26,

$$\Delta kw_0 = \frac{Ckva}{\tan\theta + R/X} \qquad (11)$$

The values R and X are resistance and reactance from the substation to any particular capacitor installation having a value **Ckva** and Δkw_0 is the released capacity for that particular installation: or R and X may be considered the resistance and reactance to the load center if **Ckva** equals the total capacitor correction. In equation 11, Δkw_0 and **Ckva** may be expressed either as kilowatts and kilovolt-amperes or as per cent.

In figure 27,

$$\Delta Kva = \left[\left(\frac{(Ckva)}{(Tkva)K}\sin\theta - 1\right) + \sqrt{1 - \frac{(Ckva)^2}{(Tkva)^2K}\cos^2\theta}\,\right] Tkva\,K \qquad (12)$$

Ckva = capacitor correction
Kva = released kilovolt-ampere capacity
Tkva = transformer kilovolt-ampere capacity
θ = uncorrected power-factor angle

On a percentage basis equation 12 becomes

$$\Delta T_c = (C\sin\theta - 100) + \sqrt{100^2 - C^2\cos^2\theta} \qquad (13)$$

ΔT_c = per cent released capacity
C = per cent capacitor correction
θ = uncorrected power-factor angle

In equation 12, ΔT_c and C are percentages of the kilovolt-ampere capacity of the transformer receiving correction. Equation 13 obviously applies to any size of transformer, substation, etc.

The value ΔT_c may also be calculated as a function of power-factor angles before and after correction:

$$\Delta T_c = -100\cos\theta[\cos\theta + \sin\theta\tan\alpha] + 100\cos^2\theta\sqrt{1 + 2\tan\theta\tan\alpha - \tan^2\alpha + \frac{\tan^2\theta}{\cos^2\theta}} \qquad (14)$$

θ = power-factor angle before correction
α = power-factor angle after correction

INCREASE IN REVENUE

The increase in kilowatt-hour output effected by the voltage rise as calculated in equation 6 is usually assumed to increase as the 1.6 power of increase in average voltage level over a 24-hour period. On this basis the additional kilowatt-hours due to secondary capacitors as compared with primary capacitors will be

$$\Delta kw\text{-}hr = \left[\left(\frac{\frac{(Ckva)}{(Tkva)K}\frac{X_t}{2} + 100}{100}\right)^{1.6} - 1\right] \times kw_0 \times \text{L.F.} \times 8{,}760 \qquad (15)$$

kw_0 = peak feeder kilowatts
L.F. = load factor

This additional kilowatt-hour output is usually evaluated at $0.02 per kilowatt-hour and capitalized at 12.5 per cent per annum.

Discussion

J. D. Stacy (General Electric Company, Pittsfield, Mass.): Mr. Starr's excellent paper re-emphasizes the extensive application for capacitors on central station systems. It is interesting to note that a large number of utilities throughout the country have applied capacitors with considerable savings—

1. Because of improvement of voltage conditions which has deferred installation of new feeders, feeder positions, cable, etc.
2. Because of release of substation capacity.
3. Due to reduced losses.

This extensive application has been the result of several important factors:

First, the improvements in capacitor design and reliability which have been effected during the last 10–12 years.

Secondly, the simultaneous marked reductions in installed cost of capacitors during the same period.

Capacitor design improvements have covered not only improvements in paper, foil, and the other solid materials used, but principally the substitution of chlorinated diphenyl for the oil previously used. The new liquid has:

(*a*) Reduced the size and weight of capacitors because of its considerably higher dielectric constant compared with that of oil.

(*b*) Increased the dielectric strength not only because of its own higher dielectric strength, but because of the improved distribution of voltage stress through the dielectric resulting from the fact that the dielectric constant of liquid and cellulose are practically the same.

In addition, careful attention to the process of vacuum drying and impregnation has been a considerable factor in bringing quality to its present high level.

As a consequence of these factors and the standardization of ratings, capacitor installed costs have been reduced sharply during the last ten years. The average cost per kilovolt-ampere of capacitors of all voltages is now approximately 36 per cent of that in effect in 1929—a reduction of 64 per cent.

L. M. Olmsted (Duquesne Light Company, Pittsburgh, Pa.): Mr. Starr's paper very ably indicates the field in which secondary shunt capacitors are more attractive economically than primary capacitors. In order to secure the utmost advantage for the secondary capacitors, he has assumed that the capacitors will be used only on transformers far enough from the feeding point that the rise in voltage caused by the capacitive current will not raise the secondary voltage above that of the transformers at the feeding point. In this way he makes effective the additional voltage correction through the reactance of the more remote transformers to offset primary-circuit voltage drop, thereby increasing the load-carrying ability of the primary circuit beyond that possible with the same capacity in primary capacitors.

While such a practice is workable, it would require continual supervision to keep it in proper relationship to the system changes which invariably accompany circuit extensions and load growth. This additional supervision would be a disadvantage, tending to reduce possible economies, particularly on the usual overhead radial system, with numerous small transformers. Underground systems, having larger transformers, generally receive more careful supervision and probably would have the capacitors relocated as required. It is fortunate, therefore, that the field of economic application for secondary capacitors is limited mostly to underground systems.

F. M. Starr: I agree with Mr. Olmsted that the method described in this paper for obtaining some additional feeder kilowatt carrying capacity by means of secondary capacitors, necessitates very careful application engineering, and would require continual supervision and checking. This is one of the intangibles which would tend to discourage the use of secondary capacitors if economic advantage were on the border line.

In closing, I should like to emphasize one further point: In the paper 220-volt capacitors were compared with 4,000-volt capacitors, and 208-volt capacitors were compared with 13,000-volt units. In specific cases it might be desirable to compare, for example, 440-volt capacitors with 6,900-volt units. There are several other comparisons which it might be desirable to make in specific cases. It is well to note, therefore, that any general conclusions drawn here do not necessarily apply in some of these specific comparisons. It is further of interest to note in connection with these comparisons that any very substantial change in the price of capacitors, which might be brought about by some new development, would again change the economics and conclusions of this problem.

Considerations in Applying Ratio Differential Relays for Bus Protection

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THE PROBLEM of bus differential protection is receiving increased attention by relay engineers because of the serious consequences of a bus fault. Several cases of bus faults have recently occurred indicating that regardless of care used in the design of structures, some unexpected failure or accident may cause a disastrous outage. In spite of the many improvements that have been made in the protection of transmission lines in recent years, little attention has been given to the protection of station busses. This has been due partly to the impression that they were not particularly subject to faults and partly to the feeling that the protective relays would do more harm than good through incorrect tripping. Recent experiences, however, indicate that a bus fault, though of infrequent occurrence, can cause much greater damage and a longer outage period than many transmission line faults. Also, high-grade performance has been obtained when a bus protective scheme is properly installed with due consideration given to the many factors which might lead to faulty operation. The purpose of this paper is to discuss some of the important factors to be considered in bus differential protection and the operating characteristics and results of a field test on a new bus differential relay.

The differential relaying scheme for protecting a station bus does not differ in principle from that which has long been used for the protection of a generator or transformer bank. That is, current transformers are located in all connections to the bus and a summation of all the secondary currents is made (figure 1). Under load conditions or for an external fault the summation of all the currents should equal zero, and, therefore, no current should appear in the operating winding of any relay connected across that circuit. For an internal fault, assuming faithful current-transformer response, the total amount of the short-circuit current should appear in the operating winding of the protective relay.

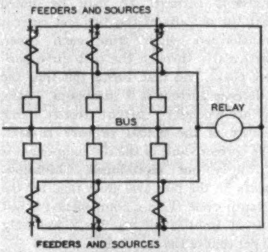

Figure 1. Differential protection for a six-circuit bus using an overcurrent differential relay

Current-Transformer Performance

A-C Saturation

In the case of a fault occurring external to the differential zone, the operating coil of the relay would receive no current whatsoever, if the performance of the current transformers was perfect. Actually, current often does appear in the operating coil of the relay upon the occurrence of external faults because of unfaithful transformer performance. Any one or more of the current transformers involved may be affected by a-c saturation because of insufficient iron in the current transformer core, an insufficient number of secondary turns, or excess burden in the secondary circuit for the magnitude of current involved. In general, the effect of a-c saturation has not been found to be particularly bad for differential relay protection.

D-C Saturation

A second and more important factor affecting current-transformer performance, which has not been fully appreciated, is that arising from the effect of the d-c component of asymmetrical faults. The introduction of faster operating differential relays has brought this problem to the front, since with the slower operating relays formerly used, the effects of the d-c transient were over before tripping could occur. The effect of the d-c component of an asymmetrical current wave is such as to aggravate the saturation of current transformers to a far greater degree than the corresponding a-c component. The effect of this d-c component on the current transformer is to magnetize the current transformer core with a unidirectional flux which thus gives rise to the term d-c saturation of current transformers. The degree of this effect depends upon the d-c time constant of the primary circuit, the resistance and inductance of the secondary circuit of the current transformer affected, the amount and magnetic characteristics of the core, the ratio of the current transformer, and the magnitude of the primary current. The degree of d-c saturation is greater for the longer d-c time constants. This makes the problem more difficult, particularly for the protection of generator busses, since the time constant for a large generator may be as great as 0.3 seconds. High-voltage busses will, in general, have a time constant less than 0.3, ranging in value from approximately 0.13 down to 0.05, or even less if the bus is located a considerable distance from the generating station.

Matched Current Transformers

On first thought it would seem practical to insure adequate performance of current transformers for differential protection by making all of the current transformers alike. However, it is found that even with standard current transformers built to the same specifications, there still remains some difference in their perform-

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Paper number 39-31, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 12, 1938; made available for preprinting December 29, 1938.

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1. For all numbered references, see list at end of paper.

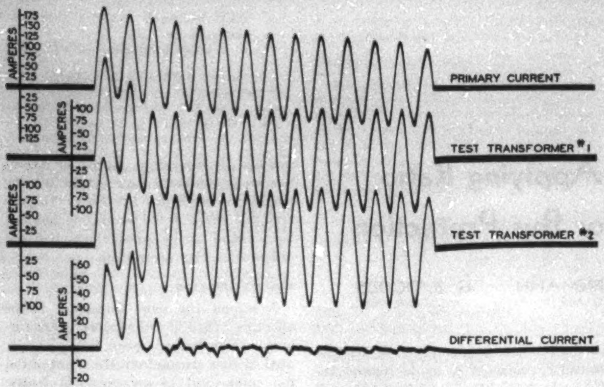

Figure 2. Oscillogram showing effect of d-c saturation

ance when subjected to asymmetrical short circuits. This is because their saturation curves will not match exactly at the high values of d-c excitation involved. Another reason why duplicate standard current transformers used throughout will not always give satisfactory performance is because the total short-circuit current may flow out to an external fault through one current transformer, but may be divided up among several current transformers in the leads to the bus from the various sources. For example, with a generator bus having two generators and two bus-tie breakers, an external fault of 60,000 amperes on a feeder may be divided up into 20,000 amperes feed-in through each bus-tie breaker and 10,000 amperes from each generator on the bus section. Obviously the current transformer in the outgoing feeder which must carry 60,000 amperes will be operating on a different portion of the saturation curve than the current transformers on the incoming leads. Therefore, unless the current transformer in the feeder circuit has been designed so that it will not saturate with an asymmetrical short circuit of 60,000 amperes, a difference or operating current is bound to occur for this type of fault in spite of any effort made to match the current transformer characteristics for symmetrical currents.

A more complete discussion of the effect of d-c transients on current transformer performance is available elsewhere.[1] However, no simple method has yet been made available which can be used to determine accurately the magnitude and duration of the difference in current transformer output for these conditions.

Figure 2 shows an actual test film in which two differentially-connected transformers were caused to saturate by d-c transient current. It may be noted that the outputs of the two test current transformers differ from each other, particularly through the first three positive peaks, and that during this time the difference current is a maximum. Also, neither secondary current records a true picture of the actual primary current. The correct value of the d-c component is obviously not reproduced. Measurements on the film also show that for the seventh cycle the a-c component for test current transformer number 2 was only 83 per cent of the true value.

Blocking Resistors

One means of controlling the amount of difference current which may flow is by inserting a definite amount of fixed resistance in the difference or operating coil circuit. A well-accepted practice for improving the performance of differential protection has been the reduction and balancing of lead impedance in the secondary circuit and the increase of impedance in series with the differential relay. For example, many operating companies make a practice of running all current-transformer secondary leads in the differential circuit to a bus out in the switch yard, when only a single-element overcurrent relay is used, and then bringing to the relay only the necessary leads. This results in a minimum of secondary lead resistance to each current transformer secondary and does increase the burden in the differential circuit by the extent of the lead resistance to the relay. Several recent installations have extended this principle by the addition of a resistance in series with the operating winding of the differential relay in order to decrease the current through this path in case of an external short circuit. Recent tests have indicated that with ratio differential relays some discretion must be observed in the magnitude of resistance to be introduced in the differential circuit or failure to trip for an internal fault might result. This comes about from the fact that the secondary voltage of the current transformers required to force the differential current through the excess burden may reach a high value. As a result, the poorer current transformer may not be able to develop this voltage and is subjected to a reversal of current through its secondary. In other words, one current transformer may actually have its secondary current reversed so that a differential relay having restraint windings wound on the same magnetic circuit would not have the restraint torque canceled out, as should occur on an internal fault with this type of relay. Therefore, a failure to trip might easily occur, if the impedance in series with the operating winding is too high.

Figure 3 illustrates a test condition wherein too much impedance was used in the differential circuit for an internal fault condition. The polarity of connections was such that had both current transformers performed correctly, the loops of the current wave of current transformer number 1 would have dove-tailed with the primary current wave, while the wave for current transformer number 2 would have been exactly opposite in phase. For the first two cycles, current transformer number 1 reversed the current through current transformer number 2 without excessive wave-form distortion. At the end of two cycles, it will be noted that current transformer number 2 takes control of the circuit, reversing the current through current transformer number 1, accompanied by a severe distortion of wave form.

Without saturation, the output of the two test current transformers should have been 126 amperes each, as determined from their ratio and the magnitude of the primary current. The differential or operating coil current, then, should have been 252 amperes. An approximation of the actual currents flowing at the fifth cycle gives values of 61 amperes for current transformer number 1, 75 amperes for current transformer number 2, and 17 amperes for the difference current. This means that a 50 per cent ratio differential relay would not have operated for this test, since the ratio of the operating coil current to the smaller restraining coil current is only 28 per cent [(17/61) (100) = 28 per cent].

Differential Relay Schemes

The fact that there are many successful bus differential relays in service at the present time, which are giving a relatively high degree of protection, does not indicate necessarily that difficulties will not be encountered on other systems or on these systems when future changes are made. It is also possible that on some of these installations an improper operation may yet occur because of the proper combination of circumstances. Otherwise, the satisfactory performance of existing installations has resulted from either a sufficiently high current setting or a long enough time to allow the transient to subside. Either of these factors tends to make a simple overcurrent relay satisfactory. There have been cases of false operation on a through fault involving d-c transient in which it is recognized that one or the other of these factors was not sufficiently high.

The magnitude of a phase-to-ground fault where a limited current exists often determines the setting of a differential relay. In such an application the use of a restrained or ratio differential relay would lessen the tendency for incorrect relay operation for heavy through faults. Of course, as pointed out elsewhere,[3] sufficient restraining windings must be provided so that on an external fault, short-circuit current must pass through at least one restraining winding. That is, current transformers must not be paralleled in such a manner that on a through fault on one of these circuits current would circulate between the secondaries of the two paralleled current tranformers. Under this condition only the difference in the outputs of the two current transformers would flow through a restraining winding and the operating winding in series, and the relay would operate as a sensitive overcurrent relay.

MULTIPLE RESTRAINT RELAYS

Schemes have been proposed for obtaining a restraining winding for each circuit connected to a bus. One of these is to build a relay with numerous restraining elements operating on a common shaft. This method is undesirable from the standpoint of added complexity and practical considerations. For example, if six or more restraining elements are made to operate on one shaft, the length of the shaft becomes unwieldy and serious mechanical problems are introduced. The same results may be achieved by applying two or more ratio differential relays having three restraining windings and so connected that one restraining coil is available for each circuit and both relays must close their contacts to complete the tripping function. For many applications the standard three-element ratio differential relay, such as the type *CA*-4, is satisfactory. An illustration of this scheme is shown in figure 4 for a bus having six lines connected to it. Each circuit has a separate restraining winding and the operating coils of the two relays are connected in series so that on an internal fault both relays will close their contacts.

For more complex bus connections the same scheme can be amplified using three or more relays per phase, but usually a maximum of three relays is all that is required, since careful consideration of the minimum number of sources that can be connected to the bus at any one time will allow the paralleling of certain current transformers. This means that for all possible bus setups at least two restraining windings will be energized on through faults. For example, operating conditions may be such that for a time no sources of current are connected to the restraining windings of one of the two relays. Upon the occurrence of an external fault this relay would then probably close its contacts. The other relay, however, would have its restraining windings energized in accordance with the current flowing from the active sources to the fault so that its correct performance in failing to close its contacts during an external fault would prevent a false tripping operation.

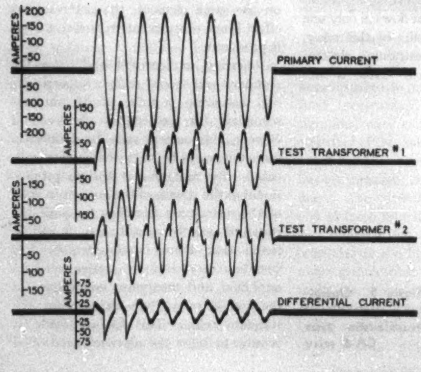

Figure 3. Oscillogram showing effect of a-c saturation and reversal of one current transformer

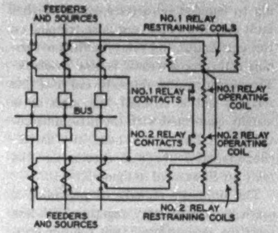
FEEDERS AND SOURCES
NO. 1 RELAY RESTRAINING COILS
NO. 1 RELAY CONTACTS
NO. 1 RELAY OPERATING COIL
BUS
NO. 2 RELAY CONTACTS
NO. 2 RELAY OPERATING COIL
FEEDERS AND SOURCES
NO. 2 RELAY RESTRAINING COILS

Figure 4. Differential protection using two CA-4 relays per phase

VARIABLE RATIO CHARACTERISTICS

The problem becomes more involved if the ratio of maximum through fault current to minimum internal fault current is large. For example, if the system is grounded through an impedance, the operating coil current may be quite limited for a phase-to-ground fault on the bus. If the magnitude of this current in per cent of normal load current is small, a high-sensitivity relay will be required (high sensitivity being synonymous with low percentage ratio). Upon the occurrence of an external interphase fault, however, the current will not be limited by the grounding impedance. If unfaithful current-transformer performance results from these higher currents, it is quite likely that the difference current in per cent of the through or restraining coil current may be high enough to cause the relay to trip. It is obvious from these considerations that the ideal relay for this application should have a variable ratio characteristic. Its percentage ratio should be low at normal currents to furnish sensitive operation for minimum internal faults. Its percentage ratio should be high, corresponding to reduced sensitivity, at the higher fault currents to prevent false tripping on through faults. A relay having a variable ratio characteristic is now available. The operating characteristic curve of this relay is not a straight line such as is usually connected with the ratio differential relay, but has a bent or "flared" characteristic. Typical curves for this relay are illustrated in figure 5.

The ordinary remedy of providing a sensitive ground relay together with less sensitive phase relays is not the universal answer to the above problem, particularly where d-c transients are involved. In such an application, the occurrence of an external three-phase fault will result in maximum, or very nearly maximum, d-c component in at least one of the three phases. The current transformers in this phase will saturate more severely than those in the other two phases receiving less d-c component. The resulting unbalance in the secondary currents will flow through the ground relay circuit, and thus tend to cause false operation of a sensitive relay.

The problem of securing the flared characteristics, as illustrated, is quite involved in a multirestraint relay where the torques vary approximately as the square of the current. In the standard type *CA*-4 relay there are three restraining windings and one operating coil winding which have been so proportioned on their respective electromagnets to give approximately straight-line characteristics. It would appear, on first sight, that in order to bend the straight-line characteristic it would only be necessary to increase the turns on the operating coil to increase the sensitivity of the relay at low restraint currents, and then allow the operating element to saturate in order to obtain lower sensitivity at higher fault currents. Such would be the case provided that the particular restraint conditions could be chosen and held constant, which they obviously cannot be. For example, if 60 amperes secondary were involved in a through fault and this current was equally divided between the three restraining windings, the torque per restraining element would be proportional to $3(20)^2 = 1{,}200$. On the other hand, if the current were divided between two restraining windings, 30 amperes in each, the restraining force would be proportional to $2(30)^2 = 1{,}800$. If the total secondary current flows in only one restraining winding, the torque would be proportional to $1(60)^2 = 3{,}600$. Obviously, there is a vast amount of difference between these three quantities themselves and between them and a host of other conditions which may be set up involving various unequal distributions of restraining-coil currents. The problem is also complicated by the fact that the restraining elements themselves tend to saturate at the high currents in a reasonable relay design so that the torque per element departs from the square law at the high currents. This explains why it was found that while the relay characteristic could be bent quite sufficiently for the conditions of maximum restraint by simple saturation, the same bend in the curves did not apply for conditions giving minimum restraint.

A New Bus Differential Relay

The fact that either one, two, or three restraining circuits per relay might be energized, and thus cause the above variations, led to the development of the type *CA*-6 relay in which no less than two restraining elements could ever be energized regardless of variation in system connections or location of the fault (figure 8). The internal arrangement of the restraining circuits of this relay is shown in figure 6. It is this particular modification of the standard three-restraining-element ratio differential relay which gave the bent operating curves of figure 5. It may be seen from inspection of figure 6 that if current flows in only one of the restraining circuits of this relay, at least two of the restraining electromagnets are energized. This is accomplished by winding each of the restraining elements with two separate windings as shown in the diagram. Current flowing into one of the windings of the restraining element must also flow through the other winding of another restraining element. These windings have an equal number of turns so that they are equally effective.

The polarity marks shown indicate the respective ends of the two windings of a single element into which the currents must flow in order for their effect to be additive. That is to say, two currents of five amperes each flowing into the polarity ends of two windings on one element give the same effect as ten amperes flowing in only one of these windings. At the same time, if a current of five amperes flows in at the polarity mark of one winding and another in-phase current of five amperes flows in at the nonpolarity mark of the other winding on the same element, the net resulting effect is one of cancellation and no torque is produced.

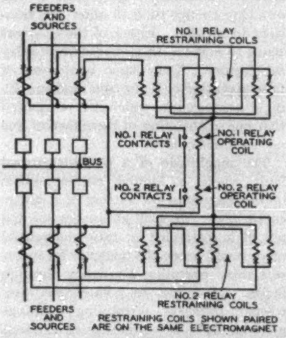
FEEDERS AND SOURCES
NO. 1 RELAY RESTRAINING COILS
NO. 1 RELAY CONTACTS
NO. 1 RELAY OPERATING COIL
BUS
NO. 2 RELAY CONTACTS
NO. 2 RELAY OPERATING COIL
NO. 2 RELAY RESTRAINING COILS
FEEDERS AND SOURCES
RESTRAINING COILS SHOWN PAIRED ARE ON THE SAME ELECTROMAGNET

Figure 6. Differential protection using two CA-6 relays per phase with double restraining windings

Referring to figures 4 and 6 for the standard and special relays, respectively, and assuming a total of 50 amperes secondary for the external short-circuit current, this current may be divided in numerous ways between the circuits indicated. By assuming a torque proportional to the square of the current in each of the restraining windings for both the standard and the special relay, it will be found that there is considerably less variation in restraining torque between minimum and maximum conditions for the special relay than there is for the standard relay. This feature made it possible to follow the approach mentioned

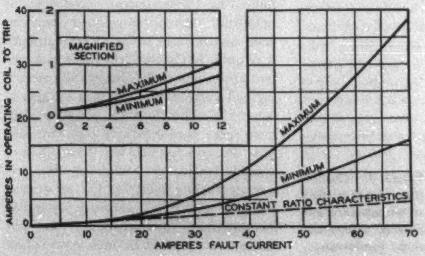

Figure 5. Operating characteristics of variable-ratio type CA-6 relay

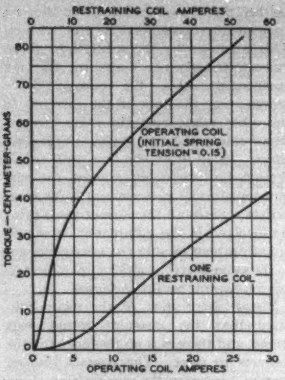

Figure 7. Torque-current curves of variable-ratio differential relay

previously which involved increasing the turns of the operating element to increase the sensitivity at low values of restraint current, and to obtain reduced sensitivity at the higher restraint currents by allowing the operating element to saturate. The reduction in variation between maximum and minimum conditions of restraint made possible a smaller variation between the operating curves at the higher restraint values, as shown on figure 5.

The torque curves shown in figure 7 indicate torque in centimeter-grams of the operating coil for various difference or operating currents and the torque per winding of each restraining element for various restraining currents. These torque curves may be used for constructing the operating characteristic of the relay for any particular division of restraining coil currents which may be chosen. Actually, in obtaining the operating curves shown, data were taken for seven different conditions intended to represent all possible divisions of a given fault current with respect to the restraining circuits and with respect to the direction in which this current might flow. It was found that test curves for any one of these conditions could be reproduced by calculated curves obtained by the use of the torque data of figure 7. Thus, the percentage ratio of the relay may be calculated for any given set of conditions. It will be found that the calculated ratio for any condition will fall between the limits indicated on figure 5.

The maximum condition of figure 5 is incurred when the fault current of indicated value flows in one restraining circuit only and flows out of the relay through the common connection to the other relay. The minimum condition shown represents two equal currents flowing into the relay in two restraint circuits and flowing out of the relay through the remaining restraint circuit without any current coming through the common connection to the other relay. In this case, the abscissa represents the total fault current, which is the sum of the two equal and smaller currents.

The time of operation of the relay may be as low as four cycles for internal faults based on one-fourth inch contact travel. A somewhat shorter operating time can be obtained with a correspondingly smaller contact separation.

System Tests

System Conditions

Tests on the relay, which has been described, were made under conditions similar to those that would exist if it were installed to protect a 66-kv bus at a large generating station. Conditions simulated were as shown in figure 9 and consisted of five sources representing supply from four transformer banks and a bus-tie breaker, and four transmission lines. The current transformer for only one of these four lines was used, since it was assumed that a fault would exist on only one of the lines at a time. Maximum symmetrical currents for a feeder fault are shown, as well as the cross-sectional area of the current transformers in the various locations, and the manner of connecting them to the relay coils.

Figure 8. Type CA-6 relay

Test Setup and Test Procedure

Because of the extremely high fault currents (21,000 amperes at 66 kv) it was undesirable to make the tests under actual conditions. The method used is shown on figure 10 in which the current transformers, instead of operating on 66-kv current received 11-kv current, thereby reducing the necessary test current to one-sixth. The test current was further reduced by winding additional primary turns through the bushing-type current transformers. By this means, it was possible to so arrange the number of turns through the current transformers to correspond to the amount and division of fault current from the various sources involved, and at the same time keep the values of fault current well below the full-load rating of the equipment being used for tests. Reversing switches were used in the secondaries of the current transformers so that it was possible to represent both bus faults and through faults in various locations.

Figure 9 shows the bus setup, fault currents, and relay connections for one particular condition. By means of the switches mentioned above, and by varying primary turns in the current transformers, other operating conditions involving different bus connections, different magnitudes and distribution of fault current, and different fault locations were simulated.

Since the time required for the d-c component to decay is a function of the inductance and resistance of the circuit, it was necessary for the test circuit to have a time constant similar to that which would exist during actual fault condi-

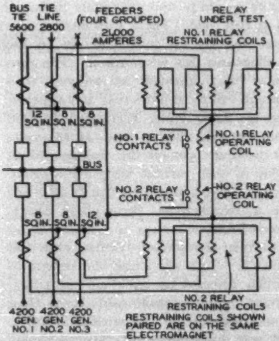
BUS TIE 5600
TIE LINE 2800
FEEDERS (FOUR GROUPED) 21,000 AMPERES
RELAY UNDER TEST
NO. 1 RELAY RESTRAINING COILS
12 SQ IN.
8 SQ IN.
8 SQ IN.
NO. 1 RELAY CONTACTS
NO. 1 RELAY OPERATING COIL
BUS
NO. 2 RELAY CONTACTS
NO. 2 RELAY OPERATING COIL
8 SQ IN.
8 SQ IN.
12 SQ IN.
NO. 2 RELAY RESTRAINING COILS
4200 GEN. NO. 1
4200 GEN. NO. 2
4200 GEN. NO. 3
RESTRAINING COILS SHOWN PAIRED ARE ON THE SAME ELECTROMAGNET

Figure 9. Approximate symmetrical fault currents from sources, and sizes of current transformers

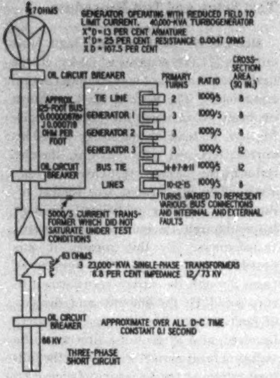
7 OHMS
GENERATOR OPERATING WITH REDUCED FIELD TO LIMIT CURRENT. 40,000-KVA TURBOGENERATOR
X"D = 13 PER CENT ARMATURE
X'D = 25 PER CENT RESISTANCE 0.0047 OHMS
XD = 107.5 PER CENT
OIL CIRCUIT BREAKER
PRIMARY TURNS
RATIO
CROSS-SECTION AREA (SQ IN.)
APPROX. 25-FOOT BUS 0.0000087R+J 0.000710 OHM PER FOOT
TIE LINE 2 1000/5 8
GENERATOR 1 3 1000/5 8
GENERATOR 2 3 1000/5 8
GENERATOR 3 3 1000/5 12
BUS TIE 4-6-7-8-11 1000/5 12
LINES 10-12-15 1000/5 8
OIL CIRCUIT BREAKER
TURNS VARIED TO REPRESENT VARIOUS BUS CONNECTIONS AND INTERNAL AND EXTERNAL FAULTS
5000/5 CURRENT TRANSFORMER WHICH DID NOT SATURATE UNDER TEST CONDITIONS
63 OHMS
3 23,000-KVA SINGLE-PHASE TRANSFORMERS 6.8 PER CENT IMPEDANCE 12/73 KV
OIL CIRCUIT BREAKER
APPROXIMATE OVER ALL D-C TIME CONSTANT 0.1 SECOND
66 KV
THREE-PHASE SHORT CIRCUIT

Figure 10. Diagram of current-transformer test circuit

tions. Constants of the test circuit as well as the current transformers used with their approximate cross section of iron, and their turn ratios, are shown on figure 10. The d-c time constant of the circuit was approximately 0.10 second.

The question was raised as to whether the time constants of the generator operating at reduced field would be the same as when operating at normal field. Conclusions were that neither the position of the field rheostat nor the value of the field current would affect the d-c time constant. The a-c time constants would be somewhat affected by the position of the field rheostat. However, by lowering the field current by reducing the voltage of the exciter and leaving the field rheostat in the position corresponding to full-load voltage, the time constants would be approximately the same as at full field. These conclusions were borne out by the results shown on the oscillograms that were taken. The generator was operated at reduced field current (40 to 50 amperes) and approximately 2,500 volts, compared to 230 amperes field current and 11,500 volts for no-load open-circuit voltage. This value of field current supplied approximately 1,400 amperes in the 12-kv leads for a symmetrical three-phase short-circuit fault.

Current-transformer ratios were all 1,000/5. There was a one-ohm resistance burden imposed on each current transformer. This is considerably larger than would normally be met with in practice, but was made large in order to determine, if possible, the limitations of this relay.

A total of 32 faults were applied. This was done by first closing the 12-kv oil circuit breaker, thus energizing the transformer bank, and then closing the 66-kv oil circuit breaker.

Oscillograph records were taken of the currents in the relay coils, as well as the exchange of currents between the two relays. During the tests only one relay was in use, the second relay being represented by dummy coils. Figure 11 shows an oscillogram taken during an asymmetrical through fault; figure 12, a symmetrical through fault; and figure 13 an asymmetrical inside fault.

As observed by the reference to figure 10 the tests were made on a single-phase basis. In order to make sure that the worst asymmetrical or symmetrical condition, whichever was desired, was obtained, it was necessary to make a number of tests under each fault condition. It was found from the oscillograph films that by making four to five tests, there was good probability of the fault current in one of these tests being completely offset.

Results

Figure 11 shows the oscillographic record of a test made under conditions simulating the operating conditions shown on figure 9. The latter figure shows the location of the fault, as well as the number of sources connected to the bus, and the amount and direction of the current flowing in the various current transformers. It will be observed that the primary current wave, figure 11, is completely offset, indicating maximum d-c transient. This wave represents a faithful reproduction of both the a-c and d-c components of the primary current, and was obtained by the use of a high-ratio current transformer shown on figure 10, which did not saturate on the current values used. It will also be observed that the current waves from the current transformers in the bus tie, tie line, and outgoing line circuits are not faithful reproductions of the primary current. It is obvious that neither the d-c component nor the a-c component represents the true values. This is caused by the saturation of the current transformer cores by the d-c component. Also, the current wave representing the sum of the currents to the other relay from the other current transformers of figure 9 shows the same effect. In all cases, the first peak comes through fairly well, after which the current-transformer performance is far from perfect. Since all the current transformers involved in the differential circuit do not saturate at exactly the same time, nor to the same degree, a difference or operating-coil current results as shown.

Visual observation of the relay indicated that the restraint torque was strong, and there was no tendency for the contacts to close. The contact line on the film, occupying the zero position of the primary current wave, also shows that the contacts did not close. (Closure of the contacts would have deflected this line, as shown on the films representing internal faults.)

Figure 12 shows another test made under exactly the same conditions as for figure 11, except that in this case an almost symmetrical current was obtained. The small amount of d-c component that was present, however, is responsible for the appearance in the operating-coil circuit of the current shown. The magnitude of this current is considerably less than the first peaks shown on the preceding figure, the calibration of the oscillograph element being unchanged. As before, the relay did not operate, but showed a strong restraint torque. In this case, the magnitude of the first cycle of the a-c component of the fault current was measured from the film and found to be approximately 25,600 amperes root-mean-square. From the standpoint of a-c saturation, this test was more severe than the preceding one because of the higher current values, yet it is readily noted that the performance of the various current transformers was much improved.

Figure 11. Oscillogram for an external asymmetrical fault

The answer to this is, of course, that in figure 12, the test current transformers are not performing under so severe a handicap imposed by d-c saturation.

A total of 20 tests were made representing through faults under various system conditions and for other magnitudes and distribution of fault current. Both asymmetrical and symmetrical currents were obtained, and, in all cases, the relay showed a strong restraining torque.

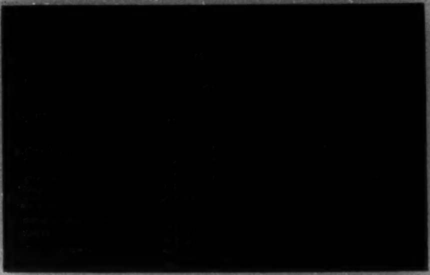
Figure 13. Oscillogram for an internal asymmetrical fault

Figure 13 shows an oscillogram for a test representing an internal fault on the bus for which the relay should trip. For this test, most of the sources were considered to be on the other bus, so that the bus-tie circuit contributed 19,800 amperes and generator number 3 contributed 7,200 amperes to the 27,000-ampere fault.

As in figure 11, the effect of the d-c component in causing saturation is immediately obvious. The value of the operating coil current is reduced very materially from the theoretical value, but, by the same token, the restraint-coil currents are also reduced, and positive operation of the relay in a fraction over four cycles is shown. (The jagged contact line shown results from a very small current being used in the contact circuit for the operation of the oscillograph element.)

A total of 12 tests were made representing internal faults under various other system conditions. Both symmetrical and asymmetrical currents were obtained, and, in all cases, the relay operated to close its contacts in a positive manner.

Conclusions

1. In applying bus differential protection, each application must be considered carefully with respect to the amount and distribution of fault currents, sizes and ratio of current transformers, number of generating sources, and, consequently, the number of restraining coils needed, relationship of protected bus to power sources as affecting the amount and extent of the d-c component, and other important factors.

2. The use of two or more standard percentage differential relays connected in series as shown in figure 4 makes available the number of restraining coils necessary for most applications.

3. In those cases where a ratio of maximum through fault current to minimum inside fault current is large due to high neutral impedance, relays with special characteristics as described in the paper are available.

4. System tests which simulate the conditions actually obtaining in practice demonstrated the reliability of the scheme and the relay described in the paper.

Figure 12. Oscillogram for an external symmetrical fault

References

1. CURRENT TRANSFORMER EXCITATION UNDER TRANSIENT CONDITIONS, **D. E. Marshall** and **P. O. Langguth.** AIEE TRANSACTIONS, volume 48, 1929, page 1464.

2. RELAY PROTECTION FOR STATION BUSSES, **W. A. Lewis** and **R. M. Smith.** *The Electric Journal*, November 1937.

Discussion

C. D. Hayward (General Electric Company, Philadelphia, Pa.): In this paper the authors have described some of the special problems encountered in the application of differential relays to generator bus protection, as distinguished from transmission line bus or distribution bus protection. Briefly speaking these problems may be summarized under two heads as to causes:

First, in old installations it is usually found that the only current transformers already available in the feeder circuits are ones of low ratio. In case of a feeder fault close to the bus these are subjected to currents of many times their rating with the result that a high degree of saturation and consequent breakdown in ratio occurs. In the tests described in this paper the authors have confined their investigations to 1,000/5-ampere current transformers, but more often in practice ratios of 400/5 or 300/5 are found. It is evident that much higher degrees of saturation and consequent greater difficulty in applying a relay of the ratio differential type would have been experienced had current transformers of lower ratio been used. It is highly desirable, of course, from a cost point of view to use the existing current transformers if possible, rather than to install new ones or to rewind the old ones for higher ratio.

The second special problem is that of the long time constant of the d-c component of the current to a fault near the generators, for which the L/R ratio is higher than for a transmission line. It is well known to relay engineers that the steady-state ratio and wave-form breakdown of current transformers on overcurrent is greatly affected by the magnitude of the secondary burden. It is not so widely appreciated that the fidelity of current-transformer response to transient offset primary-current waves is also affected by the burden imposed. The d-c component of the primary wave induces a pulse of direct current in the secondary having a magnitude and rate of decay depending on the secondary burden. By the laws of electromagnetic induction the ampere-turns of the secondary d-c pulse oppose the ampere-turns of the primary d-c component and d-c saturation of the core is produced by their difference. The effect of the secondary pulse is always smaller and more rapidly decaying than that of the primary but approaches it most closely when the secondary burden is a minimum. Hence to reduce d-c saturation as well as to reduce a-c saturation and their effects on the current-transformer secondary-current response it is desirable to keep the burden applied to the current transformers as low as possible.

A year ago at this convention L. F. Kennedy and I presented a paper titled "Harmonic Current Restrained Relays for Differential Protection" which was later published in ELECTRICAL ENGINEERING for

May 1938. This paper describes a relay employing filter circuits to separate the harmonic and d-c pulse components of the current presented to it from the fundamental line frequency component, using the fundamental component to energize the operating coil and the harmonic and d-c pulse components to energize the restraining coil. By proper adjustment of the per cent harmonic restraint this relay is made to distinguish between differential currents due to internal faults which contain relatively low percentages of harmonics and those due to current transformer saturation on heavy through faults which contain relatively high percentages of harmonics. This relay is connected in the simple form of differential circuit shown in figure 1 of the present paper.

With this circuit the current-transformer secondaries may be connected directly to paralleling busses in the switchyard resulting in the minimum current transformer burden. Only four wires need be run from the switchyard busses to the relay panel for a three-phase relay setup including ground relays regardless of the number of bus circuits involved.

Compare this with the more complicated circuit for the ratio differential relay in figure 4 or 6. Here a minimum of 19 wires is required for a three-phase setup with only six circuits. Aside from the cost of installing and maintaining the extra wires they are undesirable because of the additional burden which they, together with the relay restraint coils, place on the current transformers.

While high-speed operation may not be absolutely necessary in all generator bus applications it is never undesirable at least and there are many bus applications, transmission busses particularly, where high-speed operation is necessary for stability considerations. While the harmonic-restraint relay has an operating time in the neighborhood of one cycle, figure 13 shows over four cycles for the ratio differential relay. Considerable contact bouncing is also shown by the jagged trace of the contact vibrator. While this would not be serious if only one contact were in the circuit, since contact arcing would probably allow the current to continue to flow after the first contact, it might be serious when two or more contacts each on a separate relay, are connected in series. In this case the circuit is not completed when the contacts close and bounce open alternately but must await a time when all are closed at the same instant. This effect does not show up on the oscillogram because according to the test description, only one relay was actually connected in the circuit, the other being represented by dummy coils.

In conclusion, we believe that for the differential protections of busses as well as other equipment the many advantages of relays employing the harmonic restraint principle should be seriously considered.

W. A. Lewis (Cornell University, Ithaca, N. Y.): As discussed in the paper the effect of the d-c transient is the most important single problem in high-speed relaying. This was first forcibly realized in connection with the high-speed relays at Boulder Dam. The problem was given very careful study, for transformer and generator differential relays. It was found that it was relatively easy to predict the design of a current transformer which would not saturate on the d-c transient, but such designs were quite unreasonable to build in a great many cases. In typical cases it was found that the d-c transient has as much as 100 times the saturating effect of the corresponding symmetrical a-c component of the current, that is, 100 times as much flux is required to transform the d-c component accurately as to transform the a-c component. It is thus obvious that the ratio curves of current transformers taken on symmetrical current, even as high as 20 times normal, are entirely unsatisfactory for predicting the performance when the wave contains the maximum amount of d-c transient. In the case of Boulder Dam, the maximum symmetrical short-circuit currents on external faults were limited to three or four times the normal current, and in this case it was possible to obtain a solution by the use of special current transformers. However, suitable transformers were found to be four or five times as large as conventional transformers, and it was obvious that this method of procedure was entirely impracticable when the short-circuit currents or external faults are very much larger than the values encountered in this case.

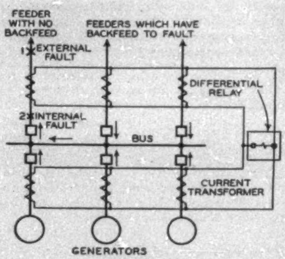

Figure 1. Schematic diagram of bus differential protection

It should, of course, be realized that the effect of the d-c transient is dependent to a large extent upon the rate of decay of the transient, or in other words upon the d-c time constant. This time constant is relatively long when the short circuit is near to high-capacity generating stations, but is reduced very rapidly as the resistance of the circuit is increased by the resistance of transmission circuits. Thus, danger from d-c transient effects is most pronounced in the immediate vicinity of large generating stations, and is almost nonexistent at distribution substations remote from the source of generation.

For any bus differential scheme to operate, it is essential that the current transformer carrying the fault current to an external fault be reasonably good. In the most extreme case, it may be visualized that a current transformer may saturate so severely that the secondary current is zero. When this is true, it is not possible to distinguish between an external fault and an internal fault, and no scheme of bus differential protection can possibly operate satisfactorily. This may be illustrated by reference to figure 1 of this discussion. Assuming first an external fault at the location marked 1 on the diagram, it will be seen that all the fault current must pass through the current transformer in the outgoing feeder. To simplify the problem, it has been assumed that there is no source of power at the other end of the feeder, so that no current will be fed back from the feeder into the fault. If it is assumed that the outgoing current transformer saturates completely, so that there is no secondary current, the current in the differential relay will be exactly the same as it would be for an internal fault at the location marked 2 on the diagram. Obviously, the relay should not trip for the external fault and should trip for the internal fault, so that satisfactory discrimination depends entirely upon the action of the current transformer in the feeder considered. It is thus obvious that the first consideration must be given to the current transformer, and the relay used must be designed to take the best advantage of the current-transformer characteristics. The authors of the paper have given very careful consideration to this problem, and have shown what can be done when each different condition is carefully considered.

Other schemes of bus differential protection which appear simpler have been proposed. However, as pointed out above, none of these can be successful if the current transformers are inadequate. In considering any scheme which makes use of departures of the wave form from a sine wave, it must be recognized that, at extremely high saturations, an iron-core current transformer approaches an air-core current transformer and, for such a case, the output current will again be very nearly sinusoidal, even though it is much less than the nominal ratio of the current transformer would indicate. In applying any such relay scheme, the characteristics of the output wave and the relay must be carefully analyzed, to make certain that the proper requirements for satisfactory operation are fully met.

It is obvious that the burden of the relay has an effect upon the operation of the protective scheme, and the smaller the burden the less the saturation will be in all the current transformers. However, it has been found that, in current transformers of ratios 2,000/5 and higher, the internal burden of the current transformer secondary winding is usually much greater than that of any of the relays proposed, and as a result the burden of the relay used is relatively unimportant in the determination of the current-transformer characteristics. Thus, while low burden is helpful, it becomes a matter of relatively small importance where the current transformer ratio is high, and therefore the desirable relay characteristics should not be sacrificed in an effort to make the burden extremely small. On the other hand, where the current-transformer ratios are 1,000/5 and lower, the internal burden of the current transformer is considerably smaller and more effort is justified in reducing the relay burden.

T. W. Trice (Consolidated Gas, Electric Light and Power Company of Baltimore, Md.): The authors of this paper are to be congratulated on the manner in which they thoroughly cover the major factors to be considered in bus differential protection. The way in which the conventional *CA*-4 relay has been modified is unique and its variable-ratio characteristics should make this relay particularly adaptable where there is considerable variation between minimum bus faults and maximum through faults.

Where the ratio of maximum through fault current to the minimum internal fault current is not large and two standard *CA*-4 relays could be used to cover all operating conditions, would it not be possible to use one of the modified relays connected as a simple ratio differential relay having six restraint windings?

It has been noted that the time of operation of the modified relay may be as low as four cycles based on one-fourth-inch contact separation and a somewhat shorter operating time can be obtained with a smaller contact separation. Assuming the smallest contact separation consistent with reliable performance approximately what would be the fastest operating time?

No mention is made as to whether or not any of these modified relays are in service. As in every case the final test of any piece of equipment is its actual service record and a summary of any operating experience with this modified relay would be of interest.

C. O. Werres (General Electric Company, Schenectady, N. Y.): Too much emphasis cannot be put on the fact that the success of differential protection is still very largely dependent upon current-transformer characteristics. This dependence is implied in the second paragraph of the article but it will bear reiteration.

The "faithful current transformer" mentioned is a most difficult but desirable goal. Its attainment involves building what has often been called the "ideal" transformer, that is, one with no errors, or to be more practical, negligible errors, throughout its entire range of performance. With this as a goal, much progress has been made. This, along with several other helpful measures listed below, is gradually improving the effectiveness and reliability of differential and naturally other types of protection:

(*a*). Improvements in current-transformer design.

(*b*). Determination of overcurrent characteristics of current transformers.

(*c*). Considering transformer characteristics when laying out protective equipments, etc.

(*d*). A trend toward lower ratio of fault current to current-transformer rating.

(*e*). Balancing burdens and keeping them at a minimum.

(*f*). Inserting resistance in the differential circuit.

Regarding current-transformer designs, the authors point out the difficulty of obtaining matched characteristics at high inductions from two transformers of the same design. The reason for this is obvious from *B-H* curves on core steels which show a wide spread between maximum and minimum characteristics of a given material. The safest solution then is to avoid saturation as was brought out in the Marshall-Langguth paper. However, in designing standard transformers, some compromise must be accepted as it is evident that nonsaturating transformers to meet every condition would be impracticable. A reasonable specification seems to be to limit the ratio error to five per cent at 20 times rated current with burden *Z*.

In view of proposals to adopt a ring-core construction as a means of approaching the ideal, it is pointed out that recent studies on various core designs show that for some ratings the rectangular core can be as good as, and more economical than, the ring core if the secondary is distributed over all four legs.

A. T. Sinks (General Electric Company, Lynn, Mass.): The authors are to be commended for again calling attention to some of the idiosyncrasies of current transformers and for emphasizing the fact that these phenomena must be considered carefully in differential relay applications. There are some points, however, with relation to their proposed ideal relay characteristic which are not quite clear.

The bushing current transformers used in the tests described in the paper had large cross sections of iron, so that the products of the iron cross sections and secondary turns were larger than would be normally encountered with instrument-type current transformers. Many current transformers in use on lower voltage systems will have large errors on asymmetrical current waves even for rather low currents. Therefore, the permissible sensitivity of the relay cannot be selected arbitrarily as it depends upon the faithfulness of reproduction of the fault currents by the current transformers, and particularly by the one that carries the total current to an external fault.

From the oscillograms of test and the authors' statement about four-cycle operation of the relay, it would appear that at least in some of the through-fault tests the only reason the relay did not operate was because it was not fast enough, as the oscillogram, figure 11 shows a large value of differential current for the first two cycles. With a given set of current transformers the period during which this false differential current may exist can be longer at low currents than at high ones. Consequently, the chances of this false differential current persisting long enough to operate the relay may be greater at lower values of primary current. Therefore, again why is a higher sensitivity at lower currents a desirable characteristic?

One of the questions which is frequently asked and apparently not well understood is: "Since the fault is fundamentally with the current transformers, why not build them to work right?" Some comment on this point, therefore, seems in order.

There are at least two ways of attacking this problem which give theoretically ideal results as follows:

1. Use air-cored current transformers with carefully balanced burdens, mutual inductances, and secondary-circuit constants.

2. Put enough iron and copper in the transformers to make them accurate up to the desired overcurrent.

There are, however, practical objections to either of these schemes. In the case of the first method, very careful balancing of the factors mentioned is absolutely essential, making each installation a hand-tailored job. Saturating burdens except in the differential element could not be tolerated. The transformers would be entirely special and would not be satisfactory for other services.

When an attempt is made to design a current transformer along conventional lines to reproduce asymmetrical waves faithfully, the first obstacle encountered is the enormous amount of core flux required to generate the d-c transient component of voltage in the secondary circuit. For instance, in the case of a 100 per cent offset wave, 0.1 second time constant, and 60 cycles frequency, the core flux required for the secondary IR voltage associated with the d-c transient is 37.7 times that required for the a-c component. (D-c flux/a-c flux $= 2\pi f \times$ time constant.) From this the large amount of iron necessary to take care of the d-c component of an asymmetrical wave can be readily appreciated. Nor does reducing the secondary-circuit resistance external to the transformer help beyond a certain point as the large amount of iron means a long length of secondary copper which can only be compensated for by making the cross section of copper large. This in turn requires more iron and so the quantities pyramid.

All this does not mean that it is impossible to design an iron-core transformer to reproduce accurately asymmetrical currents of high value, but it does mean that such a transformer will usually exceed acceptable limits of size and cost.

It would, therefore, seem that a relay of the harmonic-restraint type, which actually takes advantage of the peculiarities of the imperfect but economically feasible conventional transformer, is the most practical solution yet proposed.

J. A. Elzi (Commonwealth and Southern Corporation, Jackson, Mich.): This paper brings up the point that the application of relays for bus protection has not been given as much attention in recent years as transmission-line relaying. On the contrary, it is our opinion that bus protection has been given considerable attention but the factors that might cause incorrect operation, and which are very well discussed in this paper, have been recognized, and in many cases it has been found that the hazards involved outweigh the advantages to be gained. This paper and the results of tests contained therein show that these potential sources of incorrect operation are important and, in general, could not be taken care of with the relays available until recently.

The high-speed clearing of bus faults to minimize the damage done due to excessive fault currents is only one of the advantages to be gained. In some stations, it is equally as important to be able to sectionalize the bus in case of a fault so that there will be a minimum of loss of load and generating capacity. The extent to which this can be accomplished by a bus protective scheme is of course contingent on the arrangement of circuits on the bus and the number of sectionalizing points provided.

The results of the tests made with the new percentage differential relays described in this paper would seem to indicate that considerably more reliable operation could be expected from this relay than with some of the types heretofore available. The relatively small amount of current available in the differential circuit for the first few cycles in figure 13 leads to the question as to whether or not the relay would have operated in four cycles as indicated if this current had been further reduced by diversion to deenergized current transformers in parallel with the ones which were supplying this current. It would also be interesting to know how the cross section of the cores of the current transformers used in this test compare with those of standard bushing-type current transformers usually supplied for this service voltage.

R. M. Smith and W. K. Sonnemann: The authors are gratified that the interest in this problem has brought forth interesting and constructive discussion.

J. A. Elzi contributes a valuable point when he asks if the relay would have operated in four cycles, as shown in figure 13, if the operating coil current had been further reduced by diversion to de-energized current transformers in parallel. The tendency is in this direction because of the fact that those current transformers not subject to current in their primaries will draw an excitation current in their secondaries depending upon the voltage across the operating coils of the relays and the magnetization curves of the current transformers involved. This voltage is distinctly limited, however, by the saturation characteristics of the operating coil circuit. Also, the "dead" current transformers, not being affected by d-c saturation, will be operating on their a-c magnetization characteristics and will draw only a very small amount of current. This phase of the problem should be considered if the number of feeder-circuit current transformers in parallel is very large, and if their ratio is low. The authors feel that the more important consideration of this point occurs when the relays must trip under normal load conditions when a light ground fault occurs on the bus. Under this condition the relay is much more sensitive, as illustrated in figure 5, so that more loss of operating-coil current to "dead" current transformers can be tolerated. Ample safety factor has been found in applications studied to date, but it is felt that this point must not be overlooked.

The cross section of iron in the test current transformers represent typical values for present-day 66-kv oil-circuit-breaker bushings, except that 12 square inches appears to be a little large. No definite standards have been established in this respect, however, so that considerable variation may be found.

Mr. Hayward mentions that the test conditions would have been more severe had lower-ratio current transformers been used, such as 400/5. It is admittedly true that, for a given primary current, and other conditions remaining the same, a lower-ratio current transformer will be likely to saturate more severely. This point has been admirably answered, however, in the discussion by W. A. Lewis in which he points out that reasonable performance must be had from the current transformer in the faulted feeder circuit in order that any type of relay may properly discriminate between an internal and an external fault. As a matter of fact, a relay of the ratio-differential type, having a slight time delay, is less likely to operate falsely with low-ratio current transformers than a relay of the high-speed (one cycle) type. This is because its time delay allows the badly saturated current transformer enough time to recover from d-c saturation sufficiently to discriminate between external and internal faults.

Mr. Hayward makes the point that it is well to keep the secondary burden as low as possible in order to minimize the effect of the d-c transient in saturating the current transformers. If this could be carried to the ultimate end so that no current transformer in any part of the circuit would saturate, then a simple differential relay as indicated in figure 1 would suffice. In the majority of cases, however, where the d-c transient has a long time constant, it will be found practically impossible to escape the effect of d-c saturation in those current transformers in the faulted feeder circuit, even by decreasing the lead burden, as has been pointed out. In this case, when saturation of the output current transformers is assured, it is a definite advantage to have some degree of saturation in the input current transformers as well. For example, in figure 11 of the paper, the traces show definite saturation of the various current transformers in the input leads to the bus. The degree to which the input current transformers fail to hold their ratio makes a corresponding reduction in the magnitude and duration of the differential current.

The statement made by Mr. Hayward that the relative percentage of harmonics present in the differential current is sufficient to discriminate between external and internal faults is somewhat optimistic, in view of the findings on a large number of tests. With sufficient time delay, a material percentage of harmonics will show up in the differential current for an external fault. If time delay is introduced, however, the relay is no longer a one-cycle relay. In figure 2 of this paper, for example, the first two cycles of differential current are obviously notably lacking in harmonics to any great degree. If the relay is sufficiently sensitive to harmonics to be adequately restrained from tripping for the first two cycles of the differential current of figure 2, then there is certainly grave doubt that the relay would trip properly when subject to the differential current wave form of figure 13 of the paper. It may be argued that the lower burden of the relay mentioned would do much toward improving the wave form of figure 13. This is quite true, but, on the other hand, the internal burden of the current transformers plus the burden of the differential circuit leads to the switchyard must still be considered. The experience of the authors is that there is practically an infinite variety of wave forms possible in the differential current, even when some of the variable factors are fixed. For example, the current-transformer characteristics and burden values will be fixed for a particular installation, but the variable location of the fault, involving a variable time constant, as well as variable current values, may be taken in an infinite number of combinations. Thus, it is impossible to say that any one particular wave form is typical of all external faults.

Attention has been called to the broken nature of the contact line of figure 13. It should be borne in mind that for these tests it was only desired to get a contact indication, and not to trip any breakers. The current through the contact circuit was extremely light, amounting only to that required to operate an oscillograph element, and was thus easily interrupted momentarily. Tests not described in the paper were made to establish the fact that two contacts in series could be made to pick up an auxiliary relay, at 125 volts direct current and four to five amperes, without additional delay, and were entirely successful. The auxiliary relay is necessary, of course, because of the large number of circuit breakers which must be tripped.

The supposition of Mr. Sinks that the only reason that the relay described in the paper did not trip for some of the external faults was because it was not fast enough is not correct. It is true that a large value of differential current existed for at least two cycles; however, a large restraining torque existed at the same time. The restraining torque was considerably in excess of the operating torque for all of the through faults.

Mr. Trice asks if one of the six-circuit special *CA*-4 relays might not be used in an application where two standard *CA*-4 relays would suffice. We interpret this question to mean that, since six restraining coils are available in the special relay, it would seem to be applicable on the basis of one relay per phase for a six-circuit bus. If applied in this way, however, each restraining element would receive current from two bus circuits. If a heavy current should flow into the bus in one of these circuits and away from the bus in the other, the ampere turns on that particular restraining element would be in opposition and thus cancel out. That is, equal and opposite heavy currents in the one restraining element would not produce restraining torque as they should. The same effect exactly would occur if the current transformers of the two paired circuits were paralleled on the secondary side and connected to one single-winding restraining element. In the first case, fluxes are paralleled. In the second case, currents are paralleled. The net effect is the same. These considerations indicate that if six restraining elements are required, then two relays per phase are necessary, whether they be the standard or the special type.

Some gain may be made in shortening the operating time for internal faults by decreasing the contact travel. However, not a great deal can be accomplished by this means. Probably the fastest operating time under most favorable conditions would be two to three cycles.

In answer to Mr. Trice's third question, no relays of the special type having double windings and variable-ratio characteristic have yet been placed in service, although some are now on order.

Copper-Oxide Modulators in Carrier Telephone Systems

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Synopsis: Copper-oxide modulators are widely used in telephone systems for translating either single speech channels or groups of speech channels to carrier-frequency locations on the lines. A number of simple circuit arrangements have been developed that enable suppression of certain undesired frequencies to a degree that is impractical in tube modulators. These modulators transmit equally well in either direction and the modulating elements are more nonlinear than in tube modulators. As a result numerous effects are found that ordinarily are not important in the tube arrangements. Analytical studies have been considerably simplified by the use of a small signal, and a large carrier controlling the impedance variation of the copper oxide. It is found in this case that the superposition and reciprocity theorems hold for all the circuits that it has been possible to analyze even though the modulator is made up of nonlinear elements. Open and short-circuit impedance measurements can be made use of as in four-terminal linear networks, and a generalized reflection theory developed. Performance data are given for an idealized modulator under a variety of operating conditions.

AT LEAST as early as 1927, copper-oxide rectifiers were being tried as modulators for the speech channels of carrier telephone systems in this country. At this time only a rather large type of rectifier was available, better adapted for power use rather than in modulating the few milliwatts of a speech signal. Largely because of instabilities these early units were found to be unsatisfactory for modulator use. Further developments in copper-oxide rectifiers made in various laboratories extended the variety and improved the quality of the product available, so that by about 1931 they began to be promising as serious competitors for vacuum tubes in modulators. Since 1931 continued improvements in copper-oxide rectifiers have rapidly increased their field of application until now they are employed in practically all modulators of the latest types of carrier telephone systems.

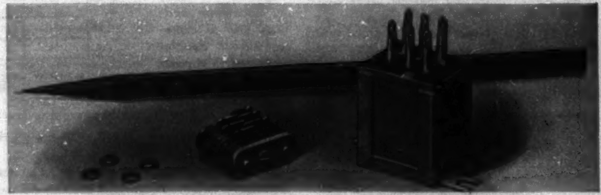

Figure 1. Four-disk copper-oxide modulator

In the new systems a copper-oxide modulator is used instead of the previous push-pull arrangement of two vacuum tubes. In cable,[*] open-wire, and coaxial carrier systems, from 26 to 28 of these modulators are needed in each direction for translating each 12-channel group of speech bands from voice to carrier and back again. These copper-oxide modulators have no power costs, tube replacements, or possibilities of power failures. In figure 1 the four $^{9}/_{16}$-inch-diameter copper-oxide disks generally used in a carrier telephone modulator are shown individually, assembled with connections, and potted in a can.

The carrier terminals have tended to become increasingly complex as it became their function to place more and more channels on a single pair of wires. The extreme simplicity and reliability of copper-oxide modulators have been of great value in helping to overcome this tendency. Copper-oxide modulators have been used from zero frequency to nearly four million cycles. Certain modulators for coaxial carrier systems have been designed to modulate simultaneously as many as 60 speech channels spaced over a 240,000-cycle band of frequencies.

Copper-oxide modulators probably differ most from tube modulators because the simplicity of the rectifier elements allows a much greater variety of circuit arrangements to be used. Although the underlying principles of operation are not new, it has become necessary to investigate numerous transmission effects that could be neglected in tube modulators. This has resulted not only from the newer circuit arrangements with their smaller losses, but also from higher transmission standards for the over-all system along with the greatly increased numbers of modulators in long circuits. Copper-oxide modulators, unlike tube modulators, transmit signals equally well in either direction. While this is a simplification in allowing a modulator also to be used as a demodulator, the modulator becomes complicated by the effects of reflections back and forth into the signal bands of numerous frequency bands of modulation products.

Circuit Arrangement

The circuit arrangements used in copper-oxide modulators generally are concerned either with carrier suppression, with carrier transmission along with the signal, or with balancing action to suppress certain unwanted bands of signal frequencies. In most carrier telephone systems economy of frequency space and amplifier load capacity demands the use of single-sideband carrier-suppressed transmission.

In figures 2*a*, 2*b*, and 2*c* three types of copper-oxide modulators are shown, each arranged to suppress the carrier in both the signal input and the signal output circuits. In figures 2*d* and 2*e* the carrier is balanced out in only one signal branch. In the usual arrangements a signal-band selective filter must be used in each signal branch to restrict transmission to that of the wanted frequency band. Largely in this way interferences are guarded against, not only into other channels or systems to which the modulator output circuit is connected on the line or at the distant

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Paper number **39-28**, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 2, 1938; made available for preprinting December 17, 1938.

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The author wishes to acknowledge his appreciation of the assistance of numerous associates in the Bell Telephone Laboratories in arriving at the views on copper-oxide-modulator performance recorded in this paper. In particular, acknowledgment is due to R. W. Chesnut, Doctor E. Peterson, and Doctor G. R. Stibitz.

[*] The general features of carrier telephone terminals have been described in a paper "Cable Carrier Telephone Terminals," by R. W. Chesnut, L. M. Ilgenfritz, and A. Kenner (ELECTRICAL ENGINEERING, January 1938).

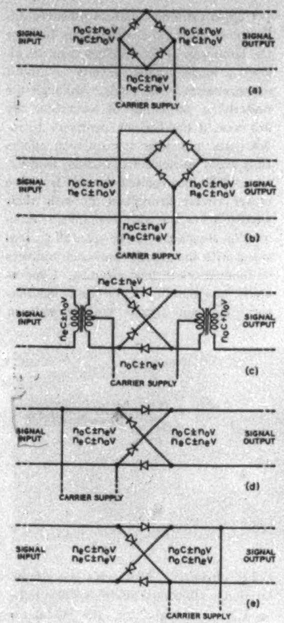
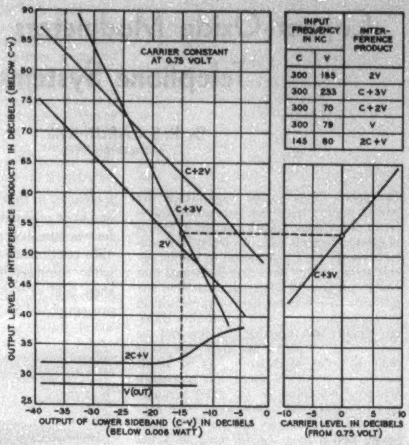


Figure 2 (left). Types of copper-modulator oxide circuits



INPUT FREQUENCY IN KC		INTERFERENCE PRODUCT
C	V	
300	185	2V
300	233	C+3V
300	70	C+2V
300	79	V
145	80	2C+V

Figure 3. Intensity of modulation products in a representative double-balanced copper-oxide modulator

end, but also back into the complex array of facilities to which the input circuit may be connected.

In any of the circuits shown, modulation results from either the reduction or reversal of the current flow between the input and output signal circuits at periodic intervals as the carrier varies the copper-oxide resistance back and forth between high and low values. In figure 2*a* where the connections of the input and output signal circuits are periodically short-circuited by the carrier-actuated copper oxide, transmission of the modulated signal into the input circuit or the unmodulated signal into the output circuit is prevented by filters, each of high impedance at the frequency of the other signal. In figure 2*b* the connections between the signal and modulated signal circuits are open-circuited periodically by the carrier. In this case each filter should have a low impedance at the other signal frequency. In figures 2*c*, 2*d*, and 2*e* the copper-oxide rectifiers are made to become alternately low and high resistance in pairs as the polarity of the carrier is either in the same direction as the arrows or in the opposite direction. As a result current flow from the input signal circuit into the output is periodically reversed by provision of a periodically reversing low-impedance path. In effect each signal is balanced from the other's circuit.

Although an indefinite number of other circuit configurations can be used, no novel transmission feature would be found which was not present in the five circuits already shown. Third order modulators in which the copper oxide is arranged to give equal interruptions to the signal in both positive and negative half cycles of the carrier are exceptions not considered here. In addition, circuits like Hartley's, in which phase discriminations have been obtained in the sideband outputs from two modulators by altering both the carrier and signal input phase of one, can be viewed as composed of two modulators of any of the types illustrated.

In these copper-oxide modulators all modulation product frequencies can be grouped into four classes:

$n_oc \pm n_ov$
$n_oc \pm n_ev$
$n_ec \pm n_ov$
$n_ec \pm n_ev$

in which c and v are the carrier and input signal frequencies and n_o is any odd number 1, 3, 5, etc., while n_e is any even number 0, 2, 4, 6, etc. If c and v contain more than one frequency each, n_o and n_e are, respectively, the odd and even combinations of all multiples of the c and v frequencies. All frequencies of one of these four types appear together in a specific branch of the modulator circuit; and they will not appear in another branch unless from a dissymmetry among the copper-oxide units or unless inherent in the circuit configuration. The branches in which the modulation products appear are shown in the circuits illustrated. It is apparent that only in the case of the double-balanced circuit of figure 2*c*, are all of these types of products completely separated in different parts of the circuit. In the other circuits shown the classes of products appear together in combinations of two types. In any balanced circuit that can be drawn the above relationships will be found to hold.

Modulation products will be of a type to which the circuit offers some degree of balance, of a type that can be made to vary in importance relative to the signal by adjustment of either the carrier or signal voltage, or of a type to which neither balance nor level adjustment is of any benefit. Satisfactory operation of such modulators requires large carrier voltages relative to those of the signal, so that products like $c \pm v$, $2c \pm v$, $3c \pm v$, etc., tend to be of large magnitude while products like $c \pm 2v$, $c \pm 3v$, etc., tend to be small. Furthermore, the former types can be made to predominate even more over the latter types either by increasing the carrier amplitude or by decreasing the signal levels. A 6-decibel reduction in signal results in 12-decibel reduction of $c \pm 2v$ and 18-decibel reduction in $c \pm 3v$. In any circuit application interferences of this type lend themselves to reduction in so far as carrier power is available for high-signal-level operation, or in so far as noise does not limit for low signal levels. Laboratory measurements of some of these modulation products made during the development of a group modulator for a 12-channel open-wire carrier system are shown in figure 3 for a double-balanced modulator like that of figure 2*c*. Single $^{3}/_{16}$-inch-diameter disks as shown in figure 1 were used in each bridge arm. About 20 to 30 decibels reduction in interference by balance alone is quite readily obtained in the normal run of manufactured copper-oxide rectifier elements, for those products to which the circuit arrangement offers a balance. Any further improvement must be obtained either by closer control of manufacture or disk selection, by artificial balancing with some means such as capacitor-resistance potentiometers, or by statistical averaging through use of numbers of disks in each bridge arm.

In single-channel modulators interferences caused by the signal into its own signal band will occur only in the presence of the signal. In such cases they need be only 20 to 30 decibels below the signal, except in special cases, as for example, modulators for broad-band program channels. In multichannel systems interferences may be produced in the silent channels by the active channels. This kind of interference or crosstalk is ordinarily made to be 70 decibels or more below the wanted signals for commercial telephone service. In such cases overlapping bands of frequencies not improved by level adjustment are avoided by judicious choice of the carrier frequencies.

Circuit Impedance and Loss

In all modulators the carrier serves merely as a means for obtaining a simple periodic variation of the impedance presented to the signals. It is not only immaterial to the signals how this variation is obtained, but the signals also are totally unaware of whether electrical, mechanical, or other means are used, just so long as the signals themselves are unable to affect the time variation of this impedance. In a copper-oxide modulator, only by making the carrier amplitude large compared to the signal amplitudes across the rectifier elements, can the impedance of the rectifiers be made to vary at carrier rather than signal rates. Too large a signal amplitude not only results in the production of undesired frequencies, but also the impedance and loss characteristics of the modulator vary with the signal amplitude. With small signals the carrier energy is used up in maintaining the copper oxide at prescribed impedance values at each instant of time, and none of the modulation products involving the signal receive more than a negligible amount of energy from the carrier. As a result the output signal energy will always be less than that of the input signal, partly because of i^2r losses within the copper oxide, and partly from the diversion of the input signal energy into the energies of the many modulation-product frequencies.

The signal impedance of a copper-oxide modulator is a combination of a characteristic impedance of the rectifier elements and the impedance of the connected circuits at all the modulation-product frequencies. The characteristic impedance of the rectifier can be viewed crudely as an average of the impedance encountered by a small signal over a cycle of the carrier, treating each instantaneous value of the carrier voltage as a d-c bias. If the impedance for small superimposed alternating voltages is measured on a single copper-oxide disk at various direct bias voltages, this impedance generally changes with both bias and frequency. Measurements to 200 kilocycles made on a $^{1}/_{16}$-inch-diameter disk are shown in figure 4. At all negative bias voltages the impedance is a resistance in parallel with a capacitor. This resistance decreases rapidly with increasing frequency while the shunt ca-

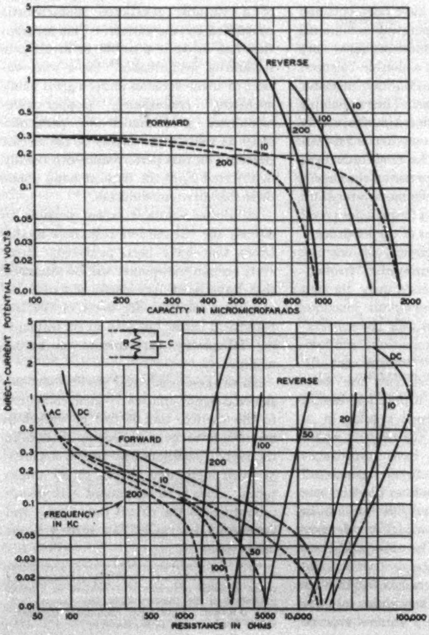


Figure 4. Impedance of a copper-oxide disk at various forward and reverse direct voltages for small superposed alternating voltage

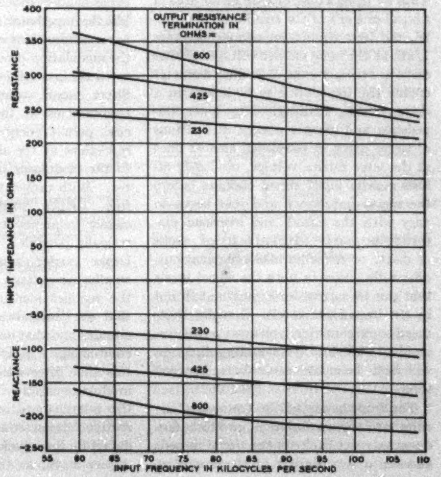
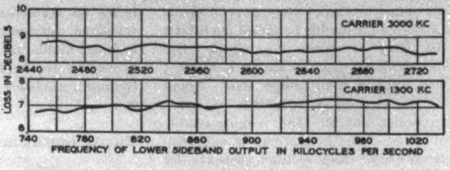


Figure 5. Impedance of a representative double-balanced modulator

Figure 6. Loss in a double-balanced group modulator for coaxial systems



pacity decreases only a moderate amount. At moderate positive voltages (about one-half volt) the impedance becomes resistive and does not change appreciably with frequency. Experimentally it is found that the signal impedances in resistance-terminated modulators can be made largely free of reactance at high frequencies by using either large carrier amplitudes, inductive tuning of the copper-oxide capacities, or lower-impedance-connected signal circuits to accentuate the importance of the low-resistance part of the copper-oxide characteristic. Very much lower circuit impedances must be used at the higher frequencies. Where 600 to 1,000 ohms is a satisfactory impedance at speech frequencies, less than 50 ohms may be the best impedance to use, at three megacycles.

Impedance measurements on a double-balanced modulator designed to translate a 12-channel group of frequencies for cable carrier systems from a band at 60 to 108 kilocycles to a 12- to 60-kilocycle band are shown in figure 5 for several resistance terminations. Absence of any impedance irregularities with frequency is apparent. Also, the tendency is shown for the modulator impedance to become less reactive with lower-resistance terminations.

Inasmuch as copper-oxide disks are available in sizes from $^{1}/_{16}$ inch to more than an inch in diameter, a wide range of circuit impedances is possible varying from only a few ohms to thousands of ohms Large-area disks roughly are equivalent to small-area disks in parallel. Thus by using a disk of *n* times the area of a small one or *n* of the small ones in parallel, the best circuit impedance becomes 1/*n*th at the same carrier voltage. Either disks in series or ones of smaller diameter enable the impedance to be raised in a corresponding manner. The lower impedance and greater energy dissipations of larger disks, or paralleled smaller ones at the same carrier voltage, obviously allows greater input signal energies before the signal impedance and loss begin to vary with the signal, and overload distortion appears. Similarly series stacks of disks, or series-parallel combinations, offer wide choice in both the signal levels that can be satisfactorily modulated and in the impedance levels. Usually root-mean-square carrier voltages across individual disks in the conducting direction will best be made somewhere between three-tenths and three-fourths volts.

The impedances of the connected circuits at the modulation product frequencies react back on the signal impedances in a way similar to the way that the two terminating impedances of a four-terminal linear network react on each other. In the case of the copper-oxide modulator a reaction from some modulation product back into the signal impedance is less and less as the product becomes of higher order, or as the circuit loss to it becomes greater. Where the impedances of the connected circuits have bothersome interactions with the copper-oxide impedance either at the signal frequencies or the lower-loss modulation products, resistance-pad separation is usually the simplest solution if the increased loss can be tolerated.

Energy losses in copper-oxide modulators between signal input and single-sideband signal output have been found to be no greater than eight or nine decibels even at frequencies of three or four megacycles. At lower frequencies five- or six-decibel losses are normal, but losses as low as two decibels have been obtained under less practical operating conditions. Experimental loss measurements are shown in figure 6 for a double-balanced modulator using single $^{3}/_{16}$-inch-diameter disks in each bridge arm. This modulator was designed to modulate simultaneously 60 speech channels occupying a 240,000-cycle band width. The modulator loss, like the impedance, depends on the impedance terminations of the modulator at all the modulation-product frequencies as well as on the internal losses of the modulator. Short circuit, open circuit, or reactive terminations at the unwanted frequencies, permit energy losses only through reflections at the signal-circuit junctions to the modulator or within the modulator. With proper terminations and loss-free copper oxide, 100-per-cent-efficiency frequency translations are theoretically possible. In a practical case, a larger carrier amplitude results in a smaller percentage of the time in which the rectifier elements have impedances that are comparable to the connected circuits and that are neither blocking nor conducting. Signal energies are lost in this time interval, so a higher efficiency modulator results. The time spent on the intermediate resistance parts of the rectifier characteristic can be further reduced by introducing harmonics into the carrier wave, so that a square type of wave results. The resistance of the rectifier is abruptly switched back and forth between blocking and conducting values in this manner. When the connected circuit impedances at the unwanted frequencies are very high or low, best efficiencies result when transmission between the signal circuits is blocked most of the time. Thus in a circuit like that of figure 2*a* when the filters are high impedance at the unwanted products, highest efficiency results when the copper oxide is a low-resistance short circuit for the major portion of the carrier cycle. In figure 2*b* an open circuit is desirable most of the time.

Linear Modulator Theory

The analytical studies that have been of most benefit in the development of copper-oxide modulators have made use of a variable resistance characteristic controlled by the carrier. This assumption has made it possible to investigate modulator performance[*] for a wide variety of characteristics under a great many operating conditions. Copper-oxide-modulator performance in particular cases as well as the effects of the circuit elements on this performance can readily be inferred from the data at hand about these idealized modulators.

In limited space it is not possible to discuss the varieties of resistance modulators that have been analyzed. However, certain viewpoints will be discussed that have been very useful not only for obtaining solutions for some of the hypothetical cases, but also in supplementing laboratory experiments on actual modulators.

All of these analytical studies have assumed a signal sufficiently small compared to the carrier, that it can be varied in magnitude without noticeable changes in the signal impedance or in the linearity between input and output signal amplitudes. This is in agreement with design procedure, as the circuit impedances and losses are determined on such a linear basis.

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[*] A physical picture of modulator performance in terms of linear networks is developed in a paper published in the January 1939 *Bell System Technical Journal:* "Equivalent Modulator Circuits," by E. Peterson and L. W. Hussey.

SUPERPOSITION PRINCIPLE

All of the modulator circuits with which we have been dealing, though composed of nonlinear elements, have been resolved into the equivalent of linear systems by virtue of using a large carrier and small signal amplitude. We may simultaneously apply any number of signal frequencies, but all have negligible effect on the periodic changing of the nonlinear element resistance by the carrier. These frequencies may be modulation product voltages, some applied at the output terminals and some at the input terminals, but in all cases, even though frequencies may coincide, it can be shown that the principle of superposition will hold without interaction between the applied forces and the responses. This permits a great simplification in the mathematical approach to modulator analysis, because the modulation product or signal voltages can be applied one at a time and the current responses summed. The voltage-current ratios at each frequency can then be replaced by equivalent impedances.

Any nonlinear resistance like copper oxide will have a current-voltage characteristic that can be expressed as accurately as desired by

$$i = a_1e + a_2e^2 + \ldots a_ne^n \qquad (1)$$

If a carrier and signal voltage are applied to this nonlinear element, each term beyond the first will independently produce currents of new frequencies composed of the intermodulation products of these two voltages. If in turn the external impedances at these new frequencies are not zero, new voltages will appear across the nonlinear element to produce still more new frequencies. In this case the simplest consideration is to minimize the number of voltages by presenting zero impedance to the modulation products. If the carrier voltage is $C \cos ct$ and the input signal voltage $S \cos st$, the current flow in the nth term is

$$i = a_n\,(C \cos ct + S \cos st)^n \qquad (2)$$

In the binomial expansion of this expression it is obvious that linear response to the signal and freedom from distortion result when the ratio of carrier to signal is made sufficiently large so that only the first two terms are important.

$$i \approx a_n[(C \cos ct)^n + n(C \cos ct)^{n-1}\cdot S \cos st] \qquad (3)$$

The first term in equation 3 is the current flow at direct current and harmonics of the carrier; it has no effect on the input signal and output signal except in so far as impedance termination presented across the nonlinear element at the carrier harmonic frequencies may alter the carrier-voltage harmonic content. The signal input current and the signal output current, as well as the unwanted modulation products of the signal, result from the second term. The even values of n produce the even-order sidebands, second order being the output signal, while the odd values of n produce the input signal current and the odd-order sidebands. These currents can be evaluated from

$$\cos^n b = \frac{K^n_{\frac{n}{2}}}{2^n} + \sum_{m=1}^{m=n} \frac{K^n_{\frac{n-m}{2}}}{2^{n-1}} \cos mb$$

in which $K^n_{\frac{n-m}{2}}$ is equal to the combination of n things taken $\dfrac{n-m}{2}$ at a time for $\dfrac{n-m}{2}$ integral and is equal to zero for $\dfrac{n-m}{2}$ nonintegral. The signal input current is

$$i_s = \frac{a_n n K^{n-1}_{\frac{n-1}{2}}}{2^{n-1}}\, C^{n-1}\, S \cos st \qquad (4)$$

while the second-order output signal sideband is

$$i_{c\pm s} = \frac{a_n n K^{n-1}_{\frac{n-2}{2}}}{2^{n-1}}\, C^{n-1}\, S \cos (c \pm s)t \qquad (5)$$

Similarly, if the output signal voltage at second-order sideband frequency $(c \pm s)$ had been applied along with the carrier in place of the input signal, and of an equal amplitude, then the following currents of the output and input signal frequencies would result

$$i_{c\pm s} = \frac{a_n n K^{n-1}_{\frac{n-1}{2}}}{2^{n-1}}\, C^{n-1}\, S \cos (c \pm s)t \qquad (6)$$

$$i_s = \frac{a_n n K^{n-1}_{\frac{n-2}{2}}}{2^{n-1}}\, C^{n-1}\, S \cos st \qquad (7)$$

If both signal input and output frequency voltages are applied simultaneously, equation 3 then becomes

$$i \approx a_n[(C \cos ct)^n + n(C \cos ct)^{n-1}\cdot S \cos st + n(C \cos ct)^{n-1}\cdot S \cos (c \pm s)t] \qquad (8)$$

The current responses obviously are the sum of the separate responses from independent application of the two frequencies. Even if a complex array of terminating impedances are supplied so that voltages appear across the nonlinear element at all the modulation product frequencies, each new voltage will individually produce its own current response, quite independently of the responses that are being produced by the other voltages. It can readily be seen then that superposition does not depend on any assumptions about what the terminating impedances may be.

Reciprocal Theorem

Equations 5 and 7 show that the sideband response to an input signal voltage is exactly equal in magnitude to the input signal response to the same amplitude sideband voltage. It can readily be seen that any two modulation products also bear such a reciprocal relationship between their voltages and currents, as a result of using the same amplitude and frequency of carrier harmonic multiplier of their respective voltages to modulate between the two frequency positions. Although reciprocity has been proved valid here only for short-circuit terminations at the modulation-product frequencies, it can also be proved under numerous other conditions of circuit operation. It seems that, regardless of modulator complexity of impedance terminations or frequency loss effects, *the reciprocal theorem is a necessary attribute of such a linear and bilateral system in which there are no internal energy sources.* Two-way systems in which an amplifier for example, is included as an internal energy source in one or both directions will, of course, violate the reciprocal theorem if the gains in the two directions are different. This arrangement is, however, both bilateral and linear.

COMPLETE PERFORMANCE CRITERIA

The laws for transmission between a signal input frequency and a signal output frequency can be completely specified from open- and short-circuit impedance measurements at the signal input and output frequencies, regardless of the complexity of the modulator. (From such measurements optimum impedance terminations can even be determined for linear-bilateral systems with internal energy sources.)

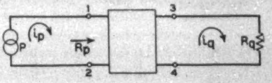

Figure 7. Four-terminal network equivalent of a linear modulator

The four-terminal network of figure 7 is assumed to represent a modulator with a large carrier amplitude and having small signal voltage P of frequency p applied at the input terminals 1–2 at the left. Current of the output signal frequency q flows out of the terminals 3–4 into the impedance R_q. The generator P is assumed to have zero internal impedance at its own frequency. Impedance terminations at the 1–2 and 3–4 terminals at all other modulation-product frequencies are perfectly general; whatever they are in a particular case, it is assumed that they are undisturbed as the terminations of the input and output at the signal frequencies are varied between open circuit and short circuit. The following symbols are used for the impedances looking into the modulator at the input terminals at input signal frequency p and at the output terminals at signal output frequency q.

Z_{po} = impedance at p for open circuit at q
Z_{qo} = impedance at q for open circuit at p
Z_{ps} = impedance at p for short circuit at q
Z_{qs} = impedance at q for short circuit at p
R_q = impedance termination at frequency q
R_p = impedance of modulator at frequency p with R_q at 3–4 terminals for frequency q
K_a = transfer admittance between voltage at frequency p applied at the 1–2 terminals and short-circuit current at frequency q flowing from 3–4 terminals
K_b = transfer admittance between voltage applied at 3–4 terminals at frequency q and short-circuit current at frequency p flowing from 1–2 terminals

For R_q first short-circuited.

$$i_{q1} = PK_a \tag{9}$$

$$i_{p1} = \frac{P}{Z_{ps}} \tag{10}$$

If the short circuit on R_q is removed, the following two additional currents will flow due to superposition of a new voltage $-i_qR_q$

$$i_{q2} = \frac{-i_qR_q}{Z_{qs}} \tag{11}$$

and

$$i_{p2} = -i_qR_qK_b \tag{12}$$

$$i_q = i_{q1} + i_{q2} = \frac{PK_a}{1+\dfrac{R_q}{Z_{qs}}} \tag{13}$$

$$i_p = i_{p1} + i_{p2} = \frac{P}{Z_{ps}} - \frac{PK_aK_bR_q}{1+\dfrac{R_q}{Z_{qs}}} \tag{14}$$

The efficiency of the frequency translation, measured by the ratio of the power delivered to R_q to the power into terminals 1–2, is

$$\eta = \frac{i_q^2R_q}{i_pP} = \frac{\left(\dfrac{K_a}{1+\dfrac{R_q}{Z_{qs}}}\right)^2 R_q}{\dfrac{1}{Z_{ps}} - \dfrac{K_aK_bR_q}{1+\dfrac{R_q}{Z_{qs}}}} \tag{15}$$

which is maximum for

$$R_q = \frac{Z_{qs}}{\sqrt{1-K_aK_bZ_{ps}Z_{qs}}} \tag{16}$$

The maximum efficiency is then

$$\eta_{\max} = \frac{K_a^2Z_{ps}Z_{qs}}{(1+\sqrt{1-K_aK_bZ_{ps}Z_{qs}})^2} \tag{17}$$

Table I. Correspondence Between Currents Entering and Leaving a Reversing-Switch Modulator

Component on One Side of Modulator	Corresponding Components on Other Side of the Modulator							
	$\frac{2k^*}{\pi}$		$-\frac{2k}{3\pi}$		$\frac{2k}{5\pi}$		$-\frac{2k}{7\pi}$	
I_0	I_{1-}	I_{1+}	I_{3-}	I_{3+}	I_{5-}	I_{5+}	I_{7-}	I_{7+}
I_{1-}	I_0	I_{2-}	I_{2+}	I_{4-}	I_{4+}	I_{6-}	I_{6+}	I_{8-}
I_{1+}	I_0	I_{2+}	I_{2-}	I_{4+}	I_{4-}	I_{6+}	I_{6-}	I_{8+}
I_{2-}	I_{1-}	I_{3-}	I_{1+}	I_{5-}	I_{3+}	I_{7-}	I_{5+}	I_{9-}
I_{2+}	I_{1+}	I_{3+}	I_{1-}	I_{5+}	I_{3-}	I_{7+}	I_{5-}	I_{9+}
I_{3-}	I_{2-}	I_{4-}	I_0	I_{6-}	I_{2+}	I_{8-}	I_{4+}	I_{10-}
I_{3+}	I_{2+}	I_{4+}	I_0	I_{6+}	I_{2-}	I_{8+}	I_{4-}	I_{10+}
I_{4-}	I_{3-}	I_{5-}	I_{1-}	I_{7-}	I_{1+}	I_{9-}	I_{3+}	I_{11-}
I_{4+}	I_{3+}	I_{5+}	I_{1+}	I_{7+}	I_{1-}	I_{9+}	I_{3-}	I_{11+}
I_{5-}	I_{4-}	I_{6-}	I_{2-}	I_{8-}	I_0	I_{10-}	I_{2+}	I_{12-}
I_{5+}	I_{4+}	I_{6+}	I_{2+}	I_8	I_0	I_{10+}	I_{2-}	I_{12+}
I_{6-}	I_{5-}	I_{7-}	I_{3-}	I_{9-}	I_{1-}	I_{11-}	I_{1+}	I_{13-}
I_{6+}	I_{5+}	I_{7+}	I_{3+}	I_{9+}	I_{1+}	I_{11+}	I_{1-}	I_{13+}
I_{7-}	I_{6-}	I_{8-}	I_{4-}	I_{10-}	I_{2-}	I_{12-}	I_0	I_{14-}
I_{7+}	I_{6+}	I_{8+}	I_{4+}	I_{10+}	I_{2+}	I_{12+}	I_0	I_{14+}
I_{8-}	I_{7-}	I_{9-}	I_{5-}	I_{11-}	I_{3-}	I_{13-}	I_{1-}	I_{15-}

NOTE: A current of the frequency indicated in the first column will be modulated to produce the components written on the same line, the magnitudes of which are the magnitude of the generating current multiplied by the factors at the top of the columns.

$$^*k = \left(\sqrt{\frac{R_r}{R_f}} - 1\right) \Big/ \left(\sqrt{\frac{R_r}{R_f}} + 1\right)$$

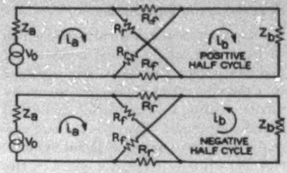

Figure 8. Equivalent circuit of a double-balanced modulator

When equation 16 is substituted in (14) it is found that

$$R_p = \frac{P}{i_p} = \frac{Z_{ps}}{\sqrt{1-K_aK_bZ_{ps}Z_{qs}}} \tag{18}$$

In order to evaluate K_aK_b, open-circuit impedance measurements must also be made. By superposition methods like those used in obtaining equations 11 and 12 it can readily be shown that

$$\frac{1}{Z_{po}} = \frac{1}{Z_{ps}} - K_aK_bZ_{qs} \tag{19}$$

$$\frac{1}{Z_{qo}} = \frac{1}{Z_{qs}} - K_aK_bZ_{ps} \tag{20}$$

From these two equations, it follows that

$$\frac{Z_{ps}}{Z_{po}} = \frac{Z_{qs}}{Z_{qo}} \tag{21}$$

$$K_aK_b = \frac{\dfrac{1}{Z_{ps}} - \dfrac{1}{Z_{po}}}{Z_{qs}} \tag{22}$$

and K_aK_b can be determined from any three of the open-short measurements by using (21) and (22). It follows that

$$K_aK_bZ_{ps}Z_{qs} = 1 - \frac{Z_{ps}}{Z_{po}} \tag{23}$$

Upon substitution in (16) and (18) it is found that

$$R_q = \sqrt{Z_{qo}Z_{qs}} \tag{24}$$

and

$$R_p = \sqrt{Z_{po}Z_{ps}} \tag{25}$$

when 3–4 is terminated in R_q for maximum efficiency.

Open- and short-circuit measurements enable us to compute the optimum efficiency from equation 17 only if the transfer admittance K_a is known. If the reciprocal theorem holds, $K_a = K_b$ and K_a can be determined from (23). The optimum efficiency is then

$$\eta_{\max} = \frac{1-\sqrt{\dfrac{Z_{ps}}{Z_{po}}}}{1+\sqrt{\dfrac{Z_{ps}}{Z_{po}}}} \tag{26}$$

Table II. Performance of Double-Balanced Modulator (Ideal Reversing Switch) for Various Input and Output Terminations

Modulator Terminations				Modulator Impedance		Modulator Loss or Efficiency	
Input Circuit		Output Circuit					
Signal	Others	Signal	Others	Input Signal	Output Signal	Voltage Ratio	Decibels
R	R	R	R	R	R	$\frac{2}{\pi}$	3.9
R	any value	R	R	R		$\frac{2}{\pi}$	3.9
R	R	R	0	$\frac{2R}{\pi^2-2}$	R	$\frac{2}{\pi}$	3.9
R	R	R	∞	$\frac{(\pi^2-2)R}{2}$	R	$\frac{2}{\pi}$	3.9
R	$\frac{(\pi^2-2)R}{2}$	$\frac{(\pi^2-2)R}{2}$	0	R	$\frac{\pi^2(\pi^2-2)R}{6\pi^2-16}$	$\frac{\pi}{\sqrt{2(\pi^2-2)}}$	2
R	$\frac{2R}{\pi^2-2}$	$\frac{2R}{\pi^2-2}$	∞	R	$\frac{(6\pi^2-16)R}{\pi^2(\pi^2-2)}$	$\frac{\pi}{\sqrt{2(\pi^2-2)}}$	2
R	0	R	0	0	0		∞
R	∞	R	∞	∞	∞		∞
R	0	R	∞	$\frac{\pi^2 R}{4}$	$\frac{4}{\pi^2}R$	$\frac{4\pi}{4+\pi^2}$	0.85
R	∞	R	0	$\frac{4}{\pi^2}R$	$\frac{\pi^2}{4}R$	$\frac{4\pi}{4+\pi^2}$	0.85
R	0	$\frac{4}{\pi^2}R$	∞	R	$\frac{4}{\pi^2}R$	1	0
R	∞	$\frac{\pi^2}{4}R$	0	R	$\frac{\pi^2}{4}R$	1	0
R_s	R_s'	R_r	R_r'	Z_i*	Z_0*	η*	

$$*Z_i = \frac{\pi^2 R_r'(R_r + R_s') + 4(R_r - R_r')R_s'}{\pi^2(R_r + R_s') - 4(R_r - R_r')} \qquad Z_0 = \frac{\pi^2 R_s'(R_s + R_r') + 4(R_s - R_s')R_r'}{\pi^2(R_s + R_r') - 4(R_s - R_s')}$$

$$\eta = \frac{4\pi(R_r' + R_s')\sqrt{R_s R_r}}{\pi^2(R_s + R_r')(R_r + R_s') - 4(R_r - R_r')(R_s - R_s')}$$

If the input signal generator has an internal impedance, most efficient energy delivery to the modulator will, of course, result if this impedance is made equal to R_p.

Equivalent T, π, and bridge networks can obviously be drawn from the open- and short-circuit measurements as in four-terminal linear networks.

It appears that even in a plate- or grid-circuit modulator the formulas of equations 24, 25, and 26 can be applied to the plate or grid circuit, respectively, where the signals are small compared to the carrier, inasmuch as the modulating parts of these circuits are linear and bilateral with no internal energy sources.

DOUBLE-BALANCED OR REVERSING-SWITCH MODULATOR*

A number of interesting conclusions can be reached about copper-oxide modulators by assuming that the copper oxide acts like a switch having a low resistance value when the positive half-cycle of the carrier voltage is across the disk and a high resistance value during the negative half-cycle. The circuits of figures 2*c*, 2*d*, or 2*e* can then be represented by the equivalent circuit of figure 8.

The input signal generator V_0 is applied out of an input circuit impedance Z_a, and the output signal is delivered to the output circuit impedance Z_b. Both Z_a and Z_b in general will be functions of frequency.

If $Z_a = Z_b = R$ at all frequencies it can easily be shown that the circuit will operate most efficiently for

$$R = \sqrt{R_r R_f} \tag{27}$$

The signal input current will then be the only frequency in i_a.

$$i_a = \frac{V_0}{2R} \tag{28}$$

From the properties of lattice networks it follows, that

$$i_b = \frac{V_0}{2R}\cdot\frac{\sqrt{\dfrac{R_r}{R_f}} - 1}{\sqrt{\dfrac{R_r}{R_f}} + 1}\cdot f(t) \tag{29}$$

where

$$f(t) = \begin{cases} +1 \text{ for positive half-cycle of carrier} \\ -1 \text{ for negative half-cycle of carrier} \end{cases}$$

Letting

$$f(t) = \frac{4}{\pi}\left(\cos ct - \frac{1}{3}\cos 3\,ct + \frac{1}{5}\cos 5\,ct \ldots\right)$$

and

$$k = \frac{\sqrt{\dfrac{R_r}{R_f}} - 1}{\sqrt{\dfrac{R_r}{R_f}} + 1} \tag{30}$$

$$i_b = \frac{V_0 k}{2R}\cdot\frac{4}{\pi}\left[\cos ct - \frac{1}{3}\cos 3ct + \frac{1}{5}\cos 5ct \ldots\right] \tag{31}$$

The magnitude of the single-sideband output current is

$$i_{1+} = i_{1-} = \frac{2k}{\pi}\cdot\frac{V_0}{2R} \tag{32}$$

and the efficiency

$$\eta = \frac{4}{\pi^2}k^2 \tag{33}$$

In addition currents flow in the output circuit at both sidebands of all odd harmonics of the carrier frequency. These can be represented by i_{3+}, i_{3-}, i_{5+}, etc. No currents flow at the sideband frequencies of the even harmonics of the carrier, i_{2+}, i_{2-}, . . .

If V_0 should be replaced by an equal amplitude generator at any of the sideband frequencies, V_{1+}, V_{3-}, etc., then the input current would in any case be

$$i_{1+} = \frac{V_{1+}}{2R},\ I_{3-} = \frac{V_{3-}}{2R}, \text{ etc.}$$

The correspondence between the magnitudes of these entering currents and the magnitudes of the output currents at the modulation product frequencies are shown in table I. Reciprocal relations between the driving voltage at one frequency and the output current at another frequency are obvious.

GENERALIZED REFLECTION THEORY

Superposition permits us to apply simultaneously driving forces of the frequencies tabulated above in any relative phases and amplitudes that we care to choose on either side of the modulator. If simultaneously I_0 is applied on one side of the modulator and

$$(I_{1+})\ \frac{2k}{\pi}\cdot\frac{Z_{1+} - R}{Z_{1+} + R}$$

is applied to the other set of modulator terminals, then the total current at the output terminals at the sideband frequency (1+) will be

$$(I_{1+})\frac{2k}{\pi}\cdot\left[1 - \frac{Z_{1+} - R}{Z_{1+} + R}\right] \tag{34}$$

This is equivalent to saying that a resistance R at the sideband frequency (1+)

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* Referred to in the German literature as the "ring modulator."

The Magnesium-Copper Sulphide Rectifier Battery Charger for Railroad Passenger Cars

By C. A. KOTTERMAN
NONMEMBER AIEE

Synopsis: When the railroads began air conditioning their trains, adequate charging of the storage batteries associated with air-conditioning systems became an important factor in the new development. The capacity of the axle driven d-c generator charger was increased; however, supplementary charging was frequently necessary or desirable, particularly when the axle generators were not operating. The railroads met this problem by using portable motor generator sets, wiring their stations and yards with standby service and through the use of small, compact, dry-disk rectifier battery chargers of sufficient current capacities to charge one or two car batteries simultaneously. The first rectifier developed for this service made use of the magnesium-copper sulphide dry-disk type. The mechanism of this rectifier is here briefly discussed, design features enumerated, and the type of rectifier battery chargers developed for railroad use described.

STORAGE BATTERIES were first added to railroad passenger and Pullman coaches about 40 years ago to provide power for electric lights which replaced the gas and oil lamps. These batteries were charged by means of axle-driven d-c generators. Electric ventilating fans were added later but under normal operating conditions the generators adequately charged the batteries. With the advent, however, of electromechanical air-conditioning equipment on railroad cars demanding d-c currents varying from 150 to nearly 300 amperes, an entirely new concept of storage-battery equipment and charging had to be worked out. In a few short years the battery load has increased from 25 to 50 amperes to nearly 300 amperes per car on some of the present-day streamlined completely air-conditioned trains. The combined efforts of storage-battery, air-conditioning, and rolling-stock engineers have accomplished a truly remarkable result in providing high-intensity indirect illumination together with air conditioning on modern trains.

This improvement in the traveling public's comfort and convenience has created many difficult problems which had to be solved. Among them may be mentioned the development of a 1,000-ampere-hour-capacity storage battery small enough in size and weight to be mounted on a coach. Adequate charging facilities had to be developed to meet the increased loads on batteries. All of this has become a reality without increasing the size of the trucks or other parts of the cars. Under normal train operating conditions, the axle-driven generator operates enough of the time to charge the batteries adequately. During the summer months, however, when the air-conditioning equipment is used to the limit to meet hot-weather conditions, supplementary or so-called "yard" charging often becomes necessary. Also, in bad winter weather when snow and ice affect the axle drive and the battery ampere-hour capacity is lowered, supplementary charging is also required.

The railroads have handled this situation in several ways. Some coach yards and stations have been equipped with d-c charging lines supplied by one or more motor generator sets. This is a costly system to install, requiring quite an investment in copper feeder cables to handle the large charging currents necessary without introducing too much *IR* drop in the charging lines.

Portable or mobile motor generator sets usually of sufficient capacity to charge the batteries on two cars simultaneously are also used. They meet the need for supplementary charging equipment but their weight and lack of easy maneuverability often restrict their use in badly congested coach yards, particularly in the winter with snow and ice hampering their movement.

Some railroads have installed 220-volt 60-cycle three-phase systems for standby service in their yards and stations. Many of the air-conditioning systems on trains include a 10–20-horsepower three-phase motor for driving the compressor associated with the air-conditioning equipment on a standing car. This is extra weight that has to be carried with the car for the sole purpose of providing standby service. The 220-volt a-c supply is, however, readily available for battery charging purposes and at a reasonable cost.

About two years ago, when the charging problem was fully realized, the P. R.

Paper number 39-22, recommended by the AIEE committee on transportation, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 10, 1938; made available for preprinting December 14, 1938.

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has been connected to the output terminals of the modulator and in this resistance is an internal zero-impedance generator of voltage

$$2R(I_{1+})\ \frac{2k}{\pi}\cdot\frac{Z_{1+}-R}{Z_{1+}+R} \qquad (35)$$

This resistance R at sideband frequency $(1+)$ must be infinite at all other frequencies, if in parallel we assume another resistance of R at all frequencies except $(1+)$ at which it is infinite.

The equivalent impedance at frequency $(1+)$ at the modulator terminals connected to the $(1+)$ resistance with its internal generator, is the ratio of $(1+)$ voltage to $(1+)$ current.

$$Z=\frac{\frac{2k}{\pi}I_{1+}\cdot 2R-\frac{2k}{\pi}\cdot I_{1+}\cdot\left[1-\frac{Z_{1+}-R}{Z_{1+}+R}\right]R}{\frac{2k}{\pi}I_{1+}\cdot\left[1-\frac{Z_{1+}-R}{Z_{1+}+R}\right]} \qquad (36)$$

which reduces to

$$Z = Z_{1+} \qquad (37)$$

Z_{1+} may be real or complex as it involves only the amplitude and phase of the superimposed voltage of upper-sideband frequency. It can readily be seen then that the solution for current flow at this frequency of equation 34 is identical with the case of linear networks in which the current is expressed as that flowing in a matched circuit modified by a reflection factor. Reflection from any modulation-product frequency can be similarly treated.

A number of cases have been worked out of efficiencies and impedances in such modulators for transmission between an input signal and a single-sideband output signal. The modulating element has been assumed perfect (k = 1) and the terminations pure resistances. The results are shown in table II.

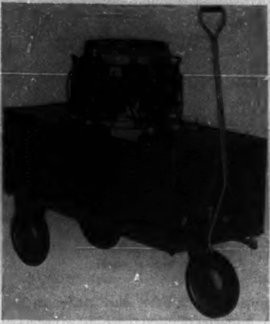


Figure 1. Mobile charger with louvred weatherproof cover removed

Mallory and Company, Inc., working in close co-operation with engineers of several railroads, evolved a heavy-duty dry-disk type of rectifier for converting three-phase 60-cycle current to direct current of sufficient current output to handle the air-conditioning load on a standing car or to charge one or two car batteries simultaneously. The current capacity of this rectifying unit together with the small physical size and weight of the charger made possible a practical battery charger easy to wheel around with operation fully automatic and reasonably foolproof. Weighing but 600 pounds with a width of 20 inches, capable of delivering 200 amperes into a 16-cell battery or 100 amperes into a 32-cell battery, this charger can be manipulated in the narrow space between cars in coach yards.

The rectifying element used in these battery chargers is of the contact type employing a magnesium-copper sulphide junction, the invention of Samuel Ruben. Such a rectifier consists essentially of a conductor and semiconductor in more intimate contact than physical juxtaposition and pressure can give.

There are three widely known types of contact rectifiers, namely, the copper-copper oxide, the iron-selenium, and the magnesium-copper sulphide rectifiers. During recent years both the copper-copper oxide and the magnesium-copper sulphide types have been developed to the point where they are capable of handling large currents per unit area. The iron-selenium rectifier has never been considered seriously as a heavy-duty type.

The copper-copper oxide rectifier of the sprayed-plate type has a current capacity of approximately 1.2 amperes per square inch of rectifying area when properly ventilated, the plate junction serving also as its own radiator to provide sufficient cooling area to dissipate heat generated during operation.

The magnesium-copper sulphide rectifying junction has a current capacity of approximately 60 amperes per square inch when properly ventilated. Due to the high current density at which the magnesium-copper sulphide rectifier can be worked, it is possible to build such a rectifier for large d-c outputs in a very small physical size. As a matter of fact, the type illustrated in figure 1 is but 18 inches in diameter by 12 inches high, weighs 100 pounds including ventilating system, and delivers 10 kw. These figures, of course, do not include the three-phase transformer.

Before describing the various types of rectifier battery chargers which have been developed for the railroad application, it is desirable to consider a-c circuit systems in connection with rectifiers for battery charging. Rectified voltages are frequently less than the a-c line voltages, therefore it is necessary to use some form of step-down transformer to reduce the line voltage to a suitable input value to the rectifier. The use of a transformer introduces transformer design and suitable transformer-rectifier circuits into the rectifier picture. The very simplest trans-

Figure 2. Transformer-rectifier circuits

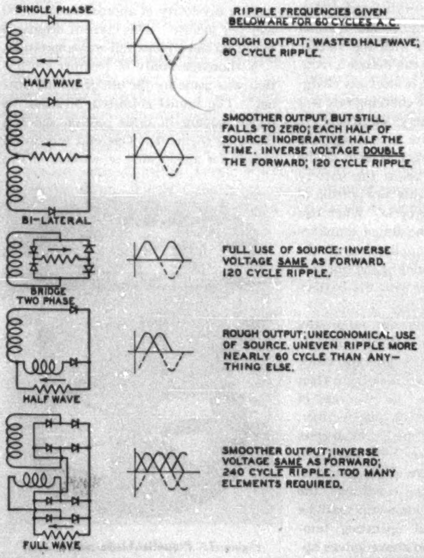
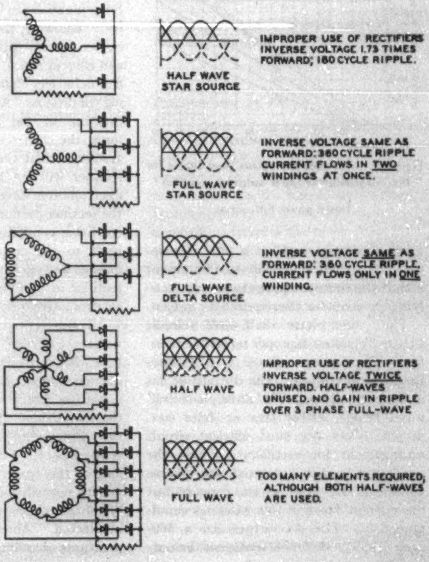

SINGLE PHASE
HALF WAVE
RIPPLE FREQUENCIES GIVEN BELOW ARE FOR 60 CYCLES A.C.
ROUGH OUTPUT; WASTED HALFWAVE; 60 CYCLE RIPPLE.
BI-LATERAL
SMOOTHER OUTPUT, BUT STILL FALLS TO ZERO; EACH HALF OF SOURCE INOPERATIVE HALF THE TIME. INVERSE VOLTAGE DOUBLE THE FORWARD; 120 CYCLE RIPPLE
BRIDGE
FULL USE OF SOURCE: INVERSE VOLTAGE SAME AS FORWARD. 120 CYCLE RIPPLE.
TWO PHASE
HALF WAVE
ROUGH OUTPUT; UNECONOMICAL USE OF SOURCE. UNEVEN RIPPLE MORE NEARLY 60 CYCLE THAN ANYTHING ELSE.
FULL WAVE
SMOOTHER OUTPUT; INVERSE VOLTAGE SAME AS FORWARD; 240 CYCLE RIPPLE. TOO MANY ELEMENTS REQUIRED.

Figure 3. Transformer-rectifier circuits


HALF WAVE STAR SOURCE
IMPROPER USE OF RECTIFIERS INVERSE VOLTAGE 1.73 TIMES FORWARD; 180 CYCLE RIPPLE.
FULL WAVE STAR SOURCE
INVERSE VOLTAGE SAME AS FORWARD: 360 CYCLE RIPPLE CURRENT FLOWS IN TWO WINDINGS AT ONCE.
FULL WAVE DELTA SOURCE
INVERSE VOLTAGE SAME AS FORWARD: 360 CYCLE RIPPLE, CURRENT FLOWS ONLY IN ONE WINDING.
HALF WAVE
IMPROPER USE OF RECTIFIERS INVERSE VOLTAGE TWICE FORWARD. HALF-WAVES UNUSED. NO GAIN IN RIPPLE OVER 3 PHASE FULL-WAVE
FULL WAVE
TOO MANY ELEMENTS REQUIRED; ALTHOUGH BOTH HALF-WAVES ARE USED.

former-rectifier circuit known is illustrated in figure 2. This is a half-wave single-phase circuit producing a very rough d-c output with an inoperative half-wave resulting in a 60-cycle ripple. A somewhat better circuit is the bilateral where the d-c output is smoother but the direct current falls to zero. Each half of the a-c source is inoperative half the time. The inverse voltage per individual rectifier junction is double the forward voltage; however, this circuit has the advantage of a 120-cycle ripple. Probably the most efficient and satisfactory circuit for single-phase operation of rectifiers is the full-wave bridge. This circuit makes full use of the a-c source; the inverse voltage is the same as the forward voltage and the ripple is 120 cycles.

The simplest polyphase circuit is the two-phase half-wave resulting in rough d-c output, uneconomical use of the a-c source, inverse voltage the same as the forward voltage, and has a 240-cycle ripple. The chief objection to this circuit is that the circuit requires too many rectifying elements for the required d-c output.

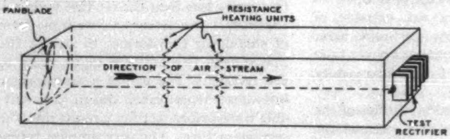


Figure 4. Wind tunnel

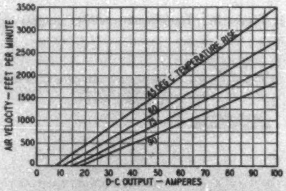


Figure 5. Air velocity versus load curves for the magnesium-copper sulphide rectifier

Three-phase full-wave

The three-phase half-wave circuit (figure 3) makes improper use of the rectifier. The inverse voltage is 1.73 times the forward voltage. The d-c output has a 180-cycle ripple. The three-phase full-wave bridge, either star or delta connected, offers the most efficient circuit arrangement for rectifiers. Where the a-c source is star connected, the inverse voltage is the same as the forward but the current flows in two windings simultaneously. The d-c output has a 360-cycle ripple, a desirable feature if the output has to be filtered. The delta connection of the a-c source has the advantage that current flows only in one winding during a rectifying cycle. The inverse voltage is the same as the forward and the d-c output carries a 360-cycle ripple. The last-mentioned circuit arrangement has been adopted on all our heavy-duty rectifiers operating from three-phase sources.

In designing a rectifier for heavy-current battery charging two essential rectifier fundamentals are of considerable importance. One is voltage ratio, defined as the ratio of direct voltage output to alternating voltage input. Maximum voltage ratio is a function of how much alternating voltage one rectifying junction can have impressed across the junction without breaking down the insulating film between the elements of the junction. The other is the current ratio, defined as the d-c output to a-c input. Maximum current ratio is a function of the maximum safe current density at which the rectifying junction can be operated with stability. Knowing the transformer-rectifier circuit system together with the voltage and current ratios, one can design a rectifier for battery charging to meet any charging conditions. As the charging rate is a function of the battery electromotive force, the chief requisite is to design the system so that the direct voltage of the rectifier will not fall below the battery electromotive force during any portion of the rectifier conducting cycle. When this condition is fulfilled, the design simplifies itself to the proper application of the voltage and current ratios of the sulphide rectifier in conjunction with the battery electromotive force.

All contact rectifiers are essentially resistance devices, therefore they generate heat when rectifying. The life expectancy of such rectifiers is based mainly on their operating temperature. In the case of the magnesium-copper sulphide rectifier, the optimum temperature is 90 degrees centigrade or 190 degrees Fahrenheit. So long as this temperature is not exceeded, during normal operating conditions, the life of the copper sulphide rectifier will be unaffected. Abnormal operating temperatures exceeding the above values appreciably will have some affect on life. As a matter of interest, we have conducted a life test, in excess of 5,000 hours, on a magnesium-copper sulphide rectifier of the type used in our railroad battery chargers, at operating temperatures averaging 130 degrees centigrade or 266 degrees Fahrenheit with little effect on the life of the rectifier. The lower the operating temperature, the longer the life when other operating conditions are normal. Heat generated at the rectifying junction must therefore be removed faster than by simple convection cooling if the rectifier is to deliver large direct currents. The magnesium-copper sulphide rectifier of 1.7 square inches of rectifying area with 3.5 inches square radiator plates or cooling fins will handle 100 amperes direct current safely when placed in a stream of air having a velocity of approximately 2,000 feet per minute. This current density is based on three-phase full-wave operation.

Figure 6. Drum housing with centrifugal-blower wheel

A thorough study of rectifier ventilation was made by the use of a wind tunnel. The tunnel consisted of a box 18 inches square in cross section and ten feet long (figure 4). One end was closed except for an opening or window in the

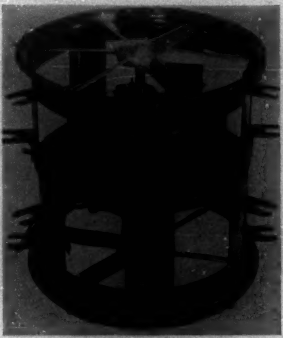


Figure 7. Propeller-blade power dome

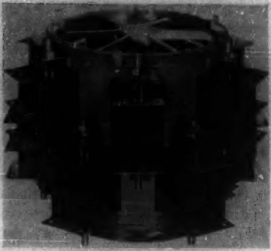


Figure 8. Propeller-blade power dome with rectifiers

center into which the test rectifier was set. The other end of the box was attached to a blower system capable of providing air velocities as high as 5,000 feet per minute. Electric heating elements were placed midway between the ends inside the box to raise the temperature of the air stream for the purpose of studying the behavior of the test rectifiers at elevated temperatures. The compiled data from the wind-tunnel tests enables us to design a ventilating system for any rectifier-transformer combination and to predict within engineering limitations the operating temperature of a power rectifier for any given ambient temperature. Figure 5 is a family of curves, air velocity versus load as obtained by the wind-tunnel tests.

To increase the current-handling capacity of the magnesium-copper sulphide rectifier forced cooling has been developed which led to the study of accelerated air flow through rectifiers. After trying various methods, one was finally evolved of supporting the rectifier elements in the wall of a hollow drum structure, mounting a pressure-type centrifugal blower wheel inside the drum. Figure 6 shows one of the earlier models employing this construction. Air is drawn in through the opening in the center of the fan rotor, then discharged tangentially from the blades through the rectifiers. This permits a very compact construction together with efficient cooling.

The rectifiers are placed symmetrically around the fan-wheel housing. This construction lends itself to very simplified wiring of the rectifiers. The whole structure is known as the "d-c power dome." It is usually supported on top of another housing in which the power transformer is placed. The air discharged through the ventilating fins of the rectifier is first drawn through and around the coils of the transformer, therefore giving the transformer forced cooling. In this way smaller and lighter-weight transformers may be employed.

The centrifugal-type fan wheel is essentially a pressure blower. It delivers large volumes of air with comparatively low velocities. Where the rectifier cooling fins are close together in order to make as compact assembly as possible, high velocity through the fins is desirable. A fan which would deliver high air velocity rather than great volumes of air was desired and this was developed in conjunction with manufacturers of propeller-type fan blades. The method of employing this type fan is shown in figure 7.

A one-third-horsepower motor is supported inside the drum cage. The motor shaft has a double extension so that two fans, one right hand and one left hand, may be rotated simultaneously. Air is drawn in by both propellers building up a pressure inside the drum which is discharged out through the rectifiers mounted in openings in the drum (figure 8).

In the two cooling methods just described, the rectifiers are mounted around a drum or cage. A modification of this principle is employed in a very recent development of forced-cooled rectifiers for large power outputs. In this design the rectifiers are mounted radially in a plane around the motor which drives a single fan. It develops an air pressure inside the housing, forcing the air out through the rectifiers (figure 9).

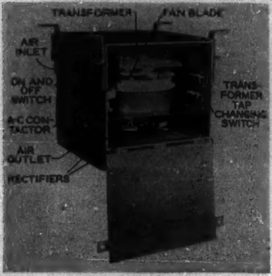


Figure 9. Undercar charger

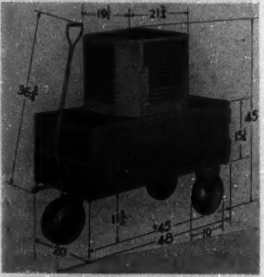


Figure 10. Mobile charger

The railroad battery chargers have been developed in two forms—the "mobile" charger for platform or yard charging and the "on-a-car" type for mounting on and traveling with a car. Both types consist essentially of a three-phase power transformer, three-phase full-wave bridge rectifiers with ventilating system, a chassis or mounting device and housing together with provision for altering the charging rate and compensating for line-voltage variations. Protective circuits are also incorporated.

The mobile charger with principal dimensions shown in figure 10 is manufactured in two types. In one form the charging rate is varied by means of primary taps on the three-phase power transformer (see figure 11). In other models a self-regulating transformer is employed. In this type a sensitive voltage relay across the d-c output of the rectifier maintains a constant d-c output by automatically adjusting the turns ratio of the transformer to compensate for line-voltage variations (figure 14). Figure 12 is a typical charging curve obtained with a charger equipped with a manual control. Figure 13 is a charging curve using the type having the self-regulating transformer.

3
4
5
LEAD
LEAD
EDISON

Figure 11. Mobile-charger control panel showing "wander" plug and tap-changing receptacles

As the life of the rectifying units depends considerably on proper and continuous ventilation, it is essential that the motor associated with the ventilating system be interlocked with the hold-in coil of the main magnetic contactor or be provided with an individual contactor with overload protection interlocked with the main contactor. A single-phase 220-volt motor is employed so that phase reversal will not affect the direction of rotation as the fans must rotate in one direction only. The "on and off" switch which starts and stops a charge is also in the fan circuit. It is provided with a thermal overload so in case of fan motor failure, the a-c power to the transformer will be disconnected.

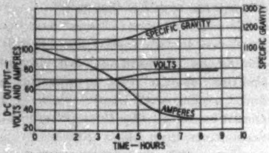


Figure 12. Typical charging curve from manually operated mobile charger

For 600-ampere-hour lead battery

The charging rate on the manually controlled model is varied by means of a "wander" plug and six primary tap receptacles as shown in figure 11. It, however, does not compensate automatically for line-voltage variations. This simple method provides a wide range of charging rates from a few amperes to the full output of the machine and has proved quite adequate for battery charging. Field experience has shown that about 90 per cent of the use of these chargers is to pump as many ampere-hours into a battery as possible in a short time, or to carry the air compressor load of a standing car with the battery floating across the rectifier. Rarely have these periods exceeded three hours.

The charging rate on the automatic regulating type is set by turning a control knob located on the control panel. The charging rate will remain constant until the electromotive force of the battery has risen to a value which can no longer be compensated for by the self-regulating feature at which time the charge will start tapering abruptly, a highly desirable feature in storage-battery charging. The self-regulating feature also maintains a constant charge during line-voltage variation of plus or minus $7^1/_2$ per cent for any rough setting of the three primary line-voltage taps. These rough settings together with the plus or minus $7^1/_2$ per cent regulation provide constant output from 193-volt lines to 253-volt lines.

The fully automatic type also incorporates a current-limiting control which changes the turns ratio of the transformer should the charging rate inadvertently be increased beyond the setting for maximum safe continuous operation.

The rectifiers are wired up in two banks (figure 15), each consisting of two full-wave three-phase bridges in series, providing approximately 37–49 volts d-c output each. A double-pole double-throw switch conveniently mounted enables the two banks to be connected in parallel to provide 37–49 volts at 200 amperes to charge one or more 16-cell lead batteries or 25 cells of Edison batteries, or they may be connected in series to charge a 32-cell lead or 50-cell Edison battery at 100 amperes. Two standard d-c charging receptacles mounted at one end of the charger are connected in parallel so that two batteries may be charged simultaneously. An ammeter shunt is connected into the circuit of each charging receptacle. An ammeter, common to both circuits, indicates the charging current in either charging line. It is practical to charge a 16-cell lead and a 25-cell Edison battery simultaneously if a small series resistor is added to the lead battery charging line.

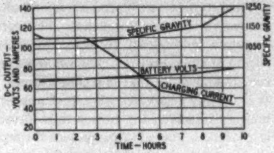
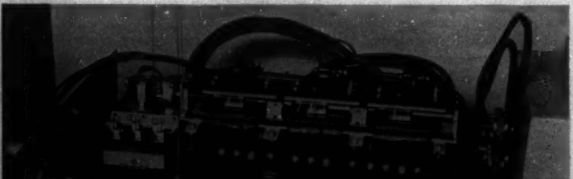


Figure 13. Typical charging curve from self-regulating mobile charger

For 600-ampere-hour lead battery

Both types are equipped with standard railroad three-phase four-wire a-c receptacles, the fourth conductor serving as a ground.

Figure 14. Self-regulating three-phase power transformer



The machines are readily wheeled around on ten-inch diameter, two-inch tread rubber-tired antifriction wheels. As already stated, weighing but 600 pounds and but 20 inches wide, they possess a maximum of maneuverability.

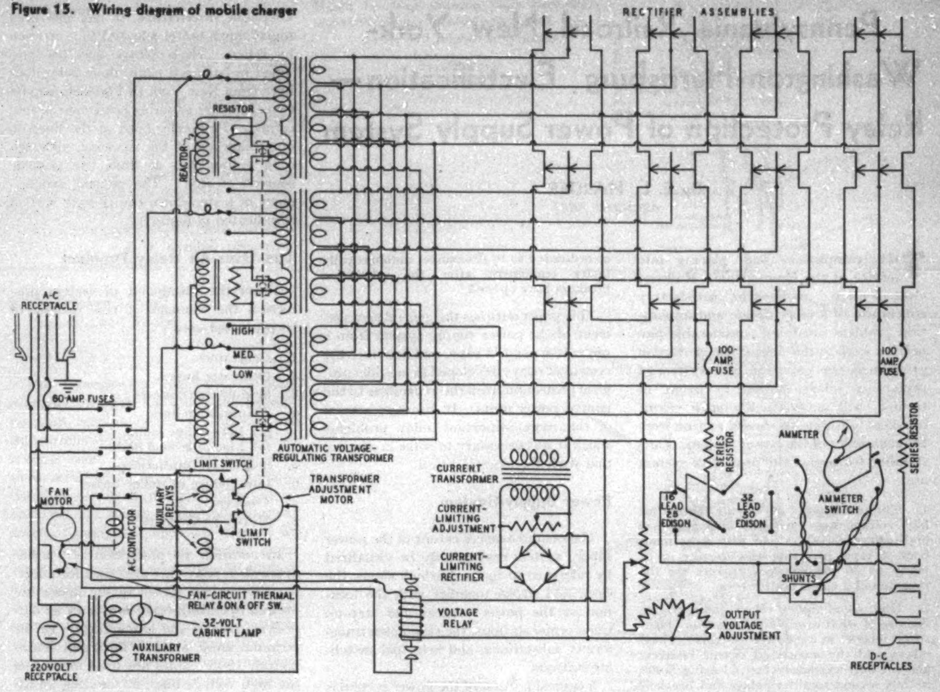
The equipment is fully protected in louvered steel housings and may be operated safely and satisfactorily in any kind of weather.

The "on-a-car" type has been developed in two forms. One shown in figure 16 is intended to be mounted in the clerestory. The other illustrated in figure 9 is enclosed in a ventilated weather-, ballast-, and cinder-proof welded steel housing for mounting underneath the car. Both forms have the same type transformers and rectifiers together with the control and protective features of the mobile types, but because they are designed to charge a particular battery, they do not need to have the flexibility of charging rate of the mobile type. The clerestory form, in operation on several railroads, supplies 100 amperes into a 16-cell lead or 25-cell Edison battery and 80 amperes into a 32-cell lead or 50-cell Edison battery and weighs approximately 290 pounds. Larger-capacity chargers up to 200 amperes into 16-cell lead or 25-cell Edison or 100 amperes into 32-cell lead or 50-cell Edison can be built of small enough size and weight to be mounted in the clerestory if desired.

The undercar type, due to its weatherproof housing, weighs more than the clerestory model but the over-all dimensions are practically the same.

Manual control of the "on-a-car" type battery charger is not needed as the regulation of the transformer-rectifier combination automatically takes care of the charging rate depending upon the specific gravity of the battery. However, where it is necessary or desirable automatically

Figure 15. Wiring diagram of mobile charger


RECTIFIER ASSEMBLIES
RESISTOR
REACTOR
A-C RECEPTACLE
HIGH
MED.
LOW
60 AMP. FUSES
AUTOMATIC VOLTAGE-REGULATING TRANSFORMER
LIMIT SWITCH
FAN MOTOR
AUXILIARY RELAYS
A-C CONTACTOR
TRANSFORMER ADJUSTMENT MOTOR
LIMIT SWITCH
FAN-CIRCUIT THERMAL RELAY & ON & OFF SW.
32-VOLT CABINET LAMP
AUXILIARY TRANSFORMER
220VOLT RECEPTACLE
CURRENT TRANSFORMER
CURRENT-LIMITING ADJUSTMENT
CURRENT-LIMITING RECTIFIER
VOLTAGE RELAY
100-AMP. FUSE
100 AMP. FUSE
SERIES RESISTOR
SERIES RESISTOR
AMMETER
16 LEAD 28 EDISON
32 LEAD 50 EDISON
AMMETER SWITCH
SHUNTS
OUTPUT VOLTAGE ADJUSTMENT
D-C RECEPTACLES

to adjust for line-voltage variation, a self-regulating transformer can be included in the equipment.

Both types operate directly from the 220-volt three-phase standby supply. They require no attention other than connecting or disconnecting this supply. One model is provided with an automatic tapering feature which reduces the charging rate to a mere trickle charge when the voltage of the battery has reached a predetermined value. Should this charger be left on inadvertently for a longer period of time than necessary to complete a normal charging cycle, the trickle rate will automatically come into operation thus preventing undue gassing of the battery.

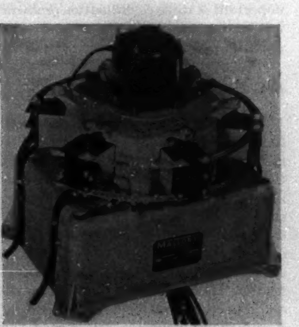


Figure 16. "On-a-car" charger

Although none has been built as yet for this service, designs are ready for an undercar type that will carry the compressor load of a stationary car. This model would eliminate the a-c standby motor and would provide auxiliary charging whenever required.

Rectifier battery chargers possess certain operating features not found in other types. They are light in weight, handle easily, and are compact enough to be used in crowded coach yards. As no heavy rotating parts are employed, they are exceedingly quiet in operation. Maintenance is reduced to a minimum as there are no commutating devices or brush wear. A very desirable feature is that the rectifier battery chargers start on load with a starting current approximately the same as the running current. This enables them to be operated directly off the 220-volt three-phase standby service.

The possibility of polarity reversal is eliminated. This feature together with the fact that the reverse current from the battery through the rectifier is of the order of an ampere, eliminates the need of reverse-current relays.

The magnesium-copper sulphide rectifier battery charger has an over-all efficiency of approximately 50 per cent. The efficiency curve is practically flat from 25 per cent full load to 125 per cent full load. The efficiency of the rectifier itself is about 55 per cent and remains at this value throughout the life of the rectifier. Very recent developments indicate that a higher efficiency may be expected of the copper-sulphide rectifier in the near future, together with greater stability of operation and even longer life than the present type.

Pennsylvania Railroad New York-Washington-Harrisburg Electrification—Relay Protection of Power Supply System

By E. L. HARDER
ASSOCIATE AIEE

THE completion and placing into service of the Pennsylvania Railroad electrification marks the satisfactory conclusion of a relay design and application problem involving considerable pioneering work in this field. The protection problem is the provision of a system of protective relays, which in event of trouble, will automatically open circuit breakers to isolate the faulty section from the remainder of the power system. Outstanding features of the protective system are:

1. The provision of a relaying system for high-voltage lines operated with very few high-voltage breakers and with large numbers of taps, involving the opening of as many as 30 low-voltage breakers for the isolation of a single fault.

2. High-speed trolley relays, inaugurating the use of what are today known as "high-speed relays" in a-c power systems. These relays and the associated circuit breakers are not only extremely fast, clearing faults in 0.04 second total for relays and breaker, but the relays are also very selective, clearing fault currents less than maximum loads and effecting simultaneous operation over 90 per cent of the section length. High-speed a-c circuit breakers, high-speed "distance measuring" relays, transient shunts, and the load presetter, are all new developments associated with this electrification.

3. Use of locomotives and multiple-unit cars not having any main circuit breaker. Trolley circuit breakers at substations open for major faults on the moving equipment. Locomotive and substation relays are so co-ordinated as to disconnect automatically faulty equipment after the substation breakers have opened.

This paper outlines the general arrangement of the power supply system from a protective point of view, and describes the system of relays developed to provide complete protection from the generators to the motive power units. It discusses several of the more important relay problems which it was necessary to solve in connection with the electrification.

Power Supply System

The comprehensive extent of the power supply system may readily be visualized by reference to figure 1 which shows the electrified tracks together with the location of the power sources and step-up transformer stations, the step-down transformer substations, and principal switching stations.

A typical portion of the power system is shown in figure 2. Altogether 25-cycle power is available at seven locations, either by steam or hydro generation, or in most cases by frequency conversion from 60-cycle power systems in the area served. This 25-cycle power is transmitted over nearly 1,100 miles of 132-kv single-phase lines throughout the 674 route miles of electrified territory. It is distributed at 11 kv over 2,189 miles of trolley from 61 transformer substations, the majority of which are located 6 to 12 miles apart.

The absence of 132-kv breakers at step-up and step-down transformer stations is a notable feature. Each transmission-line section with its connected step-up and step-down transformers is cleared as a unit by the low-voltage breakers in event of fault.

The transmission lines are "looped in" to the 25-cycle generating busses through the step-up transformers. This gives rise to the designation "looped-in" system in distinction to a system in which high-voltage lines are continuous past generating stations. While the "looped-in" system was adopted primarily on account of circuit breaker capacity and to limit telephone interference, it has also been found much better adapted to protection by relays. High-voltage lines are provided in balanced pairs, there being four lines from New York to Philadelphia and two elsewhere as far as protection is concerned. Physically both of the lines do not follow all of the trackage, although each station has at least two possible sources of feed. The general arrangement of a step-down transformer station is indicated in figure 3.

The Over-All Relay Problem

From the standpoint of system protection the elements of the system to be protected are:

1. Generators.
2. Generator busses.
3. Step-up transformers.
4. Transmission lines.
    - (*a*) Line-to-line faults.
    - (*b*) Line-to-ground faults.
5. Step-down transformers.
6. Low-voltage or trolley busses.
7. Trolleys.
8. Motive power units.

In addition to protection of specific apparatus, relays are also used for effecting the desired sectionalizing under abnormal operating conditions such as out-of-synchronism and low voltage. In the terminal areas, at New York and Washington, 12-kv cables are used instead of the high-voltage lines, introducing an additional element to be protected. Special protection is also used on the 132-kv cables through Baltimore.

It is evident that the protection of the various elements enumerated must be accomplished with careful consideration of the co-ordination with the protection of other elements so that only the faulty section will be disconnected. The more important of these co-ordination problems will be discussed in the subsequent paragraphs.

A-C Network Calculator

System voltages and currents under fault and load conditions form the fundamental basis for the relay application and the determination of these necessary data is a major problem. For the earlier stages of the electrification many of these data were obtained analytically. However, for the relaying of the complicated system involved in the through electrification, these data were obtained on the a-c network calculator developed by the Westinghouse company and owned and operated by the railroad (figure 4).

Paper number 39-27, recommended by the AIEE committees on transportation and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 30, 1937; made available for preprinting December 15, 1938.

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The suggestions and recommendations of Mr. Griffith, electrical engineer of the Pennsylvania Railroad, and his associates, in the development of this over-all protective system are gratefully acknowledged. Also his helpful assistance in the preparation of this paper. The co-operation of Mr. Watkins and his associates in the consulting firm of Gibbs and Hill are likewise gratefully acknowledged. As stated in the text, the relaying of this system was largely a joint effort of the Pennsylvania Railroad, General Electric Company, and Westinghouse. Only that portion for which the Westinghouse company supplied all or part of the equipment is described.

Generators

In addition to the conventional differential protection, generators are equipped in most cases with induction-type overcurrent relays having time and current selectivity with overcurrent relays used on the transmission lines. An additional machine protective feature is used on some of the frequency changers to prevent building up resonant torsional stresses when slipping poles. This device counts power reversals at high current, and trips for a fixed number occurring within a definite time.[1]

High-Voltage Lines; Line-to-Line Faults

Application Factors

The transmission lines are operated normally balanced and protected by differential current relays at the step-up transformer stations and by sensitive reverse-power relays at step-down stations, as shown in figure 2. The application differs from the normal application of balanced line relays in the following respects. First, the lines are tapped at numerous points between the two ends, where the balance protection is applied and while the unbalanced loading is minimized by operating regulations, provision must be made for normal amounts of unbalance occurring in operation and maintenance. The sectionalizing breakers intervening between generating stations are arranged for simultaneous operation, except for ground faults, to prevent unbalances from this source. Unbalances are created under load conditions by removal of sections of transmission lines between generating stations for servicing and by the removal of step-up and step-down transformers. Thus, the differential current relays are necessarily of a calibrated type which can be set intermediate between unbalance occurring under load conditions and those which will occur under fault conditions.

Balance Line Relays

Figure 5 indicates graphically the required "trip" and "not trip" values for the balance line relays at one location and shows the superiority of the *CD* relay characteristic adopted for this application over percentage differential or straight magnitude differential relays which were also considered. Arrows indicate the progression of relaying quantities at the step-up transformer station as other breakers clear from the faulty line.

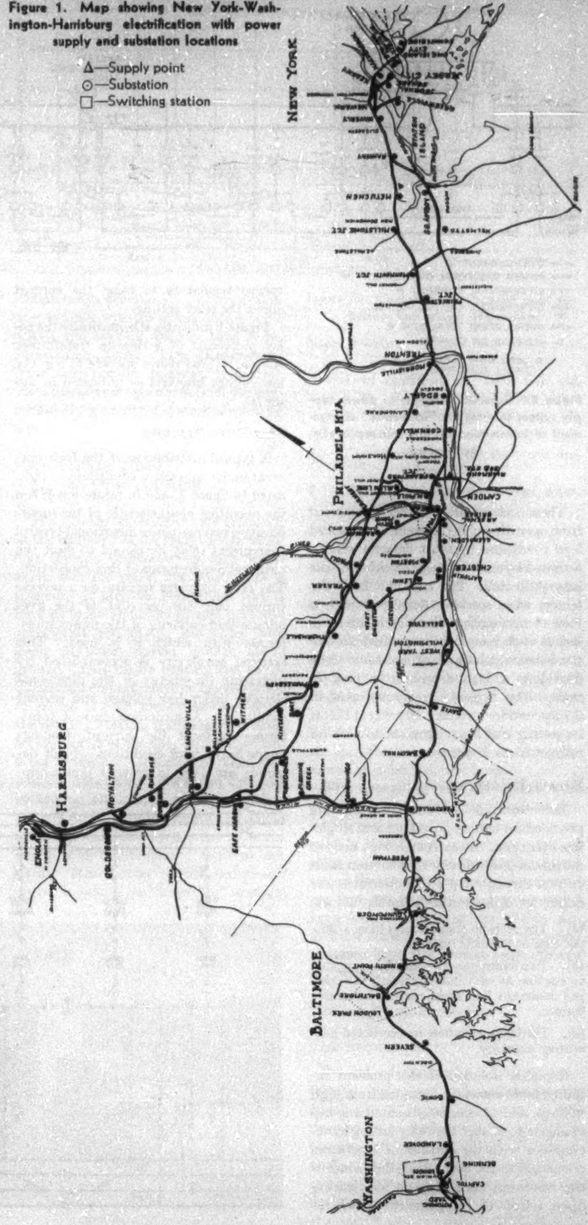

Figure 1. Map showing New York-Washington-Harrisburg electrification with power supply and substation locations

Δ—Supply point
⊙—Substation
☐—Switching station

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[1] For numbered reference, see end of paper.

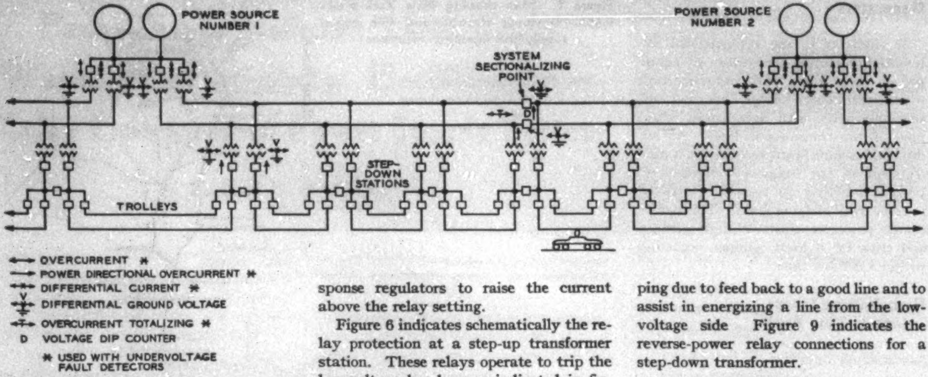

POWER SOURCE NUMBER I
POWER SOURCE NUMBER 2
SYSTEM SECTIONALIZING POINT
D
STEP-DOWN STATIONS
TROLLEYS
OVERCURRENT *
POWER DIRECTIONAL OVERCURRENT *
DIFFERENTIAL CURRENT *
DIFFERENTIAL GROUND VOLTAGE
OVERCURRENT TOTALIZING *
D VOLTAGE DIP COUNTER
* USED WITH UNDERVOLTAGE FAULT DETECTORS

Figure 2. Schematic diagram of power supply system showing the "looped-in" arrangement of transmission circuits and sectionalizing breakers

These relays are further prevented from operating under extreme unbalanced load conditions by fault detector relays, actuated by undervoltage on either of the lines protected. The type *AV* fault detectors were specially developed with a view to minimizing vibration inherent in fast 25-cycle relays when operated close to the balance point and at the same time providing a high drop-out to pick-up ratio. The type *AV* relay operates in 0.10 second for a voltage 10 per cent below its setting and has a drop-out to pick-up ratio of 95 to 98 per cent.

BACKUP PROTECTION

Induction-type overcurrent relays are provided for backup protection and single-line operation. An extremely high ratio of maximum load current to minimum fault current through step-up transformers was encountered due to the following factors:

(*a*). The step-up transformers have a 250-per-cent-five-minute rating.

(*b*). Two transformers could be connected to one line at each station, greatly lowering the minimum line fault current per transformer.

(*c*). The large variation in connected generating capacity.

Complete solution of this problem required fault detectors, actuated from high voltage, and at some points automatic tap changers to change the relay setting in accordance with the number of generators connected to the bus. Even then some of the minimum faults depend for clearing upon action of the generator quick-response regulators to raise the current above the relay setting.

Figure 6 indicates schematically the relay protection at a step-up transformer station. These relays operate to trip the low-voltage breakers as indicated in figure 2.

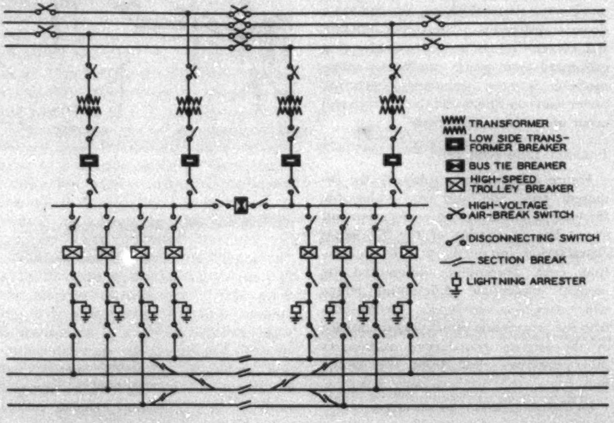
STEP-DOWN STATIONS

A typical distribution of the fault currents under minimum conditions is indicated in figure 7, and in figure 8 is given the operating characteristic of the supersensitive reverse-power directional-control overcurrent relay developed to meet the stringent requirements of this application. The relay operates rapidly on a reverse current only six per cent of the five-minute load capacity of the power transformer with which it is used. This extreme sensitivity is accomplished by increasing the energy in the directional element under low voltage and current conditions. A saturating current transformer protects the current windings under heavy load conditions. Fault detectors are used to prevent faulty tripping due to feed back to a good line and to assist in energizing a line from the low-voltage side Figure 9 indicates the reverse-power relay connections for a step-down transformer.

HIGH-VOLTAGE SECTIONALIZING POINTS

At Zoo, Perryville, and Thorndale high-voltage breakers are provided for sectionalizing the system. Thorndale and Perryville breakers are operated by the step-down-transformer reverse-power relays at those locations. This arrangement facilitates obtaining selectivity on a time basis with the step-down-transformer relays since both are of the same type. The sensitivity of this arrangement in detecting a fault is indicated by the per cent voltage difference between lines necessary to cause operation. Step-down transformers are four per cent reactance, so that eight per cent voltage difference between lines circulates full load current.

Figure 3. Single-line diagram of power circuits for one of the step-down substations


TRANSFORMER
LOW SIDE TRANSFORMER BREAKER
BUS TIE BREAKER
HIGH-SPEED TROLLEY BREAKER
HIGH-VOLTAGE AIR-BREAK SWITCH
DISCONNECTING SWITCH
SECTION BREAK
LIGHTNING ARRESTER

The relay is responsive to six per cent of full load current and hence to approximately 0.5 per cent voltage difference between high-voltage lines. This is obviously much closer than a differential voltage relay could be set, working directly from the voltages of the two lines. The Zoo breakers are not operated for line-to-line faults unless backup protection is involved.

Sectionalizing breakers of the two parallel lines are arranged to be tripped simultaneously for line-to-line faults to prevent serious unbalance on the good sections.

Analysis of Operation

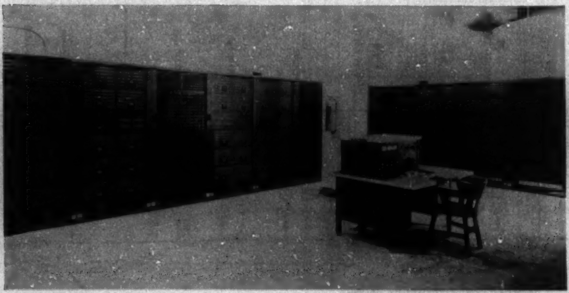
During the first stage of operation after the occurrence of a fault as in figure 7 the step-up transformer breaker nearest the fault and a number of the adjacent step-down-transformer breakers open. This produces a redistribution of fault current causing more reversal through the step-down transformers at other stations and results in their opening in the second or third stage. The majority of faults are cleared in a maximum of two stages, the complete clearing occurring in one second made up of components as follows:

Fault detector relay time—0.08 second.
Relay operating times from 0.1 to 0.5 second.
Breaker operating time from 0.16 to 0.20 second.

With certain emergency setups over-all operating times of $1^1/_2$ or 2 seconds are required for the complete clearing of a fault. This may include the opening of 30 circuit breakers.

For faults in the Philadelphia-Perryville-Harrisburg area, a high-voltage sectionalizing operation occurs as the first stage followed by the operations indicated above. While the step-up and step-down station relays are delayed by one breaker time to select with the sectionalizing point relays, the over-all clearing time is reduced due to fewer stages of step-down transformer breaker trippings. Important operating advantages result from the limitations of the extent of system affected by a fault.

Figure 4. A-c calculating board installed in Pennsylvania Station, New York City



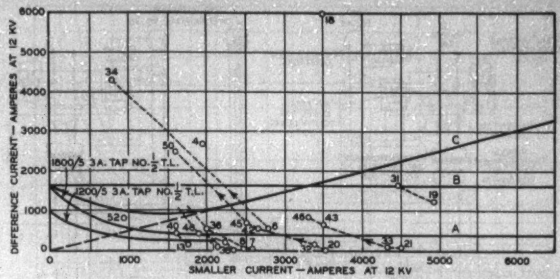

6000
5000
4000
3000
2000
1000
0
DIFFERENCE CURRENT—AMPERES AT 12 KV
0
1000
2000
3000
4000
5000
6000
SMALLER CURRENT—AMPERES AT 12 KV
1600/5 3A. TAP NO. ½ T.L.
1200/5 3A. TAP NO. ½ T.L.
A
B
C
18
34
50
40
31
19
520
460
43
40
48
36
45
42
6
13
10
38
8
7
32
20
33
21

Figure 5. Operating requirement chart for balance line relays, at Lamokin

Note: Numbers indicate case numbers of G. and H. data

X = Load condition A. Two S.U. out same line at S.H. 100,000-kva load Philadelphia-Washington

A — Type CD relay characteristic used

B — Current differential of same one line trip value

C — Fifty-per-cent-slope percentage differential relay

High-Voltage Lines; Line-to-Ground Faults

The step-up transformers are grounded at the midpoint of the high-voltage windings through comparatively high resistances with the result that when a line is grounded at any one point, that wire is lowered to substantially ground potential throughout its length, the other wire going to twice normal voltage above ground. This makes possible the use of differential voltage relays connected phase to ground at each step-up and step-down transformer station, resulting in prompt sectionalizing of the line, with the system setup in figure 2. It should be noted that had the transmission lines been tied past generating stations, a transmission line throughout the entire length of railroad system would have been affected in the event of a line-to-ground fault at one point. Many other similar factors occurred in which the "looped-in" arrangement of lines at generating stations had great advantages over other proposed operating arrangements.

In the area in which high-voltage sectionalizing breakers are used, two stages of ground relaying are used, sectionalizing of the faulted line occurring as a first stage followed by tripping of transformer breakers on the faulty portion only. Balanced voltage relays of induction type are used throughout, selectivity being obtained on a definite time basis.

The grounding resistor values were chosen of sufficiently low value to prevent arcing ground phenomena (the losses quickly damp out oscillations) but of sufficiently high value that with not over two or three grounding resistors connected per line, the ground fault current would keep inductive interference within permissible bounds. The 330-ohm grounding resistors limit the ground fault current per resistor to 200 amperes.

Trolley Relay Protection

The Requirements

In order to minimize inductive interference, burning of trolleys, and interference with adjacent equipment, it was decided at an early stage in the electrification to use extremely high-speed trolley protection, and accordingly one cycle (0.04 second) total clearing time was set as a requirement. A number of pos-

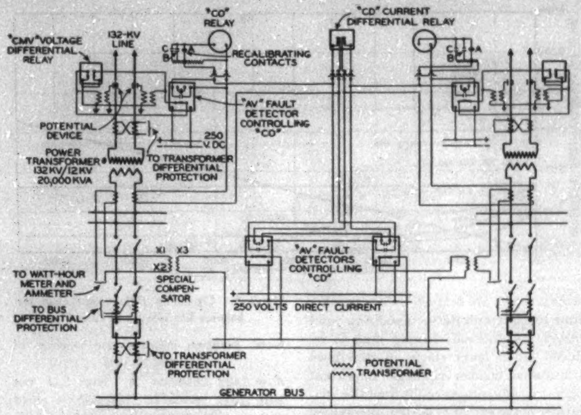

"CMV" VOLTAGE DIFFERENTIAL RELAY
132-KV LINE
"CO" RELAY
C
B
A
RECALIBRATING CONTACTS
"CD" CURRENT DIFFERENTIAL RELAY
"AV" FAULT DETECTOR CONTROLLING "CO"
POTENTIAL DEVICE
250 V.DC
POWER TRANSFORMER# 132KV/12KV 20,000KVA
TO TRANSFORMER DIFFERENTIAL PROTECTION
X1 X3
X2
SPECIAL COMPENSATOR
"AV" FAULT DETECTORS CONTROLLING "CD"
TO WATT-HOUR METER AND AMMETER
TO BUS DIFFERENTIAL PROTECTION
250 VOLTS DIRECT CURRENT
TO TRANSFORMER DIFFERENTIAL PROTECTION
POTENTIAL TRANSFORMER
GENERATOR BUS

Figure 6. Typical relay connections at a step-up transformer station

Note: Recalibrating contact *A* closed with one generator on line; *B* closed with two generators on line; and C closed with three generators on line

sible systems of relaying were investigated, including a 500-cycle superimposed frequency scheme, pilot-wire schemes, and the fundamental-frequency schemes finally adopted.

Two types of relay arrangement were brought out by two different manufacturers. These may be roughly differentiated as a relay type supplied by the Westinghouse Electric and Manufacturing Company and an impulse trip more or less built into the breaker operating mechanism and supplied by the General Electric Company. Both of these arrangements result in one-cycle operating time for relay plus breaker and constitute the first application of what are today known as "high-speed relays" in a-c power systems.

Each trolley section is normally fed from two ends, but with one end open, it must be capable, as far as any relaying limitations are concerned, of supplying for a short time a total load of about 40,000 kva at 11 kv, or ample power for a city of 60,000 people. This enormous moving load draws nearly twice as much current as a fault at the remote end of the trolley, which gives some idea of the problem involved in high-speed clearing of faults.

Protective Arrangement

Description of one of the schemes and its various elements will serve to indicate the method of solving this problem. The arrangement supplied by the Westinghouse Electric and Manufacturing Company consists primarily of an impedance relay of the high-speed type, and overcurrent elements. For longer trolley sections with heavy loads the overcurrent elements are replaced by an induction-type impedance relay, as shown in figure 10.

In general, the high-speed impedance relay, and the instantaneous current relay, where used, provide clearing for most faults, the two-cycle delay current or impedance element providing sequential tripping of the remote breaker for faults in the end zones. Thermal relays, matching the trolley heating characteristics, protect them against overload and make their full thermal capacity usable safely under any load cycle.

Impedance Element

The impedance element is of the balance beam type and operates in as little as $^1/_8$ cycle on 25 cycles. This high speed is made possible by use of double voltage restraint coils, differing in time phase and resulting in an almost uniform voltage restraint. Therefore, rise of the current-coil pull above the restraint is almost instantly detected by the relay element. Advantage is taken of the mutual reactance between trolleys which results in the impedance of 96 per cent of section length being only 80 per cent of the total section impedance. This characteristic, shown in figure 11, allows setting the relay balance point within 4 per cent of the end of the section and still retaining 20 per cent safety margin. Figure 11 is based on a four-track section, the ratios being less favorable as the number of trolleys decreases.

Current Directional Element

The impedance element is used with a current polarized directional element comparing the direction of trolley current with that flowing through the step-down transformer bank and having a tripping zone as indicated in figure 12 in order to select the faulty trolley. It is arranged to close contacts with no polarizing current provided the trolley current is sufficient.

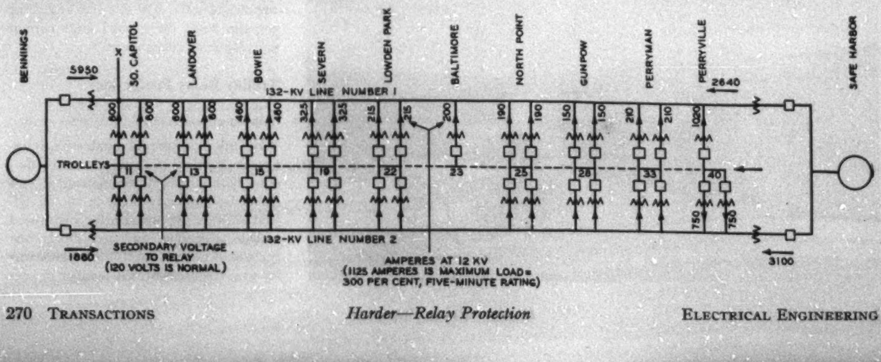

BENNINGS
SO. CAPITOL
LANDOVER
BOWIE
SEVERN
LOWDEN PARK
BALTIMORE
NORTH POINT
GUNPOW
PERRYMAN
PERRYVILLE
SAFE HARBOR
5950
X
132-KV LINE NUMBER 1
2640
800
800
800
800
460
460
325
325
215
215
215
200
190
190
150
150
210
210
1020
TROLLEYS
11
13
15
19
22
23
25
28
33
40
750
750
132-KV LINE NUMBER 2
1880
SECONDARY VOLTAGE TO RELAY (120 VOLTS IS NORMAL)
AMPERES AT 12 KV (1125 AMPERES IS MAXIMUM LOAD= 300 PER CENT, FIVE-MINUTE RATING)
3100

Figure 7. Typical fault-current distribution under minimum conditions

Load Presetter

A load presetter used with the high-speed impedance relay compensates for the change in impedance caused by the presence of train loads on the trolley sections. An idea of the magnitude of this effect may be given by taking a typical seven-mile section which would have about $3^1/_2$ ohms impedance. A maximum trolley load considered is 3,000 amperes, which at an average potential of 11,000 volts would correspond to 3.67 ohms. Thus the impedance of the maximum load is roughly comparable with the impedance of the trolley section. For a fault at the one end of the trolley, the impedance looking into the other end of the trolley would be only 70 per cent as great if a maximum load were present uniformly distributed as it would be if no load were present. The load presetter compensates for this effect by providing additional voltage restraint proportional to the load.

Transient Shunt

Since instantaneous values are being dealt with, and since in an electrical system immediately subsequent to the occurrence of a fault, a d-c or transient component may be present whose magnitude is as much as the crest value of the alternating current, a large error in impedance measurement would take place unless some measure were adopted to discount this transient current. A transient shunt was developed for this purpose. This shunt removes the transient component of the short-circuit current both from the impedance element and from the overcurrent elements of the relay equipment, making it possible to set only for the symmetrical values of current which are predictable. But for this shunt, an element set to trip down to a balance point close to the other end of the section would trip well into the other sections in the event of an asymmetrical short circuit. Transient shunts have since been applied in other high-speed relay applications.

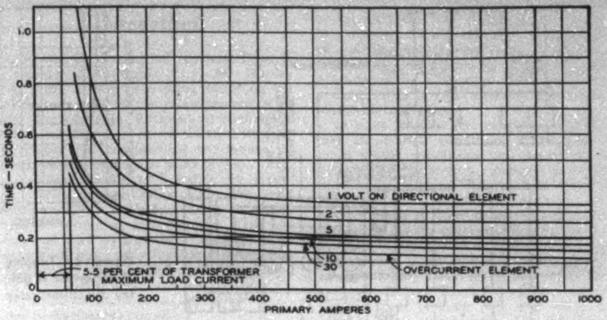


Figure 8. Supersensitive type CR reverse power relay characteristics

Note: Maximum load current = 1,125 amperes, five minutes. Normal secondary volts = 120

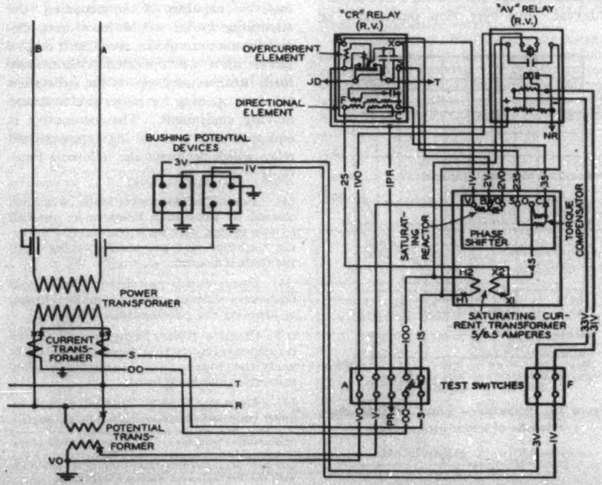


Figure 9. Typical reverse-power-relay connections at a step-down transformer station

High-Speed Overcurrent Relays

Overcurrent elements of both instantaneous and two-cycle time-delay types were provided. This permits instantaneous operation for all faults which are well above through fault plus load current. The two-cycle time-delay relay is set just above maximum load current, but below those currents which are caused by the combinations of load plus heavy through fault to the next section.

Induction-Type Impedance Relays

For longer trolley sections designed to carry heavy peak loads, discrimination between loads and faults becomes more critical and advantage must be taken of every available factor. The two-cycle-delay impedance relay balances E^2 against $EI \cos \theta$, and thus has maximum torque for current at a particular phase angle, which is adjusted to assist in selecting loads from faults. Thus loads may be carried of lower impedance than the relay setting provided the power factor is higher than during faults. In addition a thermal type of presetting is used which increases the setting in amount depending on the load current through the relay prior to a fault. Finally, the two-cycle delay provides selectivity with the breakers of other sections for faults outside of the protected section. This relay has inherent directional characteristics.

Bus Protection

After several stages of development including use of induction and instantaneous types of relays measuring differential current, the majority of busses have finally been protected by percentage dif-

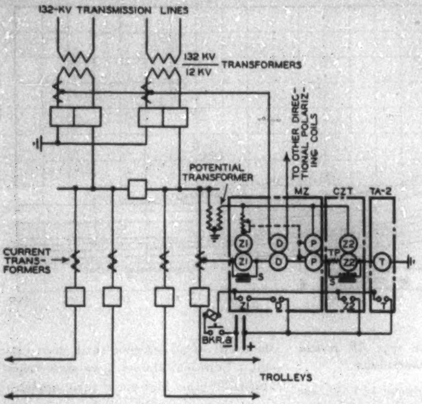


Figure 10. High-speed trolley relays—external connections

*Z*1 — First zone impedance element, *MZ* relay
D — Current polarized directional element, *MZ* relay
P — Load presetting element, *MZ* relay
*Z*2 — Second zone impedance element, *CZT* relay
T — Trolley thermal element, *TA*-2 relay
TP — Thermal presetter
S — Transient shunt

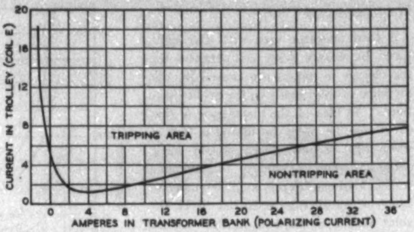


Figure 12. Tripping characteristic of trolley directional relay

ferential relays type *CA*-4. These are relays having three restraining windings and one operating winding. For the trolley busses, the restraining windings are associated respectively with (1) incoming transformer feeders, (2) incoming bus tie, (3) outgoing trolleys. This relay results in extremely fast clearing of bus faults and is comparatively insensitive to through faults because of the large restraint. The same type of relay has been applied with excellent success to generator busses.

TRANSFORMER DIFFERENTIAL PROTECTION

The differential protection of transformers on a system which does not have high-voltage breakers at the station, is a somewhat difficult problem. It is obviously necessary to clear the entire transmission line, involving the opening of a large number of breakers, in the event of a fault involving the differential protection of a single transformer. For faults in the high-voltage winding of the step-down transformer amounting practically to line-to-line faults on the transmission system, the differential protection trips the low-voltage breaker and sets up a circuit for opening the high-voltage disconnecting switches after the fault has been cleared as indicated by the resetting of the type *CA* differential relay when the fault current is removed.

OUT-OF-SYNCHRONISM

At sectionalizing points located sufficiently near the electrical center between major generating stations periodic dipping of voltage forms an out-of-synchronism indication. A "dip counter" relay was developed consisting of a counting chain of telephone-type relays actuated by the type *AV* undervoltage relay and set to trip the system sectionalizing breakers for three voltage dips within a five-second interval. For very slow pull apart, or out-of-phase conditions, overcurrent relays, operating on the totalized current of the two lines, produce simultaneous tripping of the sectionalizing breakers.

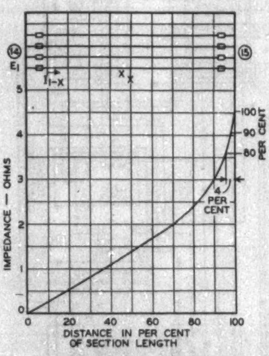


Figure 11. Impedance versus distance characteristic of a four-track section

$$Z = \frac{E_1}{I_{1-x}}$$

Undervoltage Sectionalizing

Considerable thought has been given to the confining of troubles to a particular section of the system by proper sectionalizing under certain conditions. In several instances low voltages have occurred due to various causes. This has resulted in loss of power and considerable disturbance to the system. Undervoltage relays have been installed at major sectionalizing points set to sectionalize the system automatically in event of the falling of voltage two-thirds of normal for a time adjustable between one and five minutes.

Motive Power Unit Relays*

As the trolley fault current may be as high as 50,000 to 65,000 amperes the use of high-speed circuit breakers on the locomotives capable of interrupting the maximum faults would have been decidedly uneconomical. As a result motive power units are operated without any main breakers and rely on the substation breakers opening for major faults on the moving equipment. The locomotive is equipped with a single highly specialized relay which performs the following functions:

(*a*). For major locomotive faults, which are cleared by substation breakers in one-half to two cycles, sets up a pantograph lowering sequence but prevents lowering until the fault is cleared.

(*b*). Opens motor switches to clear motor flashovers without causing a major outage, or lowering the pantograph.

(*c*). Closes a trolley grounding switch for transformer internal faults or sustained overloads, then lowers the pantograph only after substation breakers clear.

(*d*). Does not operate ground switch or lower pantograph on transformer magnetizing inrushes in excess of fault currents. Such inrushes are sometimes encountered due to bouncing pantographs or when crossing trolley phase and section breaks.

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*Locomotive relaying is a joint development of the General Electric Company and Westinghouse Electric and Manufacturing Company in co-operation with the Pennsylvania Railroad Engineers.

Multiple-unit cars are equipped with a single device which performs less functions but which nevertheless is coordinated with the substation relaying, clearing faulty equipment from the trolley promptly after the substation breakers open and permitting prompt successful re-energizing of the trolley.

This general arrangement for clearing faults on motive power units has resulted in exceptionally good operation in the six years during which it has been in service.

Conclusion

Most faults occur on trolleys and transmission circuits, one fault being encountered per 20 miles of transmission line per year, and one fault per 1.17 miles of trolley per year. For the complete New York-Washington-Harrisburg system this will amount to approximately 55 transmission faults (grounds and short circuits) and 1,875 trolley faults per year. A large number of these are of a nature which clear when the breakers are opened so that the affected section may be immediately reclosed.

With the protective arrangement described, entirely adequate selectivity is obtained among different protective elements of the system under normal conditions and for certain abnormal system setups necessary for maintenance. In so far as possible, the designs and settings have been planned to give proper selective action under the innumerable abnormal conditions which may occur during emergencies, while still permitting full use of the power-supplying ability of all lines and apparatus.

Reference

1. A System Out of Step and Its Relay Requirements, L. N. Crichton. Electrical Engineering, October 1937.

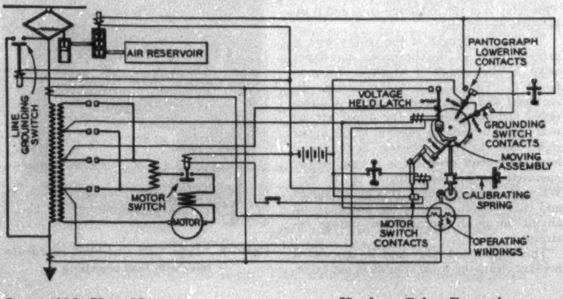

Figure 13. Schematic diagram of locomotive relay

Discussion

A. R. van C. Warrington (General Electric Company, Philadelphia, Pa.): Mr. Harder has given a very interesting paper on his company's part of the relay protection of the Pennsylvania Railroad electrification which recalls for us in the General Electric Company the considerable developmental work we also did on the same project.

Referring to figures 8 and 9, the supersensitive directional relay at the step-down stations uses a nonlinear circuit to ensure reliable torque at low voltage. This circuit was used in our 1928 single-phase reactance distance relay though we found later that greater amplification could be obtained by using a neon lamp to provide the saturating and detuning effect at normal potential, due to its sharper cutoff.

Over 80 per cent of the trolley breakers are of General Electric manufacture equipped with impulse trip, for one-cycle interruption. Backing up the impulse trip are the *CEX* relays which, in addition, take care of faults below 1,800 amperes. These relays contain two induction-cup-type units whose contacts are in series. One unit is a directional impedance unit and the other is a directional admittance unit. This peculiar combination was necessary to produce a combined characteristic which would fit in between load and fault conditions. The settings of the two units correspond to the section length. The relay operates in one cycle to open the holding coil circuit of the breaker so that it closely backs up the impulse trip. The dying away of the holding coil flux and the opening of the breaker brings the total clearing time within three cycles.

The *CEX* relay is relatively insensitive to transformer energizing transients because the operating torque of the directional impedance unit is a product of the current flux and the flux of the tuned potential circuit. It does not require a transient shunt because the d-c component produces no torque in induction relays.

The *CEX* relay gives positive directional action even on bus faults which produce zero potential because the polarizing potential winding of the directional impedance unit is tuned with a series capacitor with which it continues to oscillate for several cycles after the line voltage goes to zero. Mr. Harder did not say what precautions of a similar nature were taken in the corresponding Westinghouse Electric and Manufacturing Company directional impedance relay to ensure reliable directional action on bus faults.

In some sections the current, potential, and phase-angle conditions during maximum traffic conditions correspond to a fault within the section. In order to avoid unnecessary tripping of trolley breakers, the *CEX* relays are normally set high and are only dropped to their proper values if the rate of rise of the current indicates a fault. In order to distinguish rapid rate of rise the line current is rectified and then supplied to an auxiliary relay which supervises the protective relays. Under normal conditions the rectifiers produce only d-c which is blocked by a transformer. On the other hand, a sudden rise of current caused by a fault produces a corresponding rate of increase in the rectifier output and this increase in turn creates an impulse of current in the secondary of the transformer which operates the supervising relay.

These are a few of the many interesting problems which were solved for this unique application.

C. Romanovsky (nonmember; General Electric Company, Philadelphia, Pa.): E. L. Harder's paper on relay protection for a railroad system is of great benefit to engineers interested in a-c railroad electrification.

Other relays than those described in Mr. Harder's paper have been supplied by the General Electric Company for the solution of numerous difficult problems of system protection on the Pennsylvania Railroad electrification projects.

Among these were relays of very strict requirements for the differential protection of the 132-kv underground oil-filled cable at Baltimore and Washington. These relays must stand 5,000 amperes primary for five seconds and operate on faults as low as 30 amperes primary.

The relays operate over a two mile run (average) of number 10 pilot wire, and are meeting the following requirements:

The relays must not operate on:

A.	Five-minute load......	up to 300 amperes
B.	Through short circuit...	up to 5,000 amperes
C.	Through ground fault..	up to 800 amperes
D.	Charging current......	up to 20 amperes

The relays must operate on:

A.	Short circuit in cable.......	from 30 to 5,000 amperes
B.	Ground on cable...........	from 30 to 800 amperes

The relay operates on a sensitive percentage differential principle. It contains an operating and a restraining circuit. The current transformers at the two ends of the cable are differentially connected with respect to the operating coil so that on a fault in the cable, current will flow in the operating coil and thus operate the contacts. During an external fault, the current flowing in the secondaries of the current transformers and in the restraining coil opposes the effect of any operating current which may result from slight difference in the secondary currents of the current transformers. This restraining action prevents operation on through faults.

For the New York zone, where direct current is superposed on a-c apparatus due to common rail return circuit, relays were developed to prevent opening motor-operated disconnecting switches when more than 25 amperes alternating current with superposed 750 amperes direct current is flowing in the circuit and be able to withstand short-circuit currents of 50,000 amperes.

In order to reduce the cost of electrical equipment in large station areas, groups of low-interrupting-capacity normal-speed breakers are used for the isolation of faults on yard and passenger station feeders. Each group of from two to five breakers is fed by a high-speed high-interrupting-capacity (master) breaker which clears the fault. The high-speed breaker is relayed similar to other trolley breakers for discriminating between load and fault currents and for selective operation.

A relay was developed for selecting the faulted trolley before the master breaker opens so that when this breaker has cleared the circuit and the current through the low-interrupting-capacity breaker on the faulted trolley has fallen to a safe value, the relay permits the low-interrupting-capacity breaker to open. After the opening of the low-interrupting-capacity breaker, the relay closes the master breaker, and thus restores service on the unfaulted trolleys.

The operation of a great number of trolley-sectionalizing disconnecting switches offered an interesting problem. They were to be operated from distances up to five miles by 110-volt 100-cycle power taken from 6,600-volt signal lines at points near the disconnecting switches. The current through the control cable was limited to 0.15 ampere. Control and position indication of the disconnecting switches was to be accomplished over the smallest number of wires. Undesirable operation and false indication of switch position by induced currents during short circuits on the trolley system was to be avoided. Hazard of shock to operators and maintenance men was to be removed.

A relay and control scheme was developed using only four wires for each switch.

Small 110/220-volt insulating transformers with midpoints grounded are used rather than impedance coils. With this scheme high induced potentials cannot stress the insulation of control devices.

L. N. Crichton (Westinghouse Electric and Manufacturing Company, Newark, N. J.): There is an enormous amount of power available on this system so that the relays and circuit breakers must be capable of safely withstanding very heavy short-circuit currents. On the first units of this electrification the anticipated loads were small enough and the sections of trolley wire were short enough so that the load current never exceeded the minimum short-circuit current. It would, therefore, have been possible to protect these sections by means of instantaneous overcurrent relays set to operate on a high current and time-limit overcurrent relays set to operate on a low current. Such relays were actually installed and in addition instantaneous impedance relays were installed to secure maximum possible simultaneous clearing of faults.

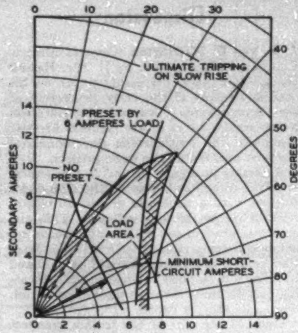

Figure 1. The load characteristics and the tripping point of the type CZT impedance relay for long trolley section at varying phase angles and load conditions

Now the loads are heavier and the newest electrified sections are much longer so that the load current under some conditions is greater than the short-circuit current. The anticipated loads and minimum short-circuit currents are shown in figure 1 of this discussion. This curve also shows the tripping characteristics of the time-limit impedance relay mentioned by Mr. Harder. It will be observed that the relay will trip if the minimum short-circuit current is applied when no load exists. If a load has existed for several seconds, the relay will trip when an additional sudden increase in current is applied. Finally, the relay will trip if current is either gradually or quickly added above the values shown on the curve at the extreme right. This adjustment differs for every different length of line.

This relay has an adjustable time limit of two cycles or more and is intended to protect only the far end of the trolley wire. A photograph of this relay is shown in figure 2 of this discussion. The thermal element which increases the setting of the relay when the load comes on is the nickel wire shown at the front of the slider. This picture shows the complete relay except for an external resistance in the potential circuit and a shunt reactance in the current circuit. This relay is of the induction type and is inherently directional.

The greater length of the trolley wire is protected by an instantaneous impedance relay, a photograph of which is shown in figure 3. This relay is not inherently directional and, therefore, contains a directional element shown in the upper left, as well as the impedance element in the upper right, and the load presetter in the lower left. This load presetter is a small motor operated by the volt-amperes in the line. When the load increases, this motor moves and cuts resistance out of the potential coil circuit so as to increase the voltage restraint.

This high-speed relay and its associated breaker clears the usual trouble in one cycle. This requires that the relay cannot have more than one-fourth cycle in which to operate and sometimes the operation is much faster than that. The result of this fast operation is that even the heaviest of short circuits cannot be observed at the generating stations and it is seldom that any burning can be found on the trolley wires. From the figures given in Mr. Harder's paper it can be calculated that the average section of trolley wire has six short circuits per year, about five of which are cleared by the high-speed relays and one by the two-cycle time-delay relay.

It was difficult to design a relay to work accurately at this high speed because of the transient in the circuit. Although this transient has practically disappeared in two cycles, it is of enormous consequence during the first one-fourth cycle when the relay must complete its work. The problem was solved by the invention of the transient shunt, the use of which has since been extended to other types of high-speed relays.

D. R. MacLeod (General Electric Company, Erie, Pa.): Mr. Harder's presentation of the relay protection of the power supply for the Pennsylvania Railroad electrification brings out the interesting contrast between the comparatively leisurely manner (seconds) in which a high-voltage transmission fault is cleared and the get-rid-of-it-quick (cycles) attitude toward trolley faults. The high level of insulation adopted for the 132-kv and 44-kv lines and the adequate ground wire and counterpoise protection is one justification for this philosophy. The ability to isolate any section of transmission between substations with only temporary interruption of power to these substations, even where only one line is available, is another justification. There were, of course, additional reasons for these engineering decisions since relaying was not the dominant factor in designing this transmission line and trolley system. While the trolley is provided with a high level of line insulation and is shielded by the transmission circuits the data given in this paper show that 97 per cent of the faults occur on the trolley. This relation between transmission and trolley faults was expected when the system was designed.

The devices used in the General Electric

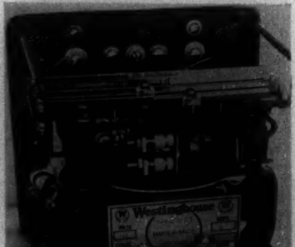

Figure 2. Type CZT time-limit impedance relay with load presetting

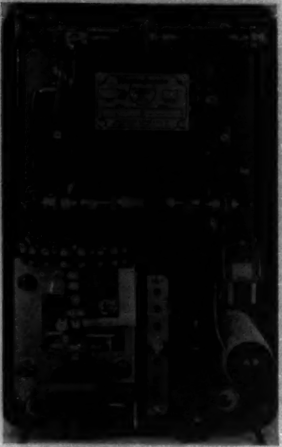

Figure 3. Type MZ high-speed impedance relay with load presetting

trolley relaying scheme are described in the discussions presented by Messrs. Romanovsky and Warrington.

There are important differences between this system of relaying and that described by Mr. Harder. Our approach to the problem has been based to some extent on theoretical studies such as Mr. Harder has described but we have given even greater consideration to experiences in the field. The majority of trolley faults occur in connection with the operation of the rolling stock and therefore we have given particular attention to the troubles which develop on these equipments. Our relaying has been designed to allow the greatest possible freedom in the design of the rolling-stock equipments which are always handicapped by space limitations.

The control schemes on different groups of locomotives differ in detail. On one group it is possible for one locomotive to have the impedance characteristics of three locomotives for an appreciable time before the operation of the ground switch. The distinguishing characteristic is however the low power factor under this particular condition. When the ground switch operates this "load" goes to zero and the impedances measured by the trolley relays are the same as if the load had not been present. If the locomotive was operating double headed the fictitious load would be equivalent to four locomotives accelerating and power factor will be entirely different from what may be expected with four normal locomotives. After the grounding switch closes there is a sudden increment of current due to the fact that the locomotive impedances are reduced to zero and this increment is always in excess of the notching increments on any of the rolling stock equipments.

Other examples of this general type based on actual experience in the field might be recited but it serves to show why our relaying system does not attempt to take advantage of presetting the relays in accordance with the characteristics of the load.

Mr. Harder claims that the transient shunt makes it possible to set the high-speed relays only for the symmetrical values of current which are predictable. Our studies of the various types of energizing transients which can occur on a locomotive load show a wide variety of wave fronts which would require a complicated network to handle. The symmetrical components of these transients are not predictable as they are complicated by the induction-motor load on the secondary of the locomotive. There is some inherent shunting of the transient in the impulse scheme but we do not take advantage of it. No shunting is necessary with our impedance relay. The settings of the impulse trip have been determined by field experience during the ten years that it has been in operation on this electrification.

Our first line of defense is the impulse trip. This scheme was originated by J. W. McNairy and is described by him in AIEE Transactions, volume 47, October 1928, page 1276. The second line of defense is the distance relay described by Mr. Warrington which can be set to cover the entire distance between substations. It is not desirable to take advantage of the mutual impedance between trolleys (see figure 11 of Harder's paper) because this mutual may not be there when it is needed most and it is possible to visualize conditions when it is in the wrong direction. On the Harrisburg electrification a few rate-of-rise relays were provided for those sections of the railroad where it was feared that abnormal operating conditions might show that it is desirable to have an additional means of discriminating between a load and a fault. The experience gained with these equipments will be of value in working out adequate protection for the trolleys as substation spacings are increased. However, it should be noted that the trend of operation practices on this electrification is in the direction to make the relaying problem considerably easier than was first visualized.

An important consideration in distance relaying is the variation in the impedance characteristic of a locomotive with variation in trolley voltage. It may be shown from a consideration of the characteristics of the a-c series motor that for a given tractive effort, field connection, and power factor the impedance as measured at the pantograph of a locomotive varies as the square of the applied voltage and that for a given tractive effort the minimum impedance varies directly as the applied voltage. The minimum impedance of two locomotives pulling the heaviest freight train which we know of from Enola (across the river from Harrisburg) to Waverly, N. J., is of the order of ten ohms at approximately 80 per cent power factor with a trolley voltage of 11 kv. If the freight train is half the size but has two engines on it to get motive power to the other end of the line, the minimum impedance will probably be the same as with the heavier train. The locomotives in passenger service present the real problem. A train which could operate with one engine may be double heading and the engineer may elect to accelerate rapidly. The minimum impedance may be seven ohms and the power factor near 85 per cent. As the train speeds up to running speed in passenger service, the power factor goes above 90 per cent and the impedance increases. The impedance of the double-headed passenger train at the power factor corresponding to the angle of the line will probably be the same as that of the heavy freight train. This impedance is, of course, much greater than the minimum impedance. In the case under consideration it would be approximately 12.5 ohms. If the voltage falls to 8,500 volts the impedance of the train at the power factor corresponding to that of the line may be 7.5 ohms. Because of the rapid acceleration of the passenger train the highest values of current are only experienced for a matter of seconds so that the trolley is not overheated. The distribution of passenger trains running with several sections is considerably closer than is usual in freight operation. It is, of course, difficult to predict what the groupings of trains will be under abnormal conditions of operation and therefore the railroad engineers specified certain conditions as a basis for working out the relay problem. The General Electric Company's system of trolley relaying was given the greatest flexibility that modern relaying permitted as it was recognized that future developments might prove that the specified conditions were inadequate.

W. A. Lewis (Cornell University, Ithaca, N. Y.): Certain phases of the paper by Mr. Harder appear to justify further amplification and emphasis. It is stated in the paper that prior to the advent of the a-c network calculator, the short-circuit currents were determined analytically. It has been the practice for a number of years on a large number of systems to use the d-c calculating board for the determination of short-circuit currents, and some question may arise as to why the d-c calculating board could not be used in this case. The d-c calculating board introduces the fundamental assumption that the phase angles of the various impedance elements entering into the network are so nearly the same that they may be assumed equal without introducing appreciable error in calculation. In this case, the frequency is 25 cycles, with the result that the reactance components are less than half of what they would be if the same circuit elements operated at 60 cycles. Because of the relatively large resistance components in the transmission circuit elements, the ratio of reactance to resistance is quite low at this frequency, and the phase angle of the circuit element is therefore relatively small, in the order of 45 degrees. On the other hand, the phase angle of the transformer elements remains quite high, in the order of 80 degrees or more, with the result that the phase angle of the transformer elements is much greater than of the line elements. For a fault in any given location there are numerous parallel circuits involving the step-down transformers in the several substations, and the division of current between these parallel branches is important in the application of the relays. Because of the wide phase-angle difference just described, it was not possible to obtain sufficient accuracy in a solution made on the d-c calculating board, and accurate representation on a miniature system was therefore not possible until the a-c network calculator became available.

Another feature deserving of mention is the mutual impedance between the several parallel trolley-rail circuits, due to inductive coupling between the parallel trolleys, and to the common use of the rail and earth in the return circuit. Mutual impedances of this character are difficult to represent on a d-c calculating board when the number of parallel circuits exceeds two, as it does on the Pennsylvania Railroad when more than two tracks are involved. This problem can be handled readily on the a-c calculating board by the use of mutual transformers which have previously been described.

The description of the protection of the extensive power supply system for this railway invites a comparison with the relaying of public utility systems. Two important differences may be noted between the two different forms of power supply. With the railway system, the load is distributed along the trolley circuits, and moves rapidly from one location to another. This means that the currents and the impedances measured by the relays for a short circuit in a given location will vary considerably depending upon train location and the load drawn at the time the fault occurs. This is in contrast with the public utility system in which the major load is almost always drawn from fixed substation locations usually at bussing points, and does not move rapidly from one place to another. The so-called "load presetting device" which recalibrates the relays as a function of the load just prior to the fault is an unique and interesting method of overcoming the problem encountered.

The second notable point of difference is the use of high-voltage lines with the large number of substations tapped in without the use of high-voltage breakers at any of the substations. Although there has been a certain tendency to eliminate the high-voltage breakers in the power system, it has never been practiced to anything like the extent utilized by the railroad. The high speed of protection obtained with this layout, which appears very complicated to the relay system, is quite notable. The development of the very sensitive reverse-power relays indicates what can be done when the necessity arises. The absence of high-voltage busses makes possible the very unique use of balanced voltage relays for the protection and isolation of ground faults.

It is also interesting to note the developments in the railway field which have found application in the utility field. The most notable of these is the use of the high-speed balanced-beam type of impedance relays. Although the principle was not new at the time, the railway installation represented the first installation in the United States of the high-speed impedance relays operating on this principle. Impedance relays operating on the same principle are now the accepted means of providing the highest speed phase-to-phase protection on utility systems, being used in the majority of distance relays, and being the backbone of the carrier-current relay system which has found the greatest favor in the last few years.

E. L. Harder: The supplementary information contributed by the several discussers on the trolley relays is the most valuable addition, making the paper a rather complete description of the over-all relay system from generator to motive power equipment. The paper had covered the protection of eight main system elements but had described only the Westinghouse form of the trolley relay protection. The discussion completes the picture of this particular element of the relay system. Strangely enough, however, the high-voltage system represents by far the more difficult and complicated relay application problem. Certain questions and points are raised by the discussers requiring additional information which is given in the following.

Mr. Warrington states that the *CEX* relay operates in one cycle. We assume that this refers to its operating time for faults close to the relay since if it operated with this speed for faults at the remote bus it would trip also for faults on that bus or in the next section. The delayed relay time mentioned in the paper for the *CZT* time-delay impedance relay is given for fault at the remote bus.

He has requested to know how the Westinghouse company has solved the directional-relay problem which exists on a single-phase system susceptible to solid faults. A current polarized directional relay was used in which the totalized current of the step-down transformer supplies the polarizing element of the relay as shown in figure 10. Thus voltage is not required on the directional element. Its characteristics are shown in figure 12 of the paper.

Mr. Romanovsky describes the protection of the oil-filled cables and lists the setting requirements. It would of course have been interesting to go into this detail for other of the relays but the space limitations of the Institute paper do not permit. He has described the manual remote control of sectionalizing switches. We fail to see what connection this has with a paper on protective relaying.

In Mr. Crichton's discussion, figure 3 shows the current polarized directional element mentioned above. His explanation of the difference in the importance of transient shunt for a fractional cycle relay as compared with a relay operating in one or two cycles is particularly significant.

Mr. MacLeod mentions that the normal clearing times (one or two seconds) used on the high-voltage system are justified by the ability to sectionalize and obtain only temporary interruption of power to substations. Out of 61 step-down substations only three received power from a single line and these three have adjacent stations fed from both lines with consequent trolley feed. Therefore in almost all cases faults can be relayed out without interfering with loads at all. At the three stations fed from a single line the question of whether the line is cleared in a cycle or a second does not affect the duration of time required to sectionalize faulty line and re-energize the station. It does not appear that the ability to isolate any section of transmission has anything to do with the justification of normal-speed relaying. The matter of prime importance is that the clearing of high-voltage faults does not interrupt power to the trolley and consequently higher speeds are not essential.

He questions the ability of the transient shunt to make possible setting for symmetrical component of current only and mentions different wave fronts of locomotive inrush current. The high-speed impedance-relay setting is actually based on the symmetrical component of current and the relay is set to produce simultaneous tripping over 80 per cent of the section length. In view of the large number of trolley faults (one per mile of trolley per year) this is unquestionably proof of the efficacy of the transient shunt. There is a disagreement in view of the transient shunt in its function. Mr. Warrington assumes (correctly) that the transient shunt removes d-c component and states that his *CEX* relay does not require a transient shunt because the d-c component produces no torque in induction relays. Mr. MacLeod states that their studies "show a wide variety of wave fronts which would require a complicated network to handle." This would seem to require some further explanation of the transient shunt action.

If a sine-wave voltage is impressed on a resistance-inductance circuit at some particular instant of time a current flows which has a d-c component dying out with a certain time constant L/R. If this process is reversed and the same current (except in secondary terms) is passed through the same resistance and inductance (or values proportional thereto) the sine-wave voltage (except in secondary terms) will be reproduced, without any d-c component. If then this voltage were used to supply a pure resistance relay the relay current would be proportional to the symmetrical component of line current only. The exact theory upon which the design is based takes account of the actual relay burden in determining the correct proportions. However, the foregoing picture is a quite accurate representation of what occurs. It is apparent that the shunt is effective right from the start and hence usable on a relay which has an operating time of less than one-fourth cycle. The time constant of the d-c transient on the Pennsylvania Railroad trolley circuits is about one-third cycle for fault at the remote end of the trolley. The transient is therefore not of much importance anyway on the *CEX* relay which Mr. Warrington states operates in one cycle. We would expect it to have some effect on the impulse trip which operates in a fraction of a cycle. It is possible to obtain a much larger zone of simultaneous high-speed action of breakers at both ends of the line through the use of the transient shunt.

Mr. MacLeod states that it is not desirable to take advantage of the presence of the paralleling trolleys because they may not be there when needed. Mr. Boehne pointed out in his oral discussion that advantage is taken of the paralleling trolleys in the General Electric scheme in preventing the impulse trip from operating for faults at the remote bus. The same objection applies in both cases. Also, in both cases the answer is the same. The advantages which accrue under all normal and reasonable abnormal conditions are well worth suffering the slight adverse effects under extreme abnormal conditions.

Mr. MacLeod also suggests that the mutual might be in the wrong direction. This seems like an unnecessary suggestion to make without good cause. For a fault near the remote end of a trolley, which is the only condition under which mutual impedance between trolleys affects the relay one way or the other, currents flow in the same direction in all the trolleys, namely, toward the fault. Thus the currents in the

Fiber Glass—An Inorganic Insulation

By F. W. ATKINSON
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FIBER-GLASS textiles have achieved considerable importance in the field of electrical insulation since their introduction to industry a few years ago. Design engineers are finding in them a solution to special problems which heretofore had been solved only inadequately or temporarily at best.

It is a well-known fact that the output rating of a piece of electrical equipment is limited first of all by the temperature its insulation will stand without excessive deterioration. The commonly used types of organic insulation, that is, cotton, silk, linen, and paper, rarely permit a rated temperature rise greater than 55 degrees centigrade over an ambient of 40 degrees centigrade. Where higher temperatures were unavoidable, asbestos has been used if the attendant space factor was not prohibitive and the low tensile and dielectric strength were not of great importance. Mica in many different forms—as the outstanding class *C* insulation—has been used extensively, but the lack of mechanical strength and the high cost have imposed sizeable limitations.

Another never-quite-solved problem has been that of moisture absorption in fibrous insulation. And no amount of treatment or impregnation has as yet effected a perfectly moisture-proof flexible insulation that also is heat and oil resistant.

It is in the providing of a more satisfactory solution to these two specific problems of heat and moisture resistance that fiber-glass insulation, up to the present time, has contributed most extensively. Other characteristics such as chemical stability and mechanical strength coupled with heat and moisture resistance are proving increasingly valuable. Concrete examples of the resistance of fiber glass to the effects of corona together with outdoor moisture conditions have come to light recently in connection with its use on neon-sign lead wire.

Glass as an electrical insulator has been used for many years, but only comparatively recently has it achieved flexibility. In so doing it has become the first totally inorganic textile—one whose characteristics observed in the laboratory and in operation have proved it to be as good as, and in most cases superior to, other fibrous materials used for electrical insulation.

The fibers are pure glass and the fundamental process which converts the original batch into molten glass is the same as that used in making milk bottles or window panes. Some of the batch constituents are also the same. The important difference between ordinary glasses and those used in making electrical insulation is that the latter contain no monovalent substances and are alkali free. To be alkali free is an essential property because alkali leaches to the surface, thus presenting an ionizable salt which, under conditions of high humidity, decreases the insulation resistance considerably. This phenomena is aptly illustrated by tests on a low-alkali glass tape and a similar one which was made of alkali-free glass. After 72 hours in a constant relative humidity of 90 per cent at 100 degrees Fahrenheit, the insulation resistance of the alkali-free glass was 40 times that of the low-alkali glass.

The data herein are presented to indicate some of the more important properties of fiber glass and some of the fundamental reasons for its acceptance as an answer to specific problems in the field of electrical insulation. These data are discussed in considerable detail and comparisons are made to other commonly used types of insulation.

Glass fibers are produced in two distinct types, staple length and continuous. The former are comparatively short fibers from 8 inches to 15 inches long and approximately 0.00025 inch in diameter. Fabrics made from these short fibers resemble cotton or woolen ones. The continuous fibers, as their name implies, are produced in continuous lengths limited only by the size of the spools upon which they are wound. They average 0.0002 inch in diameter.

The staple-length forming apparatus consists mainly of a small electric resistance furnace, a steam blower with nozzles for producing high-speed steam jets, a chain conveyor, and a packaging machine. One-third-ounce glass marbles are fed into the top of the furnace automatically one at a time, and are melted. The furnace is made of high-temperature refractory material with the exception of the bottom which is made of a precious metal alloy. The molten glass passes through small orifices in the alloy from which point the resulting filaments are blown downward onto a drum-type conveyor by high-velocity steam jets. Lubricating oil is sprayed onto the fibers as they are being blown downward onto the chain in order to minimize the interfiber

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Paper number 39-44, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 24, 1938; made available for preprinting December 22, 1938.

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Acknowledgment is made to R. A. Corby, assistant, for assembling the data for this paper.

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adjacent trolleys acting through the mutual impedances increase the effective impedance of the relayed trolley which is as desired. When the remote breaker opens, if the fault is between the relay and the remote station, currents in the other trolleys reverse and flow toward the relay and thence over the faulty stub end trolley to the fault. Again this is also desired. The effective impedance is lowered by the mutual and the high-speed impedance-relay element which was set short of the remote bus when all breakers were closed is actually able to clear a fault at the remote end of the trolley as soon as the remote breaker opens, by taking advantage of the reversal in current direction in the parallel trolleys. This covers all of the possible conditions. Thus the mutual voltage drop is sometimes in the reverse direction but never in the wrong direction as Mr. MacLeod suggests.

The implication that variation of locomotive impedance as the square of line voltage depends on series-motor characteristics, is somewhat misleading. Given *any* type of load operating at a given voltage on the secondary of the transformer; raise the primary voltage, and change the transformer taps so that the secondary voltage is the same as before; the impedance (ratio of voltage to current) as viewed from the primary side of the transformer will have increased as the square of the voltage. This is for the simple reason that the turns ratio increases directly as the voltage. Impedance varies as the square of the turns ratio.

The really important thing is that the series motor behaves like an impedance at all, that is, that when the applied voltage is suddenly raised or lowered, current raises or lowers proportionally in a negligible part of a cycle. For example, if the applied voltage falls to zero the motor current falls to zero so fast that the difference in time is not distinguishable on a film with cycles spread out to one inch. This would not be the case at all with synchronous motors for example, which would feed heavy short-circuit current in the trolley when it is faulted.

Mr. Lewis has compared the railway transmission system with conventional power-system practice. His broad experience in both fields makes this comparison of particular value.

Table I. Breaking Strength in Pounds (Minimum of at Least Five Readings on Each Sample) of Staple- and Continuous-Fiber Alkali-Free Glass Tapes at Room Conditions

Width (Inches)	Thickness (Inch) 0.010	0.015	0.020	0.025
Staple				
1/4	59	85	105	*
3/8	68	108	119	132
1/2	110	158	183	231
3/4	150	167	245	290
1	182	250	305	346
1 1/4	268	298	360	400
Continuous				
1/4	70	*	186	
3/8	91	*	265	
1/2	133	285	346	
3/4	220	322	440	
1	280	494	595	
1 1/4	*	610	850	

* These sizes not available for test.

friction during the subsequent processing. Being prevented from blowing off the chain by air suction beneath it, the mat of fibers is then drafted into a single sliver by winding the latter on a two-inch-diameter paper tube whose linear velocity is greater than that of the drum conveyor. The tubes of sliver are then placed on textile spinning machines where the yarn is spun in the same manner as cotton or wool yarn.

The continuous process differs from the staple process mainly in the manner in which the fibers are drawn from the furnace. Here no steam jets are used. The fibers (102 of them) are drawn together into a single filament and are pulled down from the furnace mechanically by winding on a bakelite tube fastened to a spindle revolving at high speed. Lubrication is accomplished by passing the filament over an oil-soaked felt pad. The filament made up of 102 individual fibers is wound on the tube at the rate of 6,000 feet per minute. (One marble will produce over a mile of the filament or about 120 miles of individual fiber.) At this point the product is ready for the twisting operation in the textile fabrication sequence. The yarn and fabrics produced from continuous fibers resemble silk or rayon in appearance.

In general, staple-fiber products are preferable where a heavy type of insulation is required. Continuous fabrics have their advantage where space factor, impregnating qualities, or appearance, are of primary importance.

Tensile Strength

This is one of the more important properties of electrical-insulation textiles and perhaps the most important mechanical one, not only because of the obvious reason, but also because it provides an excellent means of measuring the effect of other test and operating conditions upon fibrous materials. For instance, the effect of heat, acids, weathering, etc., may be evaluated by noting the corresponding change in tensile strength.

Glass fibers of diameters such as are used in making textiles for electrical insulation have extremely high breaking strength. The value for individual fibers of the order of 0.0002 inch in diameter has been shown to be in excess of 1,000,000 pounds per square inch. No other textile fiber approaches this figure. Tests also show that woven glass fabric is stronger than other textile fabrics.

Table I shows the minimum breaking strength of some of the standard staple-fiber and continuous-fiber alkali-free glass tapes used for electrical insulation.

Table II shows the breaking strength of standard 0.010 inch by 1 inch staple-fiber glass tape after subjection to different temperature cycles simulating those which a piece of electrical equipment might encounter under severe operating conditions. It will be noticed that after a temperature cycle of 500 degrees Fahrenheit for 30 minutes each day for ten consecutive days the tape still retained 79 per cent of its original strength. After 400 degrees Fahrenheit for 30 minutes, its strength even increases slightly, probably due to the decreased viscosity of the processing oils permitting a greater equalization of stress among the individual fibers.

One accepted test of heat-resistant insulation is to subject it to a temperature of 300 degrees centigrade for five minutes. A series of these tests has shown that continuous-fiber glass tape retains from 87 to 92 per cent of its original strength and staple-fiber tape retains from 85 to 95 per cent. Similar tests showed that underwriters' grade asbestos tape loses approximately 46 per cent of its original strength. (After subjection to this temperature, cotton tape did not have sufficient strength to permit tensile strength tests.)

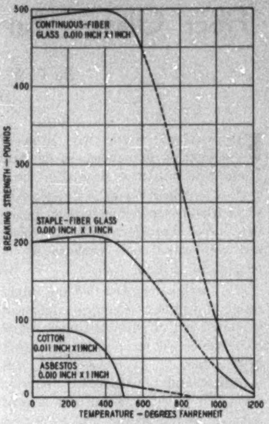

Figure 1. Effect of heat on tensile strength of electrical tapes

The effect of heat on the tensile strength of asbestos, cotton, and fiber-glass tapes is shown graphically in figure 1. Each curve represents averages of numerous determinations obtained over a period of a year.

Data below 600 degrees Fahrenheit were obtained by heating the tape in an electric resistance oven for at least 24 hours in each case and then breaking the specimens in a pendulum-type tensile-strength tester, the average of five good breaks being used to determine each point. Above 1,000 degrees Fahrenheit the data were obtained by heating the tape in a small electric furnace and then determining the breaking strength in the same manner. No data were obtained between 600 degrees and 1,000 degrees; hence, that portion of the curve is dotted. Throughout this series of tests, the maximum deviations from the mean were +10.5 per cent and −20.5 per cent.

Table II. Breaking Strength in Pounds of 0.010 Inch by 1 Inch Staple-Fiber Tapes After Subjection to Different Temperature Cycles Simulating Those Which Might Be Encountered by Electrical Equipment in Operation

	Original Conditions	400 Deg F for 30 Minutes	400 Deg F for 30 Minutes Each Day for 10 Consecutive Days	500 Deg F for 30 Minutes	500 Deg F for 30 Minutes Each Day for 10 Consecutive Days
	234	240	180	180	167
	182	187	198	185	157
	205	239	195	200	165
	213	200	195	190	176
	218	229	184	205	166
Average	210	219	190	194	166

Table III. Flexing Endurance

Samples 0.010 Inch by 1 Inch by 17 Inches

	Yellow Varnished Cotton Cloth	Yellow Varnished Glass Cloth
As received	2,100	2,700
After 280 deg F 24 hours	12,000	72,000
After 350 deg F 2 hours	4,500	80,000

Table IV. Dielectric Strength of Standard Unimpregnated Cotton, Asbestos, and Fiber Glass Tapes

All Samples 0.015 Inch by 1 Inch Nominal Except the Cotton Which Was 0.011 Inch by 1 Inch

	As Received				Washed*			
	Average Thickness (Inch)	Volts Per Mil			Average Thickness (Inch)	Volts Per Mil		
		Maximum	Average	Minimum		Maximum	Average	Minimum
Cotton	0.0121	99.5	94.5	82.9	0.0156	92.5	88.5	86.3
Asbestos	0.0151	66.2	58.3	45.0	0.0201	57.8	51.2	42.9
Continuous-fiber glass	0.0168	98.0	94.4	92.1	0.0135	107.2	97.3	92.5
Staple-fiber glass	0.0162	98.9	92.3	83.5	0.0152	102.3	99.6	95.7

NOTE: Tests were made by the short time method at room temperature of approximately 80 degrees Fahrenheit and 65 per cent relative humidity using one-fourth inch diameter brass electrodes and increasing voltage at the rate of 500 volts per second to breakdown. Each value is maximum, average, or minimum of ten readings of breakdown voltage divided by the average of ten readings of thickness.

* Samples were washed by boiling for 15 minutes in water containing 0.5 per cent soap and 0.2 per cent soda ash by weight; then dried in an oven at 225 degrees Fahrenheit for 30 minutes and conditioned at room conditions (approximately 80 degrees Fahrenheit and 65 per cent relative humidity) for at least 96 hours.

These curves show that glass tape is from 2 to 20 times as strong at room temperature as similar tapes made of other commonly used fibrous materials. The strength of glass tape does not diminish appreciably up to 600 degrees Fahrenheit and after 24 hours at 800 degrees Fahrenheit, it still retains half of its original strength.

Resistance to Abrasion

There is a wide variation of opinion as to what constitutes a proper test for the abrasion resistance of textiles. For this reason no conclusive tests have been made on fiber-glass fabrics. Tests have been made, however, on untreated and on impregnated tapes by sliding a five-pound steel weight (the bearing surface being knurled and on one-half inch radius) back and forth on a one-inch-wide specimen stretched over a steel plate. Revolutions of the reciprocating arm which drove the weight were counted until electrical contact was made between the five-pound weight and the steel plate.

With this method of test, untreated fiber-glass tape appeared to be on a par with equivalent asbestos tape, but weaker than cotton. The samples were not entirely comparable, however, due to the more open weave of the asbestos and the glass. Similar tests on impregnated tapes showed the glass tape to be the strongest of the three.

One or two manufacturers have said that some difficulty has been experienced due to the cutting of glass on sharp edges of metal during coil-winding operations. Others say that with reasonable care on the part of the workmen no difficulty is encountered.

Flexing Endurance

Insulation for many applications requires a considerable degree of flexibility and at the same time endurance to flexing, both during the treating and impregnating processes and during application on or in the finished equipment.

That glass fibers are spun, twisted, and woven on standard textile machinery into fabrics as thin as 0.002 inch is some indication of their flexibility. Using the test apparatus described below, five samples of standard 0.010-by-1-inch untreated continuous-fiber glass tape broke after an average of 26,500 flexes. Table III shows the number of flexes required to break one-inch-wide samples of 0.010 inch thick commercial yellow, varnished, cotton, and glass cloths after various heat treatments.

In making these tests one end of a sample 17 inches long was attached to a clamp fastened to the rim of a pulley 13 inches in diameter. The other end of the sample was attached to a stationary clamp placed below the pulley clamp so that

Table V. Dielectric Strength of Varnished Fiber-Glass Cloth as Compared With Standard Varnished Cotton Cambric After Short-Time Heat Treatment

		Volts per Mil											
	Average Thickness (Inch)	As Received			After 250 Deg F for Two Hours			After 350 Deg F for Two Hours			After 450 Deg F for Two Hours		
		Minimum	Average	Maximum	Minimum	Average	Maximum	Minimum	Average	Maximum	Minimum	Average	Maximum
Yellow Insulating Varnish													
Cotton	0.0093	842	915	1,038	911	982	1,046	928	982	1,019	964	1,065	1,121
Glass	0.0100	897	1,018	1,067	1,105	1,145	1,200	1,277	1,289	1,310	1,411	1,487	1,586
Black Insulating Varnish													
Cotton	0.0105	1,148	1,296	1,386	1,332	1,370	1,444	1,092	1,220	1,335	1,052	1,105	1,132
Glass	0.0095	1,243	1,312	1,368	1,220	1,362	1,538	1,187	1,372	1,680	1,195	1,400	1,518
After Creasing*													
Yellow cotton	0.0095	317	590	844	272	422	523	169	403	675	201	440	638
Yellow glass	0.0097	524	563	606	481	532	642	548	677	767	541	665	917
Black cotton	0.101	139	519	872	152	538	721	105	122	141	99	121	129
Black glass	0.0096	733	876	984	713	933	1,242	170	462	940	156	398	1,015

NOTE: All samples in table V were taken from standard rolls of cloth varnished by a manufacturer of varnished cotton cambric, the same respective varnishes being used on both the cotton and the glass. Nominal thickness of cloth was 0.010 inch but actual average thickness determined with a one-fourth-inch-diameter micrometer are shown for the cloth as received. Each value of dielectric strength is maximum, average, or minimum of ten readings of breakdown voltage divided by the average of ten readings of thickness determined separately for each sample. All tests were made by the short-time method at room conditions of approximately 80 deg Fahrenheit and 65 per cent relative humidity, using standard two-inch-diameter electrodes. Voltage was increased at the rate of 500 volts per second to breakdown.

* Samples were creased by bending double both ways and creasing between thumb and index finger as in creasing paper for tearing. Electrodes were placed directly over the creases.

Table VI. Dielectric Strength of Fiber-Glass Cloths Impregnated With Various Insulating Varnishes After Heating at 350 Degrees Fahrenheit for 140 Hours

Sample number		1	2	3	4	5	6
Impregnant		Clear air-drying varnish	Clear baking varnish	Clear baking varnish	Alkyd resin varnish	Yellow cambric insulating varnish	Black cambric insulating varnish
Average thickness (inch)		0.0067	0.0107	0.0075	0.0061	0.0076	0.0074
Dielectric strength (volts per mil)	Maximum	1,460	1,498	1,815	1,767	1,559	1,438
	Average	1,170	1,111	1,590	1,561	1,383	1,230
	Minimum	893	608	1,194	1,437	1,190	1,008

NOTE: Sample numbers 1, 2, 3, and 4 prepared by hand dipping thoroughly clean 0.004-inch-thick continuous-fiber glass cloth in comparatively thin solutions of varnishes, each one receiving three coats. Samples 5 and 6 were taken from the same rolls of commercially varnished cloth as the samples of table V.

there was one inch of slack when the pulley clamp was at the point of maximum distance from the stationary clamp. The pulley was rotated at a speed of 300 rpm creating a whipping motion causing a constant flexing of the fibers within the tape. Readings were taken of the time required to wear out each sample. Test values are shown only to the nearest hundred flexes.

Dielectric Strength

The dielectric strength of unimpregnated fiber-glass insulation as it comes from the loom is about the same as that of equivalent cotton textiles, both being determined largely by atmospheric conditions, fiber surface contamination, etc. If the effect of the surface contamination be minimized by thorough washing and drying, the dielectric strength of the glass increases approximately 3 per cent, but that of the cotton decreases about 6 per cent and asbestos about 12 per cent, due to the absorption of moisture and the attendant swelling.

The results of tests on three types of unimpregnated tape, asbestos, cotton, and glass, both "from-the-loom" and washed, are shown in table IV.

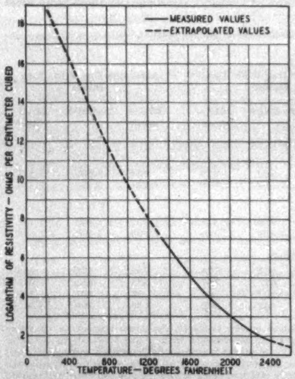

Figure 2. Volume resistivity of alkali-free glass

Table V shows a direct comparison between the dielectric strengths of commercially varnished cotton cambric and varnished fiber-glass cloth after various short-time heat treatments, the varnishes used being the same for both cotton and glass cloth.

It will be noted that in the case of the black varnish, there is little difference between the cotton and the fiber-glass averages after subjection to temperatures up to 250 degrees Fahrenheit. But after 350 degrees Fahrenheit and 400 degrees Fahrenheit for two hours, the averages for the glass are 11 per cent and 27 per cent higher than those for cotton.

The differences in the case of the yellow varnish (which, of course, has greater heat resistance than black varnish) are more pronounced, varying from 11 per cent with no heat treatment to 40 per cent after heating at 450 degrees Fahrenheit for two hours.

Perhaps the most plausible explanation for these results is that the heat treatment was sufficient to cause an appreciable amount of carbonization of the cotton fibers and yet not severe enough adversely to affect the dielectric strength of the varnish or the glass fibers. In fact the values in table V again demonstrate the already accepted fact that heat treatment of this nature definitely improves the dielectric strength of most insulating varnishes.

Table VI is included here to show some of the results obtained in efforts to determine what dielectric strengths could be expected of fiber-glass cloths impregnated with heat resistant varnishes after subjection to sustained high temperatures. Samples numbers 1, 2, 3, and 4 were prepared by hand-dipping 0.004-inch-thick continuous-fiber glass cloth (previously washed) into thin solutions of the varnishes, each being given three coats. Samples number 5 and 6 were varnished commercially (from same rolls as samples in table V).

In hand application ordinary varnished tapes are often stretched sufficiently to exceed the elastic limit of the varnish thereby impairing their insulating value. As glass fibers do not stretch, the varnish on glass tapes is not impaired in this manner.

It is not intended that these values be considered as directly comparative for the reasons that the thickness of the finished samples varies considerably and that two of the samples were varnished on standard varnishing towers and the rest hand-dipped. It is significant, however, that for five of the six samples the values of minimum dielectric strength are well above the accepted specifications for varnished cotton cambric and the minimum of one sample is over 1,400 volts per mil.

All samples were heated in a thermostatically controlled electric resistance

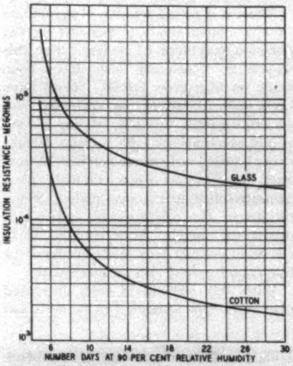

Figure 3. Samples of 0.010 inch by 1 inch by 1 inch black varnished cloth after heating at 350 degrees Fahrenheit for two hours

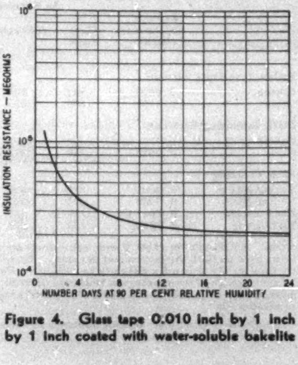

Figure 4. Glass tape 0.010 inch by 1 inch by 1 inch coated with water-soluble bakelite

Figure 5 (above). Standard roll of continuous-fiber tape 0.003 inch by ¾ inch

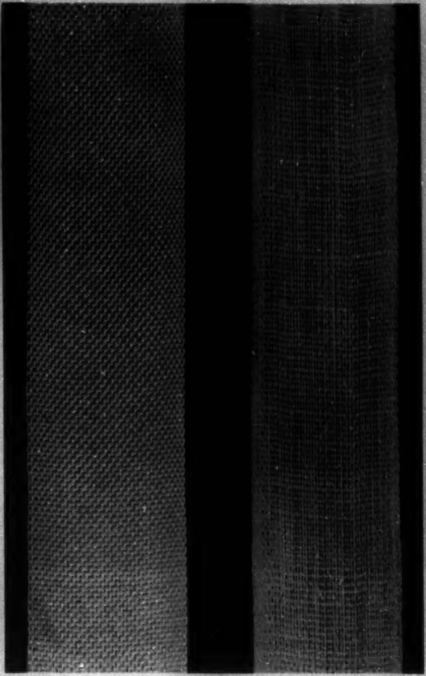

Figure 6 (left). 0.003 inch by ¾ inch standard continuous glass tape; right—0.007 inch by ¾ inch standard cotton tape

oven at 350 degrees Fahrenheit for 140 hours and tested for dielectric strength at room condition of approximately 80 degrees Fahrenheit and 65 per cent relative humidity within two hours after being removed from the oven.

It is well known that varnished textiles tend to become brittle at high temperatures and are therefore subject to cracking and subsequent dielectric failure. Glass textiles are no exception to this rule, but table V shows that the effect of the brittleness of the impregnant is not so pronounced on glass fibers as on the weaker and less-heat-resistant cotton textiles.

One test often used as a criterion for flexibility of a piece of varnished cloth is to bend it around a one-eighth-inch-diameter rod. If no cracks are observed in the varnish, the cloth is considered to be sufficiently flexible. Table V shows the results of a more severe test for the weakening or cracking of the varnish on cotton and fiber-glass cloths after various heat treatments. After the heat treatment, the samples were creased by bending double both ways and creasing between thumb and index finger as creasing paper for tearing. The two-inch-diameter electrode was placed directly over the crease in making dielectric strength tests.

It will be noted by comparing values in the third column in table V that creasing causes the "as received" yellow cotton to lose 62 per cent of its original minimum value, yellow fiber glass loses 41 per cent, black cotton 88 per cent, black glass 41 per cent. After 350 degrees Fahrenheit for two hours the percentage loss for yellow cotton is 80 per cent as against 39 per cent for the yellow glass cloth.

Insulation Resistance

A. Volume Resistance

The volume resistivity of the alkali-free glass used for electrical insulation is much greater than that of the glasses listed in the International Critical Tables. This is attributed to the absence of soda and potash which have been found by Littleton and Morey[1] to decrease the volume resistivity of solid glass considerably. Figure 2 shows the volume resistivity of electrical-insulation glass at different temperatures. Six determinations between 1,360 degrees Fahrenheit and 2,460 degrees Fahrenheit were made in essentially the same manner as was used by Kohlenrausch[2] in determining the conductivity of salt solutions. The electrodes used were made of platinum and were calibrated with a number of standard solutions having approximately the same range of resistivity as molten glass. A 1,000-cycle source of power was used and resistance measurements made by means of a General Radio type 650*A* impedance bridge.

B. Surface Resistance

Because of the high ratio of exposed surface area to the total weight of fiber glass (about 1,500 square feet per pound) measured values of insulation resistance at normal temperatures may be considered as purely surface resistance without appreciable error. This surface resistance is affected not only by the glass constituents, but to a very marked degree by the relative humidity of the atmosphere. This is illustrated in table VII which shows the insulation resistance

Table VII. Insulation Resistance in Megohms of Standard Unimpregnated Tapes One Inch Square After 72 Hours at 100 Degrees Fahrenheit and 90 Per Cent and 75 Per Cent Relative Humidity

	"From the Loom"		Washed	
	90 Per Cent Relative Humidity	75 Per Cent Relative Humidity	90 Per Cent Relative Humidity	75 Per Cent Relative Humidity
0.015 by 1 inch asbestos	0.57	1.08	1.5	5.5
0.011 by 1 inch cotton	1.03	35	30.5	380
0.015 by 1 inch glass	12.4	200	1,400	30,100

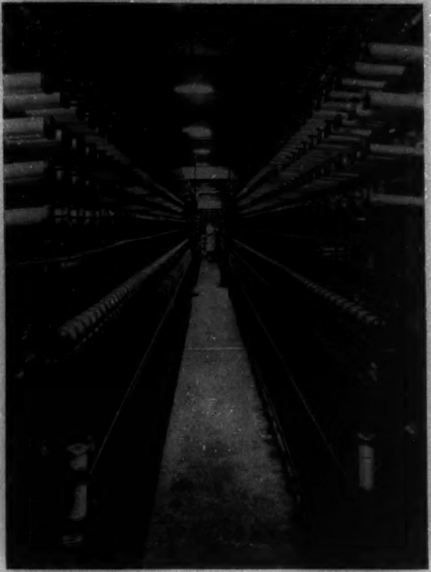

Figure 7. View in twisting mill showing the fabrication of continuous-fiber glass yarns

across one-inch-square samples of standard unimpregnated tapes both from the loom and washed (boiled 15 minutes in 0.5 per cent solution of soap in distilled water), after being held at 100 degrees Fahrenheit and the indicated relative humidities for 72 hours.

Figure 3 shows the variation in insulation resistance of one-inch-square samples of black varnished cotton and glass cloth over a period of 30 days at 100 degrees Fahrenheit and 90 per cent relative humidity.

When fiber-glass tape is coated (not impregnated) with a solution of a water-soluble bakelite and then dried, the insulation resistance becomes higher than that of the untreated tape. Figure 4 shows the variation in insulation resistance at 90 per cent relative humidity of a sample prepared in this manner. The solution of bakelite was $12^1/_2$ per cent solids by weight and the sample was dried for 90 minutes at 250 degrees Fahrenheit.

All insulation resistance determinations were made with a Leeds and Northrup insulation-resistance test set of the guard-circuit type consisting of high-sensitivity galvanometer, Aryton shunt, and lamp and scale device. The electrodes were brass clamps supported in a bakelite board on two-inch centers one being guarded by a brass ring. The bakelite board served as the cover for a $4^1/_2$-inch-diameter glass jar, in the bottom of which was a saturated solution of the proper salt for the desired humidity. (KNO_3 was used for 90 per cent relative humidity and $NaCl$ for 75 per cent relative humidity at 100 degrees Fahrenheit.) The guard circuit was complete from electrode to galvanometer. Source of current was five 45-volt radio *B* batteries in series. Readings were taken one minute after the specimen circuit was closed.

The results obtained by R. E. Ferris and G. L. Moses of the Westinghouse Electric and Manufacturing Company on the insulation resistance of a cotton-insulated coil and an asbestos insulated coil at 99 per cent relative humidity show essentially the same trend over a period of 14 days as is seen in figure 3 although the asbestos failed to ground on the 13th day.

Hygroscopicity

One of the most difficult problems to solve in connection with electrical insulation is that of failure due to the absorption of moisture. In some cases this is the primary concern of design and maintenance engineers. Glass insulation, being nonhygroscopic, is proving valuable under extreme conditions of high relative humidity coupled with intermittent high overload temperatures. Notable examples have come from the bituminous coal fields of western Pennsylvania. One company, which operates a large number of mining locomotives, reports that the average life of motor windings operating under the worst conditions has been lengthened considerably by the use of fiber-glass-insulated coils.

Reference to table IV will show that after washing and thorough drying, the average thickness of cotton and asbestos tapes had increased. This does not occur in glass tapes.

Glass insulation is available in the same forms and sizes as are other fabrics, the following being stocked:

Tape. Continuous fiber 0.005 inch by $^1/_4$ inch to 0.015 inch by $1^1/_2$ inches; staple fiber 0.010 inch by $^1/_4$ inch to 0.025 inch by $1^1/_2$ inches.

Braided Sleeving. Made from continuous-fiber yarns in standard inside diameters of $^1/_{16}$ inch, $^1/_8$ inch, $2^1/_4$ inches with wall thickness of 0.015 inch.

Varnished Cloth and Tape. Black and yellow varnished cloth and tape are available in the same widths and thicknesses as varnished cotton cambric.

Glass Insulated Wire. All sizes of round and square wire are being covered successfully with glass yarn, different thicknesses being used to obtain the same over-all diameters as standard wire insulated with asbestos, cotton, paper, and silk.

Combination Mica and Glass. A number of mica and glass combinations are being used for transformer coil, and armature coil, and slot insulation. This combination is a class-*C* insulation except for a small amount of organic binder.

Summary

In summing up the characteristics of fiber glass those which make it valuable for electrical insulation are:

1. Noninflammable and highly heat resistant. The operable temperature is limited only by the impregnant used, and there is a very definite temperature range between the operable temperature of organic fabrics and that of heat-resistant impregnants where glass is unique.
2. Exceptionally high tensile strength.
3. Nonhygroscopic.
4. Resists attack by moisture, acids, oils, and corrosive vapors.
5. Good thermal conductivity.
6. Excellent dielectric strength when impregnated.
7. High insulation resistance.

References

1. ELECTRICAL PROPERTIES OF GLASS, Littleton and Morey, John Wiley & Sons.
2. CONDUCTIVITY OF SOLUTIONS, Cecil W. Davies.

Discussion

L. E. Fogg and **R. B. Power** (Kennecott Wire and Cable Company, Phillipsdale, R. I.): Mr. Atkinson has outlined the properties of fiber glass which promise to make it an important factor in the field of electrical insulation. These properties are such that frequently fiber glass may be used advantageously in place of other insulations and it may provide a satisfactory solution for a number of difficult insulation problems. There are, however, a few comments that should be made with regard to the use of this material.

There is one point in connection with the mechanical strength of the material which would bear some clarification. The results of the flexing endurance tests as reported in table III show that a certain amount of heat treatment apparently toughens the insulation, and might make such treatment a desirable part of the preparation of the material. The cause for this improvement is not readily apparent, and should have some explanation by the author.

In table IV giving dielectric strength comparisons, Mr. Atkinson has indicated slight improvements of the glass, and slight loss for the other insulations through washing. For thin insulations there is always an increase in the dielectric strength as the thickness of the material is decreased. It seems probable that this factor is more responsible for the slight increases and decreases noted above, than any other change in the insulations. The cotton and asbestos fabrics, showing increases in thickness with washing, show a corresponding decrease in dielectric strength, while the glass, with a decrease in thickness shows an improved dielectric strength.

The lubricating oil applied to the fiber during manufacture may have some slight effect on the dielectric strength of the glass tape. However, this oil may be very detrimental if thermal and electrical conditions of insulation use are such as to make dielectric loss an important factor.

It is worth pointing out that the dielectric strength of asbestos may vary greatly from the values shown in the same table, depending on the method of preparing the asbestos tape. Some comparative dielectric-strength measurements were made recently at our laboratory on fiber glass and asbestos tape in which the asbestos had been fabricated on a cotton backing. Although the fiber glass gave results almost identical with those of Mr. Atkinson, this asbestos tape showed about 60 per cent higher dielectric strength than did the samples he reported.

It is unfortunate that in most applications of fiber glass as insulation it is necessary to use some bonding, saturating, or coating material in conjunction with the glass. Some tests which we have made recently show that if fiber glass alone could be used it would retain some insulating properties at temperatures approaching its softening point. Such temperatures cannot be attained in practical applications, however, and in most cases the maximum operating temperatures for fiber-glass-insulated equipment will be limited to a moderate value by associated insulating materials. These facts should be kept in mind when considering fiber-glass insulation for high-temperature operation.

Unquestionably, glass as a fibrous insulating material will eventually find its proper place among other similar materials. The information presented by Mr. Atkinson should indicate some places where its use would be definitely practicable.

R. W. Wieseman (General Electric Company, Schenectady, N. Y.): For rotating-machine windings a large variety of class *A* organic insulating materials are available. These materials are inexpensive, easy to apply, and they have been used successfully for many years. As the machine voltage, speed, and size increased inorganic materials were introduced to increase the life of the windings by resisting the action of mechanical stresses, corona, and elevated temperatures.

Unfortunately only two inorganic materials were suitable for rotating-machine windings up to the present time, namely, asbestos and mica in built-up form. Mica is an excellent high-voltage high-temperature moisture-resisting insulator if properly filled and bonded. It is limited mechanically, however, by the characteristics of its bonding medium. Some asbestos textiles contain as much as 20 per cent cotton. It is well known that asbestos absorbs moisture readily and it contains natural ferrous particles and conducting salts which must be removed if it is to be used in the region of high-voltage stresses. Asbestos, therefore, is usually confined to low voltages where mechanical strength is required at elevated temperatures and as an armor for high-voltage mica insulation. It is apparent that present inorganic materials, although satisfactory, are not ideal for rotating-machine insulation.

The introduction of glass-fiber insulation is welcomed by those interested in rotating machines. When properly filled with a suitable varnish or compound, glass fiber is a good class *B* insulating material. Up to the present time glass fiber alone is not a competitor to mica for high-voltage windings. Glass fiber, however, should be beneficial in many respects. First, it can replace cotton which is used for structural purposes in class *B* insulation, and thereby produce better mechanical and thermal properties. Second, it can replace asbestos with an improvement in uniformity, mechanical strength, and voltage strength. Third, it can partially replace mica on low- and medium-voltage coils with an improvement in mechanical strength. It also can be used as an armor or outside covering for high-voltage mica-insulated coils.

D-c and a-c machines have been built with glass-insulated wire in both the armature and field windings. Coils of many kinds have been insulated with glass tape and mica in several combinations. The end connections of armature windings have been insulated with treated glass tape and combinations of glass and mica tape. A 1,500-kw 600-volt d-c machine was insulated entirely with mica and glass. None of its six windings contains cotton or asbestos in any form. Insofar as machine temperatures are concerned the thermal limitation of the windings of this machine is governed only by the insulating varnish and filling compounds used in building the insulation structure.

Glass fiber in various forms can be used to advantage in rotating-machine insulation. The extent to which it will replace present materials will depend somewhat on technical and economic conditions because its feasibility has been established.

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Atkinson's paper provides a very complete comparison of the characteristics of cotton and glass when used as insulating materials. In the future, glass will undoubtedly provide an answer to many of our insulation application problems.

A number of other characteristics of glass insulation would, of course, be of interest. Certain of these are:

1. How does the dielectric strength of glass under impulse conditions compare with that of other insulating materials?

2. In transformer applications, a majority of the dielectric strength between turns is provided by the oil-saturated paper covering on the conductors. Would a glass reinforcement over the paper provide better dielectric strength?

3. One of the problems which confronts the manufacturer is that of getting varnish into the interior of coils. With cotton insulations, the varnish can be "wicked" in only a short distance. How do the "wicking" properties of glass compare with those of cotton?

E. L. Lotz (New Jersey Wood Finishing Company, Woodbridge, N. J.): The information on fiber glass presented by Mr. Atkinson is extremely valuable and timely in view of the newness of the product and of the widespread interest in this type of insulation and of the possibility of the increased temperature ratings of electrical equipment and allied insulation.

With the tremendous advances made in the manufacture of insulating varnishes and impregnating compounds in the last few years, it has become more and more apparent that the limiting factor in the operation of insulation at high temperatures is not the varnish or impregnating compound, but the carrier on which they are used. Cotton when impregnated or coated and subjected to high temperatures (125 degrees centigrade) fails only in a mechanical sense, that is, it fails in so far as tensile and tearing strength are concerned but does not fail electrically as the dielectric strength of the varnish or compound will, if anything, be higher after the heat treatment. The use of fiber glass as the carrier should insure both good electrical and mechanical properties even after exposure to high temperatures for long periods of time.

The dielectric strength of cotton and glass impregnated with the same varnish is substantially the same as is also the case for the two in the unvarnished state. This means that the dielectric strength is determined almost entirely by the type of varnish used, as is clearly shown in table VI of the paper.

The curves in figure 3 are very interesting and would tend to show that varnished glass cloth is considerably better than varnished cotton cloth under conditions of high humidity. I have tested both glass and cotton for resistance to moisture and have found that varnished cotton compares very favorably with varnished glass in respect to power factor and dielectric strength. The experiment consisted of vacuum-drying samples of black varnished cotton and fiber glass at

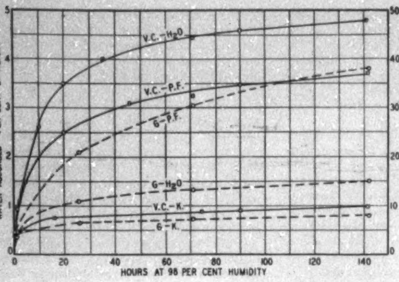

Figure 1. Water absorption of varnished glass and cotton

105 degrees centigrade for six hours under a vacuum of 0.1 millimeter and then exposing the samples to 98 per cent relative humidity and determining the amount of water absorbed, the power factor, and dielectric constant. The result of this experiment is shown in figure 1 of this discussion. The curves are plotted against hours of exposure and show that even though the varnished glass absorbed much less water, the resultant power factor was just as high as the varnished cotton. The curve designated *V.C.-P.F.* means the power factor of the varnished cotton, whereas, *G-P.F.* is the similar curve for varnished glass. In both cases, there was an initial rapid increase in water absorbed, power factor, and dielectric constant in the first ten hours.

In order to compare the dielectric strength after exposure to moisture, samples which had been vacuum dried as above were immersed in water at 24 degrees centigrade for 24 and 42 hours and the amount of water absorbed and decrease in dielectric strength measured. The dielectric strength was measured using one-fourth inch electrodes and the short time ASTM method of test. The results are shown in table I of this discussion. After 24 hours, the varnished glass had absorbed 0.77 per cent water by weight and had a reduction in dielectric strength of 46.2. Varnished cotton absorbed 3.2 per cent water by weight, but the reduction in dielectric strength was only 30.85. After 42 hours the glass had absorbed 1.1 per cent water by weight with a reduction in dielectric strength of 59.2 per cent. The varnished cotton absorbed 4.35 per cent water by weight and had a decrease in dielectric strength of 48.5 per cent.

Table I. Water Absorption-Dielectric Strength

Exposure	Water Absorbed (Per Cent by Weight)	Per Cent Reduction in Dielectric Strength
Black Fiber Glass		
24 hours in water at 24 degrees centigrade	0.77	46.2
42 hours in water at 24 degrees centigrade	1.1	59.2
Black Cotton Tape		
24 hours in water at 24 degrees centigrade	3.2	30.85
42 hours in water at 24 degrees centigrade	4.35	48.5

In the above experiments, the same varnish was used on both the cotton and glass and it is reasonable to assume that in each case the varnish itself would be affected to the same degree so that the difference in the performance of the two must be directly dependent on the carrier. Since glass is nonhygroscopic the mechanism of the water absorption is probably of the nature of a surface layer on the glass fibers. In the case of cotton most of the water is taken up by a wick action and since the carrier itself does not contribute anything to the dielectric strength, the presence of the large amount of water in the cotton does not show up in the dielectric-strength measurements.

Lloyd E. Miller (Reliance Electric and Engineering Company, Cleveland, Ohio): Mr. Atkinson's paper is so complete as to detail, that it leaves practically nothing to the imagination and very little concerning the physical characteristics given in the paper, to discuss.

There is, however, a feature that might very well be discussed regarding the commercial usage of this material. The high temperature to which fiber glass can be subjected has been stressed in this paper and in previous papers and talks. It might be said to have been overstressed. It has been stressed so much that it has been ill advisedly seized on by many as proof that with this material the industry is now ready to double the temperature at which electrical machinery can be operated.

The fact that impregnants must be used in conjunction with fibrous insulation, and the fact that all satisfactory impregnants to date are entirely, or in part, of organic material, and are, therefore, the limiting factor in such combination, seems to have been neglected. It is noted that Mr. Atkinson mentions this in his paper, but it seems that it should be more strongly emphasized than in the past. This fact should serve as a spur to the varnish manufacturers.

It does seem that the electrical industry might profitably consider higher temperatures than are now sanctioned, but extreme changes due to new materials should be approached with caution.

It should be borne in mind that the ability of fibrous insulation to withstand high temperatures is not of itself sufficient to justify such changes. Fire hazard must be considered, as must also strains and movements set up by expansion and contraction.

There is no doubt that the new fibrous glass has given the electrical industry a substantially improved insulating material. It can be accepted as a fact that its heat-resisting properties are considerably above those which can be used by the industry at the present time. It would, therefore, seem that efforts of the manufacturers should be along the line of making the product easier to use and apply.

Graham Lee Moses (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Atkinson and his colleagues in the Owens-Corning Fiberglas Corporation have undoubtedly made a very significant contribution to the insulation art. Insulation engineers have long felt that existing class *B* insulating materials had definite limitations and it is hoped that this new material will overcome these defects.

Our experience with fiber glass parallels Mr. Atkinson's on some phases of tests on the fundamental material. However, our experience has largely been in connection with actual commercial machines. We have found that its success depends upon the correct application. It has proved satisfactory on equipment where weight and space requirements necessitate high-temperature operation and the insulation is exposed to high humidity conditions. Our experience in the application of glass insulation to transportation apparatus is detailed in an article by Mr. Ferris and myself which appeared in the December 1938 issue of ELECTRICAL ENGINEERING.

Our practical and design experience with glass insulation on commercial apparatus covers a period of nearly two years involving over 50 types of motors and generators and several hundred units. It has also been used on many types of control.

Fiber-glass insulation has its share of desirable characteristics and naturally has its limitations. It should not be substituted wholesale for any existing insulating medium but should be applied with care and consideration only where it is required and its use is economically justified. Applications should be avoided which bring out or emphasize its limitations or weaknesses.

Glass insulation by virtue of its excellent tensile strength under high temperature conditions lends itself to being used for binding class *B* coils and holding other forms of insulation in place. It makes an excellent finishing tape producing the desired strength with long life at high temperatures and a fine glossy finish when properly treated. The excellent tensile strength permits the use of thinner class *B* tapes thereby obtaining improved space factor.

Because it is a woven material its inherent (untreated) dielectric strength is only comparable to that of similar conventional materials. When treated it takes on the characteristics of the impregnant film as do other woven materials. In our opinion it should not be used for the main ground insulation on high-temperature class *B* apparatus. This material is of course not to be considered to replace mica for ground insulation.

One of the present limitations of glass insulation is its relatively low resistance to abrasion when the fibers are not lubricated or supported by impregnation. In tape form the fibers are lubricated by an oil film applied during manufacture. In the apparatus this must be supplemented by varnish or other impregnant. When this lubricant or impregnant is eliminated or its qualities destroyed, vibration is apt to cause abrasive destruction. This very definite limitation must be faced by engineers applying this material.

Another limitation is the relatively low shear strength of the glass fibers. This is an exhibition of the brittleness which has long been associated with glass. To avoid this, apparatus should be designed to prevent exposing glass insulation to sharp corners or edges.

The smooth, silky appearance produces another handicap as it permits the warp to slip on the weft, unless a very close weave is used. This tendency can be minimized by pretreating the tape in varnish and only partially curing before applying the tape.

The next logical step in the development of high-temperature insulation is the production of a high-temperature impregnant properly to lubricate or support glass insulation and give it high dielectric strength at elevated temperatures. In most of the previous points discussed the impregnant is an important factor. The importance of this cannot be overemphasized and the development of fiber-glass insulation has in effect "passed the ball" to the impregnant manufacturers.

In our search for improved insulation to operate at elevated temperatures it must be remembered that as insulation improves it ceases to become the determining factor. Already there are important considerations requiring the operation of some classes of machinery at temperatures below that now permissible for insulation.

One interesting and spectacular experiment which we made, was to operate glass-insulated magnet coils at temperatures around 575 degrees centigrade (1,067 degrees fahrenheit). Some of these operated several hours without failure even though the impregnant was completely destroyed. Naturally, life at this temperature is limited and there was no intent to operate apparatus at such temperatures but it was merely done to demonstrate the reliability under extreme conditions due to failure elsewhere in the control equipment.

Briefly glass insulation is an important addition to our family of insulating materials. It is not a panacea nor a cure-all. In our opinion it should be applied judiciously with full appreciation of the limitations as well as the important advantages. Westinghouse engineers, in the early stages of this development, recognized the possibilities of fibrous glass insulation and recommended the use of the continuous-filament type.

T. R. Walters (General Electric Company, Pittsfield, Mass.): The data presented in this paper add considerable to our understanding of this new and very interesting insulating material. The point is well brought out that in the field of varnished fabrics the fabric base is the weaker part of the structure and consequently, replacing the organic with an inorganic fabric has a very beneficial effect on the heat resistance of this class of material.

There are several other points which I would like to bring up. One is that while the data show definitely that fiber glass will successfully withstand a much higher temperature than either cotton or asbestos before losing its tensile strength, these tests were all on a short-time basis. It would be interesting to know just how high a temperature these new fibers will stand successfully for very long periods of time. Also, as a matter of record, were the tensile tests reported taken at the temperatures shown on figure 1, or were they taken at room temperature after aging at the indicated temperatures?

In the section concerning dielectric strength, it is stated that "The dielectric strength of unimpregnated fiber-glass insulation as it comes from the loom is about the same as that of equivalent cotton textiles." This being so, I would be interested in the author's theory as to why fiber-glass insulation produces higher dielectric strengths than cotton when the same varnishes are used. A possible explanation is given for the results obtained after heat treatment but not for the higher results obtained on the samples before aging.

In another part of this same section, a statement is made that "In hand application ordinary varnished tapes are often stretched sufficiently to exceed the elastic limit of the varnish, thereby impairing their insulating value," followed by the statement that since glass fibers do not stretch, the varnish on glass tapes is not impaired. Varnished cotton cloth can also be made which will not stretch. Quite often, however, due to the nature of the surface to which the tape is being applied, it is necessary to use tape which will stretch. The decrease in dielectric strength in this instance, is small for the amount of stretch usually required for satisfactory taping.

Wm. A. Del Mar (Habirshaw Cable and Wire Corporation, Yonkers, N. Y.): Fiber glass, from the point of view of the cable designer, is not so much an insulating material as a vehicle or filling for such insulating materials as wax, varnish, or lacquer. In this respect it may be regarded as a competitor of cotton, dry paper, and asbestos. The characteristics of fiber-glass insulation, such as dielectric strength, power factor, and ability to resist heat, cold, or moisture, are essentially those of the associated wax, varnish, or lacquer. Fiber glass gives body and strength to these materials, enabling them to be applied in thicker layers. Its advantages over other fibers are that it does not impair the useful characteristics of the wax, varnish, or lacquer with which it is associated. From this it would appear that the title of this paper is misleading as fiber glass is merely an inorganic vehicle for air or organic insulation.

Fiber glass has a definite value for certain classes of wire such as magnet wire, fixture wire, and small motor leads but, so far, it has not made much headway for the larger types of cables.

Fiber glass is a valuable braid material as it does not decay, burn, or become waterlogged. Here, again, its characteristics are limited by the organic filler which must be used. It is more likely that its application to power cables for some time to come will be along this line rather than for the insulation itself. The present high cost of the fiber glass, as compared with other braid materials, limits its use in large cables to services where other available materials show abnormally rapid deterioration.

Varnished glass cloth insulation, as now made, does not stand heat (96 hours at 125 degrees centigrade) as well as heat-resisting varnished cambric made with the same varnish. It becomes more brittle than the cotton base material. It is to be hoped that this material will be developed to the degree of heat resistance that seems to be inherent in the component materials.

K. N. Mathes (General Electric Company, Schenectady, N. Y.): Mr. Atkinson's paper constitutes an excellent survey of the properties of glass fiber insulation. From the standpoint of the manufacturer of electric apparatus the properties and service life in built-up form are of primary importance. Service trials and laboratory tests have established that the electrical and particularly the physical properties of glass fiber insulation depend largely upon the nature and thoroughness of the compound or varnish treatment.

Untreated glass fibers are extremely sensitive to nicking, and unless protected can even cut and destroy each other under conditions of abrasion and vibration. For this reason a film of oil or sizing is applied to the fibers to protect them in the textile operation and allow their use as yarn or fabric. The sizing used at present is somewhat corrosive to copper, hygroscopic, and, in addition, is not truly compatible with most insulating varnishes. Because of the nature of the sizing and the high density in the case of the continuous-filament yarn, it is difficult to secure complete varnish or compound penetration. Although glass is not hygroscopic in the sense that the solid fibers can absorb water, moisture can be picked up in the following ways:

1. As an adsorbed film upon the glass surface.
2. By adsorption in the sizing.
3. By capillarity between fibers or threads.
4. By capillarity between fibers and surrounding varnish which may not satisfactorily wet the fiber.

As a result the moisture resistance of glass fiber as measured by the electrical properties in many cases is only comparable to cotton.

When used as major insulation, the glass fabric serves merely as a matrix to carry the insulating varnish. The extent of the deterioration of the varnish over long periods of time is extremely important. The most characteristic effect is the slow embrittlement and tendency toward cracking which may seriously impair the dielectric strength. In contrast, the overlapping flakes of pasted mica depend upon varnish only as a binder. In addition, since all organic varnishes deteriorate under the action of corona, the varnished glass fabric cannot be considered as a substitute for mica in high-voltage applications.

This discussion emphasizes limitations and precautions which are important to recognize since glass fiber is assuming so rapidly an important place in the field of electrical insulation. Glass insulation has found use in many types of electric apparatus, and will find increased use with further development, and as service data more firmly establish the advantageous properties. In comparison with other types of insulation, in many cases economic consideration may be the deciding factor for use.

H. C. Louis (Consolidated Gas, Electric Light and Power Company of Baltimore, Md.): The data in the paper giving the results of the elaborate tests on fiber-glass insulation show this material to have some very desirable and superior characteristics and to open up some very attractive possibilities. The development of this insulating material marks a definite step forward in the progress of insulation development.

Numerous mechanical tests were made as described in the paper, but we wish to stress the importance of giving full consideration to the effect of fatigue effects from vibration. The possibility of deterioration or even pulverization of such material due to this effect in extended service should not be minimized. Operating experience shows numerous troubles in materials to be traceable to vibration fatigue, and possibilities of anticipating and preventing this call for the very best efforts and attention in investigations of the material under consideration.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The utilities are, of course, gratified to learn again that the manufacturers are investigating the new kinds of insulation as they are brought forth. Obviously, when these insulations are suitably developed by themselves or as incorporated in apparatus, many utilities will be glad to use them when they become attractive commercially as well as technically.

Referring to the newer insulation, that is, glass, it would be of interest to learn what developments have been made with other types of binders than the varnish and bakelite mentioned by the author. This question, of course, is raised in connection with frequent or continuous operation of glass insulation in apparatus at such temperatures as 150 or 175 degrees centigrade.

F. W. Atkinson: L. E. Fogg, R. B. Power, and Lloyd E. Miller refer to the fact that the operable temperatures for glass insulation in most applications must be limited by the impregnant used. Mr. Miller advises caution in design and rating changes in the direction of higher temperature operation, and Herman Halperin raises the question as to impregnants which may be capable of withstanding temperatures such as 150 degrees centigrade or 175 degrees centigrade.

The author fully realizes the necessity for caution in raising the temperature ratings of electrical apparatus in general and that many other considerations make it impossible to operate at temperatures even approaching those which glass will stand. However, as E. L. Lotz and Graham Lee Moses have pointed out in their discussion, as far as the insulation is concerned, the limiting factor with impregnated glass is the impregnant, whereas with class *A* insulations, it is the fabric. Our tests of numerous varnishes on glass and cotton fabrics definitely show that the better heat-resistant varnishes will stand temperatures considerably higher than those which cause cotton to lose practically all of its mechanical strength. Our experience with the relative heat resistance of varnished glass cloth and varnished cotton cambric does not parallel that of Wm. A. Del Mar. Of three different samples of yellow varnished cotton cambric held at 125 degrees centigrade for 168 hours, only one could be bent 180 degrees around a one-eighth-inch-diameter mandrel without cracking. All of the six samples of yellow varnished glass cloth (each varnished by a different company) withstood this test. A few of the better commercial varnishes (applied to glass cloth) have been found to pass this test after a week at 175 degrees centigrade. One experimental impregnant, partly organic, retained remarkable flexibility after 120 hours at 200 degrees centigrade and its minimum dielectric strength was over 1,500 volts per mil (short-time method, one-fourth-inch electrodes, material 0.010 inch thick). It seems reasonable, then, to assume that:

1. The temperature limits of class *A* insulations are determined by the fabric and not by the impregnant.
2. With a fabric that will stand it, varnishes are available which can be used where hot spot temperatures are 150 degrees centigrade or higher.
3. The final word has not been written about heat resistance in impregnants as varnish manufacturers and individuals are making great strides in the improvement of this quality. It is believed that the introduction of glass fabrics into the insulation field has provided further inducement to effort on this particular research problem.

K. N. Mathes and Graham Lee Moses have mentioned the low resistance of untreated glass fabrics to abrasion and shear on sharp edges. This probably is the weakest property of the untreated glass at the present time, although that is not true of treated fabric and it does not seem to be of primary importance. Laboratory tests along this line are not very conclusive so it seems pertinent to refer to the experience encountered by manufacturers in the actual application of "Fiberglas" insulating materials during the three years since their introduction. Few have reported any lasting difficulty due to abrasion and shear and some have indicated as much as 15 per cent saving in labor in the winding of coils. This would hardly be possible if much difficulty were encountered due to nicking and shearing during fabrication. It should be pointed out further that continuous research is definitely minimizing this weakness.

Mr. Mathes mentions the important point of thoroughness of impregnation of treated Fiberglas insulation. This problem, of course, is as applicable to glass as to the other textile insulating materials but no particular difficulty has been found in our laboratory in obtaining thorough impregnation of glass cloth, and the impregnation of wound coils is being carried on by numerous manufacturers with facility and efficiency at least as great as with cotton and asbestos. We have found, however, that some types of varnish wet glass fibers much better than others and this seems to be the high hurdle in accomplishing thorough impregnation of Fiberglas. Heat treatment definitely improves impregnation by strengthening the bond between the varnish and the fibers. We believe this to be, in part, the answer also to the question of Messrs. Fogg and Power regarding the increase in flexing strength of varnished cloth after heat treatment. Another explanation is that the polymerization of thermosetting varnishes adds considerably to their toughness.

T. R. Walters raises the question of variation in tensile strength of Fiberglas with time of exposure to high temperatures. Our experience indicates that there is very little change in the tensile strength of electrical glass (alkali-free) after the first 24 hours.

Cloths made of this type of glass and used for steam-turbine blanket covers retained from 30 per cent to 40 per cent of their original tensile strength after 12 days next to a hot plate maintained at 900 degrees fahrenheit (482 degrees centigrade). This is about the same loss in strength as shown in figure 1. The significant thing, however, is that even after this treatment, the glass cloth is stronger than cotton and asbestos are with no heat treatment.

Messrs. Louis and Moses bring up the point of deterioration due to vibration. We know definitely that if this is an inherent weakness in Fiberglas insulation, it can be proved only after a longer period of time than Fiberglas-insulated apparatus has been in operation; for as far as is known, no failures have been attributable to vibration.

Unfortunately we have at the present time no data to compare directly with the interesting curves presented by E. L. Lotz. However, we have measured the 60-cycle power factor of a number of different samples of varnished cotton and varnished glass cloth at room conditions, and have found no cotton sample whose power factor was lower than 13 per cent nor any glass sample whose power factor was higher than 7 per cent.

So far as we know, no impulse tests, as Mr. Dissmeyer has suggested, have been made. As far as the reinforcement of the dielectric strength of the paper insulation in transformers is concerned, untreated glass could offer little; but varnished glass should provide much greater dielectric strength and resistance to the effects of corona. With reference to the "wicking" of varnish in glass-covered coils, there seems to be no difficulty. In fact, although coil manufacturers are naturally loath to disseminate such information, there is reason to believe that some of them consider that a fewer number of dips are necessary for some types of glass-insulated coils than for other types of insulation.

Temperature Limits and Characteristics of Mica as Used in Conjunction With Class "B" Insulation

By ROBERT H. SPRY
ASSOCIATE AIEE

THE TYPE of class *B* insulation referred to in this paper is primarily intended for small and medium sizes of general purpose motors, generators, and associated equipment.

Of the many different varieties of micas found in the world's mineral deposits, only two are used for electrical insulation, namely, muscovite and phlogopite. Commercial sources of supply are found mainly in India, Madagascar, Africa, Brazil, and Canada. The mica-bearing veins are very erratic in their occurrence; in some localities they are found on and near the surface, while in others, they are several hundred feet below the ground. The yield of commercial mica obtained per ton of rock may be as low as one per cent. In general, the area of rough mica slabs varies from approximately 2 square inches to 50 square inches. However, in exceptional cases, pieces are found as large as three feet by four feet. After the extraneous rock and other incrustations are removed from the slab of mica, it is then split into thicknesses of approximately one-eighth inch—commercially termed "blocks." The blocks are further classified for quality and graded for size by experienced native labor. Details regarding the various recognized qualities and gradings may be found in ASTM method *D*-351-37*T*.

Muscovite mica is often referred to as India mica, white mica, ruby, potassium mica, etc. Clear muscovite films have a dielectric strength of approximately 3,000 volts per mil to 900 volts per mil for thicknesses of 0.002 inch to $^1/_{16}$ inch respectively at 25 degrees centigrade. For temperatures of 300 degrees centigrade the dielectric strength is roughly equal to 75 per cent of the stated values. The power factor varies from 0.0001 to 0.0008 at 25 degrees centigrade for a frequency range of 60 cycles per second to 1,000 kilocycles per second. The dielectric constant under the same conditions is 6.5 to 8.5.

Phlogopite mica, which is more universally known as amber or silver mica is softer physically than the muscovite micas. Selected grades of phlogopite mica may be operated at temperatures of 800 degrees centigrade to 900 degrees centigrade as compared to 400 degrees centigrade to 500 degrees centigrade for muscovite mica. Its dielectric properties are, however, poorer than the muscovite micas, the dielectric strength being approximately 70 per cent of the values quoted above and the power factor has a rather wide spread of 0.003 to 0.09.

The most important characteristic of mica which permits its wide use as a dielectric is its basal cleavage. Films or splittings as thin as 0.0003 inch may be produced by skilled natives. Specifications of commercial splittings range from 0.0006 to 0.0013 inch in thickness. By using these thin films or splittings, a high degree of flexibility is obtained for such applications as coil wrappers and molding micanite plates.

The physical limitations of the size or area of the natural micas are overcome, to a large extent, by assembling the films into large sheets with the aid of suitable binders. A wide variety of types of "built-up" or Micanite plates and wrappers are obtained by combinations of binders, adhesives, carriers, and heat and pressing operations. The following general classifications of micanite plate and other combinations of mica fall within the AIEE definition of class *B* insulation:

Group I. Hard, rigid sheets for commutator segments, etc., contain a minimum percentage of binder (shellac or Glyptal) which has been polymerized to prevent exuding of the binder and slippage of the mica films when subjected to heat and pressure. Organic-bonded heater plates for resistance elements that are mechanically supported also come within this group.

Group II. Hot molding plates for cylinders, vee rings, slot cells, etc., containing from 10 per cent to 20 per cent of shellac or Glyptal binders which are undercured to facilitate molding and forming operations. Additional heat treatment or curing cycles are required on this type of plate to make it suitable for supporting mechanical loads at elevated temperatures.

Group III. Cold flexible plate for slot, phase, and ground insulation can be formed readily at room temperatures. The binder is essentially composed of nondrying oils, plasticizers, and natural and synthetic resins. It, therefore, should not be used under high mechanical loads where stability is of paramount importance.

Group IV. Composite Insulation. The mica splittings are usually backed with, or sandwiched between, carriers such as capacitor tissue, silk, cotton, cellulose acetate, Fiberglas, asbestos, slot papers, or varnish-coated cloths. The carriers provide sufficient mechanical strength to enable the mica films to be applied directly to the conductors and coils. The binders used in these combinations are comparable to those listed in group III with the addition of asphalts and pitches.

The percentage of mica by weight in the composite insulations varies over wide limits, depending upon the materials used. For the thinner carriers such as capacitor tissues and silk, the average quantity of mica is in the order of 65 per cent, the remainder being binder and carrier.

In the past decade, numerous inorganic materials have been tried for binders in order to increase the operating limits of electrical apparatus. The apparent limitations of all the inorganic binders are the high heat treatments or temperatures required to make the binders chemically and electrically stable. Following such heat treatments, inorganic binders lose their flexibility and become, in most cases, a brittle, crystalline structure. Therefore inorganic-bonded mica is limited to simple rigid forms such as plates and cylinders. The large number of plasticizers and synthetic resins now available has improved the retention of flexibility in organic binders and varnishes. A specific example of the improvement in the heat resistance of varnish films within the last six years is represented by the following data which we have collected on varnished cambric cloth.

The test procedure in determining the respective heat resisting properties of the varnish film is accomplished by hanging narrow tapes (approximately one inch in width by six inches to ten inches in length) in ovens maintained at temperatures of 100 degrees centigrade and 125 degrees centigrade plus or minus 2, degrees, respectively. Samples are withdrawn at regular intervals and bent around a one-eighth-inch diameter mandrel. A checking or cracking of

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A paper presented under the auspices of the AIEE committee on electrical machinery at the AIEE winter convention, New York, N. Y., January 23–27, 1939.

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the varnish film is considered as the end point or failure and the total number of hours is recorded for the particular sample. In 1932 the average life of varnished cloth was in the order of 500 to 600 hours at 100 degrees centigrade, and 100 to 150 hours at 125 degrees centigrade. Today the special varnished cloths of comparable thicknesses are capable of withstanding in the order of 2,500 to 3,000 hours at 100 degrees centigrade, and 350 to 450 hours at 125 degrees centigrade. By sacrificing some of the electrical properties, the heat resistance can still be greatly improved on certain grades of varnished cambric cloth. It is recognized that in view of the fact that the entire surface of the varnish film is exposed to the oxidizing and/or polymerizing action of the atmosphere, the rate is considerably greater than would be encountered in practical applications of multiple layers. Therefore a correlation between test data and practical experience must be made for each condition encountered.

Because of basic differences between varnish-coated products of class *A* insulation and the combinations of mica and coils for class *B* insulation, it is difficult to establish reliable test methods for determining heat-resisting properties. The binders for the mica products must first have a certain minimum degree of adhesion to hold the splittings in place in addition to the other electrical and physical requirements; second, due to the overlapping laminated structure of the mica films, tests such as tensile strength, tearing, bending, etc., are unreliable because they depend essentially upon the carrier and not the over-all combination of the insulation.

It appears that the only dependable and satisfactory way in which to determine the aging characteristics and temperature limits of class *B* insulation is to apply them to the individual unit of electrical apparatus where such variations as heating and cooling cycles, expansion and contraction, vibration, high and low humidities, corrosive vapors and gases, etc., are present.

Doctor T. S. Taylor's paper on "Repeated Thermal Expansions and Contractions and Their Effect Upon Long Armature Coil Insulations" (AIEE, 1924) is the only specific data that can be found on this subject. A model of four slots having an over-all length of 110 inches was built up from punchings. Ventilating ducts $^1/_2$ inch and $1^1/_4$ inches wide were included in an attempt to duplicate present engineering practice. Eight coils, two per slot, were insulated with various types of class *B* mica-tape insulation. The model was subjected to heating and cooling cycles with increasing temperatures of 75, 100, 130, and 160 degrees centigrade for a total of some 11,500 periods. Following each series of tests a potential of 37,000 volts was applied from conductor to ground with no breakdowns. At the duration of the experiment, an examination of the coils disclosed that the carriers and binders were completely destroyed in and near the several air ducts and that considerable deterioration had occurred in the slot section. I refer to this paper somewhat in detail for predicating the following comments.

First, for the relatively short period of time consumed in making these observations as compared to several years of operating life of the normal motor, temperatures in the order of 160 degrees centigrade were sufficient to destroy the organic carriers of the mica films and the bonding properties of the varnishes.

Second, in and near the air ducts, where the insulation was not mechanically supported, and also subjected to a higher rate of oxidation, there was excessive deterioration and swelling of the composite structure.

Third, the dielectric properties of the mica were not affected as evidenced by the high potential tests.

Fourth, in the absence of vibration and continuous voltage stresses, the individual factors of expansion and contraction of these long coils did not have a deleterious effect upon the insulation.

If the model had been subjected to vibration and electrical stresses comparable to conditions found in practice, it is quite possible that some of the mica films would have shifted, particularly those in and near the air duct; also there would no doubt have been added deterioration due to ionization in the spaces created by the volatilization and disintegration of the binder and carrier.

Thermal conductivity of a given insulation and indirectly its life, is a function of its density or compactness. Therefore, if the best composite insulation now being made should be loosely applied and poorly impregnated, its ability to withstand elevated operating temperatures would be seriously reduced. This factor alone probably accounts for the large deviations in the operating life of electrical apparatus. Any time-temperature relationships should also take into consideration the compactness of the coil as well as its intimate physical contact with the slot walls or other supporting and heat-conducting mediums.

If it were possible to analyze failures on the basis of those due to vibration versus pure dielectric breakdown, we would no doubt become more critical and active in establishing standards for the former. The swelling or protruding of the coil insulation due to the normal coefficient of expansion of the composite structure and possible volatilization of the bond at elevated temperatures in the air ducts and at the end of the slots creates a localized mechanical problem. Continued vibration may be sufficient to pulverize both the carrier and mica at these areas. At the higher operating voltage stresses the swelling of the coils may also introduce ionization in the coil structure.

Where space factor or the minimum frame size for a given horsepower output is limited such as, railway motors, the AIEE have established separate and special operating temperature limits for the class *B* insulation. The permissible hot spot temperature has been increased from 125 degrees centigrade to 175 degrees centigrade. Quite frequently the heat generated by the I^2R losses in starting and the core losses due to higher operating flux densities exceed 175 degrees centigrade on motors having insufficient thermal capacity. Unless some emergency exists, the duration of these excessive temperatures is for relatively short periods of time and therefore gives a reasonable operating life for the motor. The deteriorating effects of the higher operating temperatures and the extreme conditions of vibration and weather on the insulation of railway motors is in most cases of secondary importance to the maintenance of time schedules. To obtain a reasonable operating life at these temperatures the insulation must be essentially inorganic such as mica and asbestos or glass fibers. Special attention must be given or changes in design made to see that the mica splittings in the entire coil structure are mechanically held in place and prevented from slipping. The common flat iron or heating element employing organic-bonded micanite heater plate is a typical and comparable example showing that if the mica is mechanically held in place the operating temperatures can be materially increased above the present AIEE temperature limits.

The mica splittings used in class *B* insulation have two outstanding physical advantages over other flexible inorganic materials such as asbestos and glass fibers. First, due to the solid and nonporous structure, the surface leakage path is tremendously increased. Second, moisture or water is adsorbed by the laminations of the mica splittings and does

A Discussion of Proposed ASA Transformer Standards

By R. T. HENRY
FELLOW AIEE

THE proposed American Standards Association standards for transformers, regulators, and reactors which are about to be circulated for a trial period involve several departures from previous standards.

The temperature rise has been separated from the ambient and total temperatures. It is the temperature rise which determines the amount of material required and therefore the rating; whereas the ambient and total temperatures are involved only in the operation and life of the insulation and have little or nothing to do with the rating.

Insulation levels have been established in zones for dielectric tests instead of basing the tests on rated voltage. These insulation levels are based on the level of protection available in the various voltage classes.

The proposed standards include impulse tests in addition to the low-frequency dielectric tests. These impulse tests are specified in kilovolts instead of gap spacings.

The insulation strength of transformers, regulators (except induction type), constant-current transformers, and instrument transformers have been co-ordinated, thus providing the same insulation strength for various types of apparatus of corresponding voltage ratings. The insulation levels for both power and distribution transformers have been made the same for 23 kv and above.

The test code has been enlarged and brought up to date and now includes complete instructions for making the various tests on transformers, etc.

The proposed guides for operation of transformers recognize the fact that the life of insulation depends not only on the temperature but on the duration of such temperature as well and that while transformers can be operated with a copper temperature of 95 degrees centigrade (55 degrees centigrade rise at 40 degrees centigrade ambient) for limited periods without serious damage, continuous operation at such temperatures would reduce the life of the insulation to a few years at best. The recommendations for loading are therefore based on a *maximum* ambient temperature of 40 degrees centigrade but a *daily average* ambient temperature of 30 degrees centigrade.

The guides also recognize that the permissible continuous loading is greater than the rating at low ambient temperatures and less than the rating at high ambient temperatures. In addition, the guides recognize that transformers can carry appreciable overloads for limited periods and the recommendations provide for short-time overloads for recurrent conditions and higher short-time overloads for emergency conditions.

A paper presented under the auspices of the AIEE committee on electrical machinery at the AIEE winter convention, New York, N. Y., January 23–27, 1939.

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not materially reduce the dielectric strength because of the nonabsorption characteristics of the individual laminations. The interstices of the asbestos and glass fibers must be filled with a varnish or compound to give additional dielectric strength above that of the air. Until suitable inorganic varnishes are developed, the most logical type of class *B* insulation having the highest over-all electrical and temperature limitations would be one composed of mica splittings and asbestos or glass fibers.

Notwithstanding the apparent improvements in varnishes, binders, and new carriers or base materials, we must recognize that either the electrical or physical limitations of class *B* insulation are still dependent upon *organic* substances and until they are radically changed or supplanted by inorganic binders and varnishes having suitable electrical and physical properties, plus stability at higher temperatures, we must not overlook this factor in proposing changes in general standards for operating-temperature limits of electrical apparatus.

In view of the variables which are so difficult to measure and correlate, any standards of temperature limits must, of necessity, be a compromise between the conservative viewpoint of low operating temperatures, reliability, long life, high efficiency, and the liberal viewpoint of lower first costs by reduction of size, higher operating temperatures, shorter life, reduced efficiencies, higher maintenance cost, and possible shut-downs for major repairs. Several years of operating experience has proved the AIEE standards for class *B* insulation, that is, a maximum hot-spot temperature of 125 degrees centigrade to be a fair and practical compromise.

The available information in regard to time-temperature characteristics of electrical insulation of all types as applied to electrical apparatus, is very meager. It would therefore seem very desirable for both the manufacturers and the operating companies to undertake a definite program for the investigation of this important subject. Any information thus found would be to the mutual knowledge and advancement of our engineering society.

Asbestos and Glass-Fiber Magnet-Wire Insulation

By K. N. MATHES
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MAGNET WIRE may be divided into class *O*, class *A*, class *B*, and class *C*, on the basis of the AIEE temperature ratings.[1] Many types of class *O* or class *A* magnet-wire insulations are in common use. The variety of class *B* magnet wire insulations is, for obvious reasons, much more limited. Different types are listed in table I.

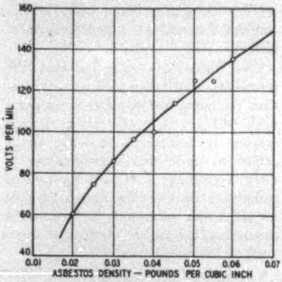

Figure 1. Volts per mil versus density of dry asbestos

Thickness 25 mils

Felted asbestos fiber has been prominent as a class *B* fibrous magnet-wire insulation. The recently developed commercial production of glass-fiber-insulated magnet wire has created considerable interest. This paper is limited to a consideration of the properties and application of these two types of class *B* fibrous constructions.

Class *B* magnet wire is used principally where high temperatures are encountered. The temperature stability of insulation, however, cannot be considered alone as it is qualified by many other factors when considering a particular application. In some cases flexibility may be of first importance and an extremely heat-resistant impregnating material, which may become brittle and crack, cannot be used. Heat resistance may have to be sacrificed in order to obtain the necessary degree of flexibility. Similarly insulation may be subjected to higher temperatures when not exposed to conditions of moisture. It is important, therefore, to study many other properties of a class *B* magnet wire even though the temperature stability may be of primary importance.

Conclusion

The temperature limitations of class *B* magnet wire must be studied in terms of many related factors. The stability of the associated varnish is one of the most important considerations. Where good moisture resistance must be retained, operating temperatures should be restricted to approximately 125 degrees centigrade in order to preserve the varnish film.

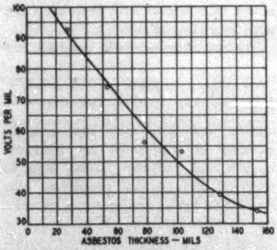

Figure 2. Volts per mil versus thickness of dry asbestos

Density 0.035 pound per cubic inch

Experience has indicated that for many types of service conditions and with the proper material reasonable insulation life can be expected for a range of temperatures from about 125 to 175 degrees centigrade. Of course insulation life decreases rapidly at the higher temperatures. In special applications, which are completely free from vibration and where dielectric requirements are not severe, operation at temperatures above 175 degrees centigrade may be permitted. The designer of electrical apparatus must correlate these factors to suit his particular problem.

The advantages of asbestos-insulated magnet wire in class *B* operating service are well established.

Glass-fiber magnet-wire insulation is finding an important field of use in electrical apparatus. The superior electrical properties and possibility of decreased space factor are important in many applications, and further improvements can be expected as development proceeds. Glass-fiber insulation is still too new to permit the presentation of extensive information concerning its life under service conditions.

Properties of Unvarnished Asbestos and Glass Fiber

In the analysis of any electric insulation wherein several components comprise the whole, it is essential to give careful consideration to the selection of these components and evaluate their individual characteristics.

The term "asbestos" is a commercial term applied to any mineral which can be readily separated into more or less flexible fibers. Asbestos is a heat-resisting nonmetallic mineral silicate including two groups—the serpentine or chrysotile group (hydrous silicates of magnesium) and the amphibole group (metasilicates of magnesium, calcium, iron, sodium, and aluminum, with a little water of hydration).

Table I

Continuous (enamel)	Inorganic oxide bound with organic binder[2]	Inorganic material coated with organic material[3]
Ribbon	Asbestos paper	Paper or cloth baked mica tape
Fibrous	Varnished asbestos fiber	Varnished glass fiber

Table II

Property	Asbestos	Glass Fiber
Fusing point (degrees centigrade)	1,550[4]	About 700
Yield point (loss of resilience) (degrees centigrade)	No significance	550 to 600
Dehydration temperature* (degrees centigrade)	Over 385	None
Stress - relieving temperature (degrees centigrade)	No significance	Above approximately 150
Acid resistance	Fair	Good (attacked by hydrofluoric)
Resistance to corona	Decomposes slowly producing magnesium nitrate	Unaffected

*Loss of water of hydration (chemically combined water).

A paper presented under the auspices of the AIEE committee on electrical machinery at the AIEE winter convention, New York, N. Y., January 23–27, 1939.

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[1] For all numbered references, see list at end of paper.

Chrysotile asbestos is the variety commonly used in the electrical industry. Due to the fineness of fibers, and their strength and flexibility, it can be carded for felting or spun into roving (a soft ropelike material) for application to wire in much the same manner as other textile materials.

The flexibility of asbestos is closely related to the combined water content, which, in the chrysotile variety, averages 14 per cent and accounts for its superiority to all other types. Flexibility is maintained consistently at all temperatures below 385 degrees centigrade.[4] At higher temperatures the water of hydration is gradually driven off and the fibers can be easily pulverized. From a consideration of heat resistance alone, it can be said therefore, that asbestos possesses ample margin for all magnet-wire applications in motor and generator armature and field coils.

Emphasis must be placed on two factors; magnetic iron and surface impurities (conducting salts present on the surface of all asbestos fibers). Magnetic iron cannot be separated from the fiber commercially and extreme care must be used in the selection of a source having a low content (0.50 per cent maximum of magnetic iron for nonferrous grades) which is so generally dispersed that the possibility of large particles, causing grounds between turns in apparatus coils, is remote. The same care must be exercised in selection to obtain material free from surface conducting salts since it has definitely been proved that the dielectric strength is not so much dependent upon the basic asbestos as on the amount of surface impurities present which have a great affinity for moisture.

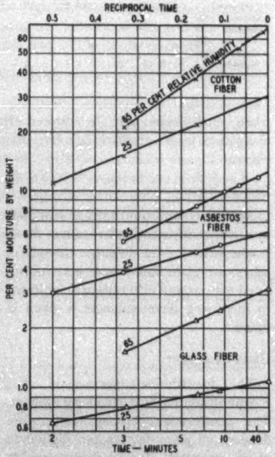


Figure 3. Increase in per cent moisture versus minutes at 25 per cent and 65 per cent relative humidity, at 25 degrees centigrade

Samples conditioned 16 hours at 150 degrees centigrade

The dielectric properties of untreated dry asbestos fiber plotted as a function of density and thickness are illustrated in figures 1 and 2.

Asbestos fiber for electrical insulation is graded as number 1 or number 2 depending upon the average length as determined by screen test. The asbestos fiber is mixed with cotton to facilitate textile operations, 10–15 per cent being the usual average content for magnet-wire materials. The fiber is processed into laps for direct application or spun into roving (either with or without a cotton core) and wound on suitable packages for spiral wrapping on the conductor.

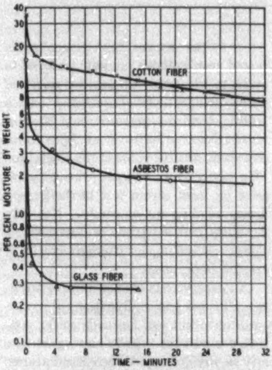


Figure 4. Decrease in per cent moisture versus minutes at 50 per cent relative humidity and 25 degrees centigrade

Samples conditioned at 100 per cent relative humidity and 40 degrees centigrade

Glass fiber is a synthetic material of definite composition and quality. The commercial production of glass fiber has been described previously.[5] Textile fiber glass may be classified as staple fiber (6 to 8 inches long) and continuous filament. The fibers are 0.00025 inch in diameter or smaller. Because of the enormous surface area of the fine glass fibers, severe weathering and low electrical surface resistivity may be experienced unless the fibers are drawn from excellent electrical glass. An alkali-free glass has been developed for electrical purposes which has high surface electrical resistivity and good resistance to ordinary moisture conditions. The alkali-free glass may disintegrate under some steam conditions and is not as stable as asbestos in this respect.

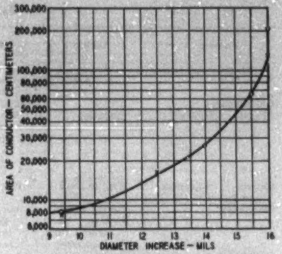


Figure 5. Maximum insulation increase for round double-cotton- or double-glass-insulated magnet wire

⊙—National Electrical Manufacturers' Association values for cotton

The surface of the glass fiber is particularly sensitive to cutting or nicking by adjacent fibers and must be protected by a sizing material possessing proper lubricating and binding properties. The sizing material used at present contains hygroscopic materials which impair the electrical and physical properties. In addition, the sizing is somewhat corrosive to copper and is not truly compatible with most insulating varnishes.

Although the glass fiber does not permit volume absorption of moisture, it cannot strictly be called nonhygroscopic since electrical properties may be damaged by moisture picked up in the following ways:

1. Adsorption on the glass-fiber surface.
2. Absorption in sizing material.
3. Capillary action between fibers or threads.
4. Capillary action between fiber and surrounding varnish.

The per cent regain and loss of weight for unvarnished cotton, asbestos, and glass fiber as a function of time at various humidities are given in figures 3 and 4. The potential advantages of glass fiber can be emphasized by comparison with the asbestos fiber which is truly a bundle of extremely fine fibers with surfaces contaminated by conducting salts.

For use as wire insulation, both staple and continuous glass fiber are used. Staple-fiber yarn is tightly spun, bonds together well because of its relatively rough finish, and can withstand greater

Table III

Property	Cotton	Asbestos	Glass
Space factor	See figure 5	Same as double cotton	Same as cotton (One half that of cotton with special glass)
Uniformity	Good	Fair	Good
Abrasion resistance	Superior	Adequate for coil applications	Inferior to asbestos for sharp-edged surfaces but may be superior to asbestos for smooth rubbing surfaces (adequate for coil applications when proper consideration is given to the tension device)
Impact resistance (see figure 6)	Fair to good	Fair	Very good
Flexibility	Good. Double wrap superior to single wrap	Fair, for round wire. Poor, for large rectangular sizes	Good. Double wrap superior to single wrap. Slightly inferior to cotton
Stiffness due to insulation (Important in small wire sizes)	Less than single glass	Less than single glass	Single but not double wrap exhibits considerable stiffness
Bonding (depends somewhat on varnish treatment)	Good	Good	Fair to good. Staple-fiber insulation bonds better than continuous filament

elongation than continuous-filament yarn. In contrast, continuous-filament yarn can be softly spun, yet is stronger, more resistant to abrasion, and smoother in appearance than staple-fiber yarn. Continuous-filament yarn is available in much finer sizes than staple-fiber yarn, which is used where heavy insulation thicknesses are desired and varnish saturation is difficult to secure.

A number of other basic properties listed in table II are of interest in the comparison of asbestos and glass fiber.

Properties of the Insulated Wire

In brief, two principal factors are of interest in connection with magnet wire:

1. The ease of handling and the ability to withstand the factory winding operation and electrical tests.
2. The design characteristics (temperature rise, space factor, etc.) which will allow a reasonable insulation life under service conditions.

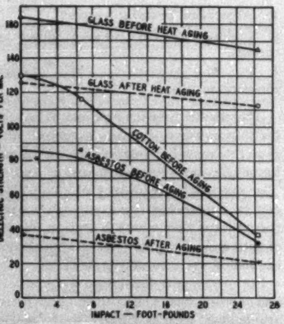


Figure 6. Dielectric strength versus increasing impact

Up to the present time generally accepted test methods are not available to evaluate completely the relative merits of fibrous magnet-wire insulations. As a result, practical experience must be combined with improved test methods to indicate the relative value of the insulation.

In many cases magnet wire may receive its most severe test in the factory during winding and assembly. Not only flexibility and resistance to abrasion but elongation and impact strength may be of extreme importance.

Laboratory tests cannot hope to duplicate all types and combinations of service conditions, but can be made under controlled conditions. Tests on magnet wire may be divided into three classifications:

1. Tests on the wire itself.
2. Tests on built-up coils to represent conditions present in various types of apparatus.
3. Tests on apparatus.

Each method of test has certain advantages and serves as a guide post in the over-all analysis. Most laboratory tests are accelerated—one or more conditions are made extreme in order to shorten the time of test. Accelerated test results must be carefully interpreted and only relative results can be expected. Long time tests (extended over a period of months with a variety of conditions, such as temperature, moisture, and voltage cycles) have much more significance in terms of actual operating service. Even long time test results serve only to indicate relative values of different types of insulation and cannot be extrapolated to indicate the absolute life under operating conditions, particularly since a combination of conditions not present in the test may be the deciding factor.

Once in place, insulation must meet a combination of various types of service conditions. The most practical criteria of a particular wire insulation comes from an analysis of actual operating results. The problem, however, sometimes is complicated by the difficulty of determining, after a failure in the field, which portion of the system failed first and the reason.

Various physical properties of cotton-covered and varnish-treated asbestos- and glass-fiber-insulated wire are compared in table III.

Flexibility

When bent or stretched, a felted insulation such as asbestos exhibits an uneven thinning of the insulation in certain spots. In contrast, a wrapped fiber insulation such as glass fiber spreads more evenly—the parallel threads, when properly applied, tend to separate slightly and maintain a constant space factor. The superior flexibility of a wrapped-thread type of insulation becomes of practical importance when large wires must be bent on short radii without damage or when small wires may be subject to elongation from automatic winding machines.

Table IV

	Abrasion Resistance (Average Turns per Mil of Insulation Thickness to Cause Failure)			
	Asbestos		Glass Fiber	
	Large Wire	Small Wire	Large Wire	Small Wire
As received	35	44	19.2	80
Exposed one month to 170 to 180 degrees centigrade	0.48	25	1.1	5.9

Impact Resistance

Ability to withstand impact is important when wire must be forced into place. A comparison of the dielectric strength of asbestos-, glass-fiber-, and cotton-insulated wire wound into coils and subjected to increasing direct impacts is given in figure 6.

Heat Stability

The quantitative appraisal of class *B* wire insulation is particularly difficult. The permissible operating temperature depends on many factors and the length of life expected. It is necessary to discuss the relative effect of exposure to high temperatures on the physical and dielectric properties.

Deterioration of physical properties may be even more important than impaired electrical properties. It is precisely this factor which is also most difficult to measure. The effect of vibration and conditions caused by temperature cycles encountered in service cannot easily be duplicated in the laboratory. Abrasion and impact tests are made which may serve as an indication of resistance to such conditions in service. Table IV indicates the effect of temperature on the relative resistance of asbestos- and glass-fiber-insulated wire to the abrasion of smooth, tungsten-carbide rods in the form of a rotating cylindrical cage.

By referring to figure 6 again the effect of heat aging can be observed by comparing the position of the dotted to the solid curves. Glass is superior to asbestos in this respect.

Table V indicates the relative effect of exposure at 170 to 180 degrees centigrade on the dielectric strength of asbestos- and glass-fiber-insulated magnet wire. It is important to recognize that these tests were made on wire placed in a well-ventilated oven, and that aging occurs much more rapidly than when the wire is sealed deep in apparatus where oxygen is largely excluded. A month of exposure of 170 to 180 degrees centigrade was sufficient to damage the varnish film and thereafter the rate of deterioration was slower.

Both asbestos and glass must be considered as physical spacing mediums. Failures or loss of dielectric strength under high heat and moisture conditions are primarily failures of the treating materials used in the insulation system.

Moisture Resistance

Insulation resistance is one accepted criterion of the effect of moisture on insulation in coils. The average insulation resistance between parallel wires of wound coils is plotted as a function of time at 100 per cent relative humidity and 40 degrees centigrade in figure 7. These coils were wound to duplicate field coils and were completely varnish treated. The upper curve indicates the possibility of improvement in the moisture resistance of glass insulation. In this case the original sizing was removed from the glass fiber and replaced with an improved material before varnish treatment.

Application of Class B Magnet Wire

No sweeping conclusions should be drawn from this compilation of data. Although tests show glass-insulated wire as definitely superior to asbestos in electrical properties, the mechanical properties after exposure to high temperature may be inferior in some respects. Since glass-fiber insulation is so new, only preliminary service data have been collected; years of experience have proved the serviceability of asbestos insulation.

Keeping in mind the effect of temperature on various properties, it is possible to divide approximately the types of application for both asbestos- and glass-fiber-insulated magnet wire into three divisions based on operating temperatures as listed below.

1. Temperatures up to 125 degrees centigrade—For reasonable life the film of the binding varnish is not seriously impaired and relatively good moisture and physical and dielectric properties are retained.
2. Temperatures from 125 to 175 degrees centigrade—Film structure of varnish deteriorates but sufficient varnish may remain to act as a binder. Moisture resistance, electrical and physical properties are somewhat impaired.
3. Temperatures above 175 degrees centigrade—The binding varnish is destroyed. Electrical and particularly physical properties are considerably impaired.

In most cases operating temperatures above 175 degrees centigrade are impossible because vibration and wear destroy the insulation mechanically.

It is perhaps too early to compare extensively the field of application for asbestos- and glass-fiber-insulated magnet wire. For the larger size of wires, the increased cost of glass-fiber insulation must be justified, either by increased quality or by saving in over-all cost.

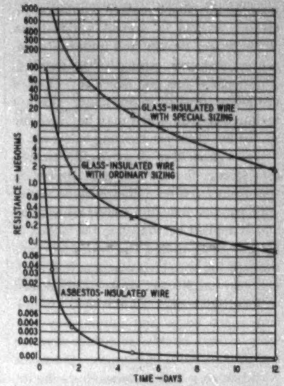


Figure 7. Electrical resistance versus days at 100 per cent relative humidity and 40 degrees centigrade

Table V

	Dielectric Strength (Volts Per Mil)		
	Asbestos Average*	Single Glass Average*	Double Glass Average**
Wire as received, tested at 25 degrees centigrade, 50 per cent relative humidity	87.5	191	170
Wire as received, and exposed 16 hours at 40 degrees centigrade, 100 per cent relative humidity	29.8	105	85
Wire exposed one month at 170 to 180 degrees centigrade, tested at 25 degrees centigrade, 50 per cent relative humidity	53.4	146	114
Wire exposed one month at 170 to 180 degrees centigrade, exposed 16 hours at 40 degrees centigrade, 100 per cent relative humidity	11.3	41	32

*Average of 20 results on different sizes of wire from 0.0201 to 0.114 inch.
**Average of five results on 0.72-inch wire.

The following two factors should be studied:

1. Possibility of decreased space factor, resulting in smaller motors or increased copper area with the same slot space and resulting greater output or efficiency.
2. Possibilities for saving in labor.

Method of Manufacture

A commonly used method of asbestos magnet-wire manufacture consists of wrapping asbestos roving spirally around the conductor, applying compound, and removing the excess with suitable polishing dies. With this method, uniformity of the insulation is limited by the uniformity of the roving which, in the smaller sizes, is difficult to control. Low production speeds result because of lack of strength when the finer sizes of roving are used, and this method is generally limited to large wires with insulation increases greater than for double cotton.

A method whereby the asbestos is felted on the wire was introduced in recent years which permits the application of carded asbestos directly to the wire, giving a homogeneous, intertangled mass of asbestos at all points on the conductor. Improved space factor, greater density of insulations, more uniformity, and reduced cost are achieved. Wire sizes from number 30 to 4/0 can be readily insulated maintaining double cotton insulation increases or less in special applications. With greater density and uniformity improved dielectric strength has resulted. This method has one deficiency—loss of insulation flexibility on larger sizes of rectangular wire which may be overcome by the use of proper treating materials.

Since glass fiber is so readily twisted into strong, fine thread, it is applied in this form to wire. The yarn is wound with parallel ends in a universal winding and is applied by wrapping the ribbon of parallel ends around the wire as it runs through the wrapping machine in a manner very nearly the same as that used with cotton or silk. Care is used to eliminate sharp edges and unnecessary rubbing surfaces in order to reduce abrasion and friction to a minimum. Since the yarn has very little extensibility, the insulating process must be very precise to avoid yarn breakage.

Staple glass fiber has been applied to wire directly as it comes from the fiber-making machine in the form of a wrapped sliver of parallel filaments. Wire insulated in this way does not have the density nor uniformity of a wrapped-thread type of insulation.

Fibrous-insulated class *B* magnet wire must be compound or varnish treated in order to attain sufficient abrasion resistance to be suitable for winding operations. An adequate treatment should give the wire the following characteristics:

1. Good abrasion resistance—obtained by adequate bonding, penetration, and filling of the interstices between fibers.
2. Hard and smooth surface film.
3. Good flexibility.

These desirable qualities are functions of the following factors:

1. Type and nature of treating varnish or compound.
2. Method used to secure penetration.
3. Temperature and degree of baking.

Because so many factors are concerned, no one exact method can be described at this time for varnish or compound treatment of fiber-insulated wire.

References

1. AIEE Standard No. 1, April 1925.

2. United States Patent 2,022,827 to Vega Manufacturing Company.

3. British Patent 380,426, 1932, A. G. Hovey, British Thomson-Houston Company.

4. Textile Research (a book), J. M. Weaver. Chapter 15, Asbestos Textile Industry.

5. Fiber Glass—Mechanical Development, J. H. Plummer. *Industrial and Engineering Chemistry*, volume 30, July 1938, page 726.

Lightning Performance of 110- to 165-Kv Transmission Lines

A GREAT MANY papers have been presented in recent years on the subject of lightning as it affects the electrical industry and particularly transmission lines. Quite a few of these papers have dealt with the lightning performance of high-voltage lines in service covering individual systems in various sections of the country.

A committee paper was presented in 1935 on "Lightning Performance of 220-Kv Lines"[1] which summarized and discussed the records of some 19 lines in the 220-kv class comprising practically all lines of that voltage operating in the United States and Canada. This type of record with its broad inclusive coverage of lightning performance of lines was received with so much favor that it was recommended by the AIEE power transmission and distribution committee that the lightning and insulator subcommittee collect and present similar records giving the lightning performance of high-voltage lines in the general voltage classification of 132 kv. The data and discussion presented in this paper have resulted from the above situation.

Scope and Plan

In collecting data on these higher-voltage lines, it was decided to include the transmission voltage range from 110 kv up to 165 kv, inclusive, for lines operating in the United States and Canada.

The data were obtained by sending a questionnaire to companies who either operated or had under their control lines in the above classification. The questionnaire included 71 questions which were designed to give principally what was considered important information on line construction and line operation and to bring out outstanding features which might throw some light on the lightning performance of these lines over the past ten years. Some information not directly connected with the lightning performance of the line was requested and has been included in the data presented herewith.

Each company was asked its opinion as to whether the ten-year lightning performance of each of its lines was satisfactory, and if not, what types of improvement were being considered. The detailed questionnaire with accompanying prints of typical towers and counterpoise designs is too lengthy to reproduce here, but the pertinent data received has been tabulated, analyzed, and discussed.

Extent of Lines Reported

The questionnaire was sent out to 35 companies in the United States and Canada. Operating records and data were received from 24 companies. Four companies reported that they had no lines in the voltage range indicated and seven companies failed to send in an answer. It will therefore be seen that of the 35 companies contacted, the response to the committee attempt to get information in this way has been most gratifying, replies having been received from 70 per cent of the companies.

It is interesting to note that of the 24 companies sending in data, records have been received on lines operating in 20 states in the United States and also from Canada. The lightning severity as shown by the isokeraunic level where these lines operate ranges from 22 storms to 68 storms per year, that is a ratio of slightly over three to one.

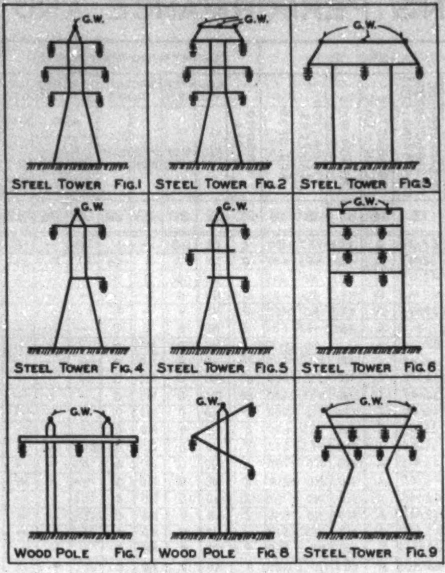
Table I gives in some detail the character of the transmission lines on which data were obtained. The table shows that the records cover 7,140 miles of line (right of way) of which 3,890 are of two-circuit, steel-tower construction; 1,725 of single-circuit steel construction; and 1,526 of single-circuit, wood construction. Fifty-four per cent of the mileage is of 132-kv construction; 31 per cent of 110-kv rating; and the balance distributed among the other voltage classes.

Paper number 39-60, prepared by the lightning and insulator subcommittee of the AIEE committee on power transmission and distribution, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 30, 1938.

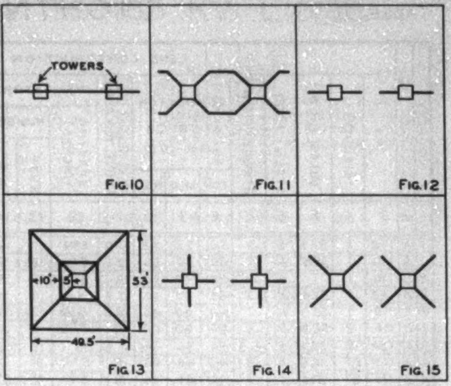
Personnel of AIEE lightning and insulator subcommittee: Philip Sporn, *chairman;* H. A. Frey, I. W. Gross, D. C. Jackson, Jr., W. W. Lewis, J. T. Lusignan, F. W. Packer, and C. F. Wagner.

The committee wishes to acknowledge the wholehearted co-operation of the contributing companies who have generously contributed data to make this report possible and their prompt response in answering the questionnaire on which the data presented in this report were based. It also acknowledges the assistance of G. D. Lippert of the American Gas and Electric Service Corporation in preparing the data received into suitable form for analysis as given herein.

[1] AIEE Lightning Reference Book, page 1272.



Figures 1–9. Typical transmission structures



Figures 10–15. Typical counterpoises

All the lines operate at 60 cycles unless otherwise noted in footnotes 1 and 2 of the table.

Presentation and Discussion of Data

It has not been possible to include all the data received as a result of the questionnaire, but the major part of it, particularly that which covers the lightning characteristics of the line and its lightning performance, has been correlated and tabulated in table II. The names of the companies and lines are given by symbol only as a result of a specific request by a few companies that company name and line be withheld from publication.

While replies to the questionnaire were in most cases complete and carefully prepared, in a few cases some questions were apparently misunderstood with the result that the data submitted presented some difficulties in interpretation. However, such instances are few and it is believed that any errors of interpretation placed on the data sent in are of minor importance.

Table I. Miles of Three-Phase Line (Right of Way)

Wood-Pole and Steel-Tower Construction

Line Kilovolts	Single Circuit Wood	Single Circuit Steel	Double Circuit Wood	Double Circuit Steel	Total
165				135.2	135.2
154	89.5			28.7	118.2
140		283.3		52.7	336.0
132	593.3	1,076.7		2,181.6*	3,851.6
120	37.6			274.1	311.7
115		37.3		110.6	147.9
110	806.0	326.9		1,107.4†	2,240.3
Totals	1,526.4	1,724.2		3,890.3	7,140.9

Total wood........1,526.4
Total steel.........5,614.5

Total...........7,140.9

* Includes 31.5 miles of four-circuit single-phase 25-cycle line.

† Includes 171.9 miles of 25-cycle line.

TOWER AND COUNTERPOISE CONFIGURATION

In figures 1 to 9 are given the typical tower structures in use as tabulated in column 7. Likewise, figures 10 to 15 give types of counterpoise configuration, where used, as listed in column 21 of table II.

GROUND WIRES

The effect of ground wires in reducing lightning outages is shown both in table III and figure 16. From table III it appears where no ground wire is used, single-circuit steel lines show an average of 29.8 outages per hundred miles of line per year; with one ground wire, the outages figure becomes 9.8; and with two ground wires 7.0. The same general type of reduction is shown for wood-pole construction although the figure of 0.44 outages for the single-circuit, wood line with one ground wire is probably questionable for comparison purposes as it includes the record of only one line.

The benefit in going from one ground wire to two is clearly indicated in table III where the outages on two-circuit steel lines drop from 7.1/4.2 to 5.2/3.5 as the ground wires are increased from one to two. The figures separated by the slant indicate outages per hundred miles of line per year, the first figure before the (/) being for single-circuit outages and the second figure for two-circuit outages. These figures for two-circuit steel tower lines give a ratio of 1.36 for single-circuit outages and 1.20 for double-circuit outages as between the one-ground-wire and the two-ground-wire arrangement. For single-circuit steel-tower lines the ratio

TABLE II

LIGHTNING PERFORMANCE AND

COMPANY	LINE	LINE CONSTRUCTION																	LIGHTNING PROTECTION							
		K.V.	CIRCUITS	MILES (R.W.)	STEEL - WOOD	STRUCT. (FIG. N°)	CONDUCTOR HEIGHT AT TOWER MIN.-(FT.)			GR.W. HEIGHT (FT.) ABOVE TOP COND.	INSULATORS			CLEARANCE-LINE TO STRUCTURE (IN.) COND. SWING (DEG.)	CONDUCTOR ALUM. - COP.	ARMOR RODS	VIBRATION DAMPERS	AVG. SPAN (FT.)	GROUND WIRES	COUNTER POISES		GROUND RODS		GR. RES. OF STRUCTURES		
							TOP	MID.	BOT.		SPACING (INCHES)	NUMBER								FIG. N°	% OF LINE	PER TOW.	% OF LINE	AVG.		MAX
												SUSP.	D.E.											BEFORE CP.-GR.	AFTER CP.-GR.	AFTER CP.-GR.
1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20	21	22	23	24	25	26	27
1	A	132	1	82.5	S	3	52	52	52	11.8	5¾	10	12	62/50	A	YES	YES	1000	2	13	68	—	1	7.9	—	18
2	A	110	1	38.8[2]	W	7	30	30	30	5.1	5½	6	7	36/45	A	NO	YES	632	0	NO	0	1	100	91	81.7	260
2	AA	110	1		S	—	30	30	30	8	5½	7	8	42/45	A	YES	NO	645	1	NO	0	0	0	—	—	—
2	B	120	1	37.6	W	7	30	30	30	5.7	5	8	9	42/45	A	YES	NO	491	0	NO	0	—	—	—	—	—
2	C	110	1	55.3	W	7	60	50	40	5.7	5⅛	7	8	38/45	A	YES	NO	460	0	NO	0	—	—	—	—	—
2	D	110	2	52.9	S	2	60	60	60	9	5⅛	8	9	—	C	NO	NO	881	2	NO	0	0	0	—	—	—
3	A	154	2	28.7	S	1-3	36	36	36	12.6	5	10	12	55/-	A	YES[3]	NO	977	1	NO	0	NO	0	—	—	—
4	A	115	2[4]	16.5	S	3	46	46	46	8	5¾	9[5]	9	36/45	C	NO	YES	600	2[6]	NO	0	NO	0	—	—	—
4	B	115	2[4]	30.6	S	3	46	46	46	8	5¾	9[5]	9[5]	36/45	C	NO	YES	600	2[6]	NO	0	NO	0	—	—	—
4	C	115	2[4]	13.2	S	3	46	46	46	8	5¾	9[5]	9[5]	36/45	C	NO	YES	600	2[6]	NO	0	NO	0	—	—	—
4	D	115	2[4]	20.7	S	3	46	46	46	8	5¾	9[5]	9[5]	36/45	C	YES	YES	600	2[6]	NO	0	NO	0	—	—	—
4	E	115	1	19.5	S	3	46	46	46	8	5¾	9[5]	9[5]	36/45	C	NO	YES	600	2	NO	0	NO	0	—	—	—
4	F	115	1	17.8	S	3	46	46	46	8	5¾	9[5]	9[5]	36/45	C	NO	YES	600	2	NO	0	NO	0	460	—	—
8	A	132	2	11.9	S	2	100	87	74	13	5½	9	11	42/45	A	NO	NO	790	2	NO	0	NO	0	2.9	—	—
8	B	132	2	10.9	S	2	100	87	74	13	5¾	9	11	42/45	A	NO	NO	814	2	NO	0	NO	0	9.4	—	—
8	C	132	2[4]	4.5	S	2	100	87	74	13	5¾	9	11	42/45	A	NO	NO	795	2	NO	0	NO	0	10.6	—	—
8	D	132	2	23.4	S	2	100	87	74	13	5½	9	11	42/45	A	NO	NO	840	2	NO	0	NO	0	12.4	—	—
8	E	132	2	28.8	S	2	97	84	71	13	5½	9	11	42/45	A	NO	YES	512	2	NO	0	NO	0	25.8	—	—
9	A	110[P]	2	23.6	S	2	70	60	50	8.5	5⅜	6	8	60/30	C	NO	NO	560	2	NO	0	NO	0	—	—	—
13	A	132	1	79.9	S	1	69	57	45	10	5	10	11	48/45	A	YES	YES[7]	880	1	NO	0	NO	0	—	—	—
13	B	132	1	54.0	S	1	76	64	52	10.7	5	10	11	48/45	A	YES	YES[7]	970	1	NO	0	NO	0	—	—	—
13	C	132	1	26.4	S	5	69	57	45	10	5	10	11	48/45	A	YES	YES[7]	880	1	NO	0	NO	0	—	—	—
13	D	132	1	8.2	S	1	76	64	52	10.3	5	10	11	48/45	A	YES	YES[7]	970	1	NO	0	NO	0	—	—	—
13	DD	132	1	23.9	S	1	75	63	51	10.3	5	10	11	51/45	C	NO	NO	770	1	NO	0	2	100	—	—	—
14	A	132	1	14.8	S	1	76	64	52	9.3	6 7/16	9	11	45/45	C	NO	NO	701	1	NO	0	NO	0	—	—	—
15	A	132	1[b]	30.9	S	2	74	61	48	9	6½	8	8	60/30	C	NO	NO	500	(a)	NO	0	2	100	—	3	5
15	B	132	1[b]	27.9	S	2	74	61	48	9	6½	8	8	60/30	C	NO	NO	500	(a)	NO	0	2	100	—	3	5
15	C	132	1	30.9	S	2	81	68	55	10.4	6½	8	8	60/30	C	NO	NO	500	1	NO	0	2	43	—	3	5
15	D	132	1[c]	42.2	S	2	84	71	58	9	6½	8	8	60/30	C	NO	NO	880	(a)	11	3	2	45	—	10	50
15	E	132	1[c]	61.7	S	2	84	71	58	9	6½	8	8	60/30	C	NO	NO	880	(a)	11	3	2	43	—	10	50
15	F	132	1	24.4	S	2	84	71	58	10.4	6½	8	8	60/30	C	NO	NO	880	1	NO	0	NO	0	10	—	—
15	G	132	1	29.7	S	2	84	71	58	9	6½	8	8	60/30	C	NO	NO	880	1	NO	0	2	85	—	10	67
15	H	132	2	36.9	S	2	84	71	58	9	6½	8	8	60/30	C	NO	NO	880	2	NO	0	2	51	—	8	25
15	I	132	1	10.6	S	2	84	71	58	9	6½	8	8	60/30	C	NO	NO	880	2	NO	0	2	56	—	10	30
16	A	110	1	79.4	W	7	43	43	43	—	5½	8	9	31/45	C	NO	NO	509	0	NO	0	NO	0	—	—	—
16	B	110	1	83.7	W	7	49	49	49	10.5	5¼	8	9	—	A	YES	NO	592	2	NO	0	2	100	—	—	—
17	A	132	1	50.3	S	1	70	58	46	8.5	4¾	10	12	47/50	A	NO	NO	588	1	NO	0	NO	0	—	—	—
17	B	132	1	27.9	S	1	70	58	46	8.5	4¾	10	12	37/50	C	NO	NO	608	1	NO	0	NO	0	—	—	—
17	C	132	1	22.4	S	1	70	58	46	8.5	4¾	10	12	47/50	C	NO	NO	612	1	NO	0	NO	0	—	—	—
17	D	132	1	29.4	S	1-2-6	70	58	46	8.5	4¾	10	12	47/50	A-C	NO	NO	588	1-2	NO	0	4	?	—	1.96	4.8
17	E	132	1	22.8	S	1-2	72	59	47	9.5	5¼	9/11	12/14	37/50	C	NO	NO	602	1-2	NO	0	4	?	—	3.5	14.1
17	F	132	1	7.9	S	1	72	59	47	9.5	4¾	10	12	37/50	C	NO	NO	591	1	NO	0	NO	0	—	—	—
17	G	132	1	52.3	S	1-2-6	70	58	46	8.5	4¾	10/12	12/14	37/50	A-C	NO	NO	590	1-2	NO	0	4	?	—	2.2	14.1
17	H	132	1	76.1	S	1	70	58	46	8.5	4¾	10	12	37/50	A	NO	NO	608	1	NO	0	NO	0	—	—	—
18	A	132	1	5.8	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	644	1	NO	0	2-3	(d)	—	—	—
18	B	132	2	18.0	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	680	1	NO	0	2-3	(d)	—	—	—
18	C	132	2	18.0	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	680	1	NO	0	2-3	(d)	—	—	—
18	D	132	2	8.9	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	715	1	NO	0	2-3	(d)	—	—	—
18	E	132	1	8.9	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	715	1	NO	0	2-3	(d)	—	—	—
18	F	132	2	9.9	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	710	1	NO	0	2-3	(d)	—	—	—
18	G	132	2	9.9	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	710	1	NO	0	2-3	(d)	—	—	—
18	H	132	2	6.4	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	650	1	NO	0	2-3	(d)	—	—	—
18	I	132	1	6.1	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	675	1	NO	0	2-3	(d)	—	—	—
18	J	132	2	5.1	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	715	1	NO	0	2-3	(d)	—	—	—
18	K	132	2	6.5	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	680	1	NO	0	2-3	(d)	—	—	—

FOR FOOT NOTES SEE END OF THIS TABLE.

CONSTRUCTION OF 110 KV. TO 165 KV. LINES

GRADING & ARC DEVICES[1]	FAULT CLEARING TIME (CYCLES)			LINE OPERATION														LIGHTNING TERRITORY[2]	ISOCERAUNIC LEVEL	OPERATING RECORD SATISFACTORY	CONSIDERING IMPROVEMENTS TO BETTER LINE PERFORMANCE	COMPANY	LINE
	RELAYS				OUTAGES DUE TO LIGHTNING ONE CIRCUIT / TWO CIRCUIT											CASES OF							
	PRIMARY (MIN.)	BACK UP (MIN.)	OCB (MIN)	YEARS IN SERVICE	1928	1929	1930	1931	1932	1933	1934	1935	1936	1937	AVG. OUTAGES/ 100 MILE OF LINE/YEAR	BURNED DOWN CONDUCTORS	PROLONGED OUTAGES						
28	29	30	31	32	33	34	35	36	37	38	39	40	41	42	43	44	45	46	47	48	49	1	2
RR	–	INST.	15	7	–	–	0	0	0	3	2	1	3	1	1.5	1	4	S	50	NO	YES	1	A
T	3	–	8	4	–	–	–	–	–	–	17	5	8	12	28.7	0	0	H	40	–	–	2	A
T	3	–	8	4												1	1	H	40	–	–	2	AA
P-T[5]	3	–	8	5	–	–	–	–	–	13	15	6	16	22	38.0	0	0	H	40	–	–	2	B
P	15	48	8	5	–	–	–	–	–	9	16	12	10	16	23.0	0	0	H	40	–	–	2	C
RR-P[6]	12	66	8	5	–	–	–	–	–	15	10	2	16	10	20.0[6]	0	0	H	40	–	–	2	D
HH[3]	4	–	8	10	0/9	0/5	0/14	0/6	0/6	0/8	0/10	0/11	0/15	0/8	0/35.5	0	1	S	62	NO	YES-T.	3	A
RR	1	25	8	12	3/0	3/2	4/5	0/1	3/0	10/3	3/0	4/0	6/0	5/1	25/7.3	0	0	M	22	YES	NO	4	A
RR	1	16	8	11	4/0	8/0	5/0	1/0	3/0	5/0	2/0	4/0	5/0	6/0	14/0	0	0	M	22	YES	NO	4	B
RR	1	35	8	9	–	–	0/0	2/0	0/0	0/0	0/0	0/0	1/0	1/0	3.8/0	0	0	M	22	YES	NO	4	C
RR	1	16	17	9	–	0/0	7/1	0/0	0/0	6/0	1/0	1/0	7/0	4/0	14/0.5	0	0	M	22	YES	NO	4	D
RR	3	20	17	7	–	–	–	1	4	1	1	2	4	0	9.5	0	0	M	22	YES	NO	4	E
RR	1	5	8	1	–	–	–	–	–	–	–	–	–	4	22.5	0	0	M	22	YES	NO	4	F
RH	6	120	6	10	0/0	0/0	0/0	0/0	1/0	0/0	0/0	0/0	0/0	1/0	1.8/0	0	0	M	28	YES	NO	8	A
RR	6	180	12	9	0/0	2/0	1/0	0/0	0/0	0/1	0/0	0/0	1/1	0/0	3.6/1.8	0	0	M	28	YES	NO	8	B
RR	6	168	12	9	0/0	2/0	0/0	0/0	0/0	0/0	0/0	0/0	0/0	0/1	4.4/2.2	0	0	M	28	YES	NO	8	C
RR	6	174	6	9	–	0/0	0/0	0/1	0/0	0/0	0/0	0/0	0/1	0/0	0/1	0	0	M	28	YES	NO	8	D
RR	12	150	12	9	–	0/1	1/1	1/0	0/0	1/1	3/2	3/1	3/1	2/2	5.4/3.5	0	0	M	28	NO	YES-C.P.	8	E
NO	18	120	18	26	0	0	1	2	0	4	3	2	4	1	7.2[6]	0	0	S	35	YES	YES-C.P. & GR. RDS.	9	A
LH	3	42	13	11	1	3	4	6	5	1	1	5	3	2	3.9	0	0	H	40	YES	YES-REDUCE GR. RES.	13	A
LH	3	15	13	9	0	3	0	1	2	2	5	3	2	3	3.9	0	0	H	40	YES	YES-REDUCE GR. RES.	13	B
LH	3	29	13	12	1	0	0	0	1	0	2	1	1	2	3.0	0	0	M	40	YES	YES-REDUCE GR. RES.	13	C
LH	3	29	8	11												0	0	M	40	YES	YES-REDUCE GR. RES.	13	D
LH	3	29	8	11	–	–	–	–	3	0	1	1	9	6	7.1	0	0	M	40	YES	NO	13	DD
NO	1	60	8	10												3	4	M	40	YES	NO	14	A
LR	5	NO	16	14	2	1	0	1	0	2	0	0	0	0	1.9	0	0	S	42	YES	NO	15	A
NO	5	NO	16	11	0	0	1	0	0	0	0	0	0	0	0.35	0	0	S	42	YES	NO	15	B
RR[6]	2	NO	16	7	–	–	0	0	1	0	0	0	0	0	0.41	0	0	S	42	YES	NO	15	C
NO	5	NO	16	11	–	–	–	–	–	0	2	1	1	2	2.75	0	0	S	42	YES	NO	15	D
NO	5	NO	11	9	–	0	1	6	0	1	0	1	2	0	2.0	0	0	S	42	YES	NO	15	E
RR[6]	5	NO	11	8	–	0	2	1	2	0	1	1	0	1	7.3	0	0	S	42	YES	NO	15	F
NO	1	NO	16	13	0	1	1	0	0	0	0	1	0	0	1.0	0	0	H	42	YES	NO	15	G
NO	1	NO	16	10	0/0	0/0	0/1	1/0	0/0	1/0	1/0	1/0	1/0	1/0	1.5/0.3	0	0	H	42	YES	NO	15	H
NO	1	NO	16	13	0	0	0	0	0	0	0	0	0	1	0.9	0	0	H	42	YES	NO	15	I
HH	12	NO	9	9	–	–	–	–	6	23	33	15	13	23	24	0	2	–	33	–	NO	16	A
–	7	42	12	7	–	–	–	–	–	–	–	8	4	2	5.5	0	0	–	33	–	NO	16	B
LH	60	NO	12	14	6	7	7	?	7	3	3	2	3	?	9.5	0	0	H	35	NO	NO	17	A
LH	60	NO	12	12	1	2	2	0	0	0	2	1	3	0	4.0	0	0	H	35	NO	NO	17	B
FC	60	NO	12	13	2	6	2	1	0	0	1	0	1	0	5.8	0	0	H	35	NO	NO	17	C
LH-P	81	NO	12	7	–	–	–	0	0	0	2	1	0	1	2.0	0	0	H	35	YES	NO	17	D
P-T	31	NO	8	2	–	–	–	–	–	–	–	–	1	0	2.2	0	0	H	35	YES	NO	17	E
FC	27	NO	12	12	1	2	0	0	0	0	0	0	0	0	3.8	0	0	H	35	YES	NO	17	F
LH-P	60	NO	12	2	–	–	–	–	–	–	–	–	1	0	1.0	0	0	H	35	YES	NO	17	G
LH	60	NO	12	12	–	–	–	–	–	–	–	–	3	5	5.3	0	0	H	35	NO	NO	17	H
NO	–	–	–	12	1	3	0	0	2	1	1	0	0	0	13.8	0	0	M	38	YES	NO	18	A
NO	–	–	–	12	0/0	3/0	0/0	2/0	0/1	0/0	0/0	0/0	0/0	0/0	2.8/0.6	0	0	M	38	YES	NO	18	B
NO	–	–	–	10	0	1/0	1/0	0/0	0/0	0/0	0/0	0/0	1/0	0/0	1.7/0	1	1	M	38	YES	NO	18	C
NO	–	–	–	11	0/0	0/0	0/0	0/0	0/0	0/1	0/0	0/0	1/0	0/0	1.1/1.1	0	0	M	38	YES	NO	18	D
NO	–	–	–	10	0	1	0	0	1	0	1	0	1	0	4.4	0	0	M	38	YES	NO	18	E
NO	–	–	–	1	–	–	–	–	–	–	–	–	–	0/0	0/0	0	0	M	38	YES	NO	18	F
NO	–	–	–	12	1/1	1/0	0/0	0/0	0/1	0/0	0/0	0/0	0/2	1/0	3.0/4.0	1	1	M	38	YES	NO	18	G
NO	–	–	–	12	0/0	0/0	0/0	0/0	0/0	0/0	0/0	1/0	2/0	1/0	6.3/0.	0	0	M	38	YES	NO	18	H
NO	–	–	–	14	1	1	0	0	3	0	0	0	0	0	8.2	0	0	M	38	YES	NO	18	I
NO	–	–	–	5	–	–	–	–	–	0/0	0/0	0/0	0/0	0/0	0/0	0	0	M	38	YES	NO	18	J
NO	–	–	–	5	–	–	–	–	–	0/0	0/0	0/0	0/0	0/0	0/0	0	0	M	38	YES	NO	18	K

TABLE II (CONTINUED)

LIGHTNING PERFORMANCE AND

COMPANY	LINE	LINE CONSTRUCTION																	LIGHTNING PROTECTION							
		K.V.	CIRCUITS	MILES (R.W.)	STEEL-WOOD	STRUCT. (FIG. Nº)	CONDUCTOR HEIGHT AT TOWER MIN.-(FT.)			GR.W. HEIGHT (FT.) ABOVE TOP COND.	INSULATORS			CLEARANCE-LINE TO STRUCTURE (IN.) COND. SWING (DEG.)	CONDUCTOR ALUM.-COP.	ARMOR RODS	VIBRATION DAMPERS	AVG. SPAN (FT.)	GROUND WIRES	COUNTERPOISES		GROUND RODS		GR. RES. OF STRUCTURES		
							TOP	MID.	BOT.		SPACING (INCHES)	NUMBER								FIG. Nº	% OF LINE	PER TOW.	% OF LINE	AVG.		MAX
												SUSP.	D.E.											BEFORE CP.-GR.	AFTER CP.-GR.	AFTER CP.-GR.
1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20	21	22	23	24	25	26	27
18	L	132	2	53.2	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	695	1	NO	0	2-3	(d)	–	–	–
18	M	132	2	8.1	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	700	1	NO	0	2-3	(d)	–	–	–
18	N	132	2	67.8	S	1	76	63	50	11	5	10	12	48/38	C	NO	NO	685	1	NO	0	2-3	(d)	–	–	–
20	A	140	1	148.0	S	1-5	52	46	40	–	4¾	10	12	48/20	C	NO	NO	520	0	NO	0	0	0	–	–	–
20	B	140	2	43.5	S	1	68	57	45	7.7	4¾^{v}	9	11-12	48/20	C	NO	NO	665	1	NO	0	0	0	–	–	–
20	C	140	1	44.3	S	1-5	52	46	40	6.7	4¾	10	12	48/20	C	NO	NO	564	1	NO	0	1.6^{e}	100	39	19.5	155
20	D	140	1/2	38.2^{f}	S	1-6	48	48	48	6.7	4¾^{v}	10	11-12	48/20	A-C	NO	YES-NO	710	2-1	NO	0	1.01^{e}	100	65-25	20-8.7	130-55
20	E	140	1	62.0	S	1	64	52	40	7	4¾	10	12	48/20	C	NO	NO	517	1	NO	0	0	0	–	–	–
21	A	110	2	50.4	S	2	60	51	43	7	5	8	10	–	C	NO	NO	630	1	NO	0	0	0	–	–	–
21	B	132	2	189.0	S	1	85	74	63	12.4	5	8	10	–	A	YES	NO	1056	1	10-14	14	0	0	250	100	–
21	C	110	2	68.3	S	1	73	62	51	9	5	8	10	–	A-C	NO	NO	830	1	NO	0	0	0	–	–	–
21	D	110	1	68.3	W	8	36	32	28	4	5	7	9	–	A	NO	NO	330	1	NO	0	–	0	–	–	–
21	E	110	1	54.8	S	3	61	61	61	7	5	8	10	–	A	YES	NO	880	1	14	38	0	0	35	10	–
21	F	110	2	103.6	S	1	85	74	63	12.3	5	8	10	–	A	YES	NO	1056	1	NO	0	0	0	–	–	–
22	A	110	2	86.5	S	2	62	54	46	8.8	5⅞	7	8	35/45	A	NO	NO	530	2	NO	0	0	0	51	–	–
22	B	110	2	86.5	S	2	62	52	42	8.2	4¼	7	8	33/45	A	NO	NO	530	1^{g}	NO	0	0	0	51	–	–
22	C	165	2	135.2	S	2	74	62	50	9.5	4¾	10	12	44/45	A	YES	NO	880	2	NO	0	0	0	175	–	–
23	A	132^{i}	1	28.4	S	5	55	41	41	11.9	5⅛	10	12	50/45	A	NO	YES	1410	1	NO	0	0	0	–	–	–
23	C	132^{j}	2	56.9	S	1	83	70	57	11.9	4¾	10	12	47/45	A	YES	NO	975	2	12	31	5	62	–	45.8^{k}	–
23	D	132	1	30.7	S	1	85	70	57	10.8	5⅛	10	12	50/45	A	NO	YES	1195	1	NO	0	0	0	60	–	–
23	G	132	2	65.0	S	1	83	70	57	10.9	4¾^{v}	10	12	47/45	A	NO	YES	1270	1	10-12-14	15	0	0	38.9	–	–
23	H	132	2	28.7	S	1	93	78	65	12.9	5⅛	11^{h}	12	50/45	A	NO	YES	1175	1	NO	0	0	0	31.5	–	–
23	J	132	2	89.5	S	1	83	70	57	10.9	4¾^{v}	12^{h}	12	50/45	A	YES	NO	1000	1	NO	0	0	0	2.2	–	–
23	K	132	2	63.2	S	1	83	70	57	10.9	5⅛^{v}	10	12	50/45	A	YES	NO	1100	1	NO	0	0	0	2.5	–	–
23	L	132	2	18.1	S	1	76	63	50	12.9	5⅛	10	12	55/45	A	NO	YES	1620	1	NO	0	0	0	35.8	–	–
23	M	132	2	28.9	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	NO	YES	840	1	NO	0	0	0	–	–	–
23	N	132	2	80.6	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	NO	YES	930	1	NO	0	0	0	–	–	–
23	O	132	1	41.2	S	1	93	78	65	12.9	5¾	9	11	50/45	A	YES	YES	1160	1	NO	0	0	0	–	–	–
23	P	132	2^{r}	73.0	S	1	83	70	57	10.9	4¾	11^{h}	12	47/45	A	YES	NO	1320	1	NO	0	0	0	7.8	–	–
23	R	132	2	21.8	S	1	83	70	57	10.9	4¾	11	12	50/45	A	YES	NO	1160	1	NO	0	0	0	5.5	–	–
23	S	132	2	80.6	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	YES	NO	1065	1	NO	0	0	0	11.0	–	–
23	U	132	2	167.8	S	1	83	70	57	10.9	4¾^{v}	11	12	50/45	A	YES	YES	1240	1	NO	0	0	0	3.7	–	–
23	V	132	1	37.4	S	1	85	70	57	10.9	5⅛	10	12	50/45	A	NO	YES	1200	1	NO	0	0	0	73.5	–	–
23	W	132	2	42.8	S	1	83	70	57	10.9	4¾	12^{h}	12	50/45	A	NO	YES	1130	1	NO	0	0	0	79.1	–	–
23	X	132	1	51.8	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	NO	YES	1180	1	NO	0	0	0	21.7	–	–
23	AA	132^{k}	2	35.0	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	YES	NO	1180	1	NO	0	0	0	–	–	–
23	BB	132	1	39.8	W	5	35	35	35	–	5¾	10	12	–	A	NO	YES	955	0	NO	0	0	0	–	–	–
23	CC	132	2	47.9	S	1	85	70	57	12.9	5⅛	10	12	50/45	A	NO	YES	1260	1	NO	0	0	0	–	–	–
23	DD	132	1	38.9	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	NO	YES	1170	1	NO	0	0	0	42.3	–	–
23	EE	132	2	23.6	S	1-3	83	70	57	12.8	5⅛	10	12	50/45	A	NO	YES	1240	1	NO	0	0	0	–	–	–
23	FF	132	2	40.5	S	1-3	83	70	57	13.0	4¾^{v}	10	12	47/45	A	NO	YES	1370	1	NO	0	0	0	10.9	–	–
23	GG	132	1	47.9	S	1	84	70	57	12.5	–	–	–	–	A	NO	YES	–	1	NO	0	0	0	6.9	–	–
23	HH	132	2	38.0	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	YES	NO	960	2	NO	0	5	52	24.5	–	–
23	II	132	1	65.4	S	1	83	70	57	10.9	5¾	9	11	50/45	A	YES	YES	1150	1	NO	0	0	0	2.5	–	–
23	JJ	132	2	4.9	S	1	83	70	57	10.9	5¾	9	11	50/45	A	YES	NO	1060	1	NO	0	0	0	–	–	–
23	KK	132	2	55.0	S	2	70	58	46	8.8	4¾	10	12	42/45	C	–	–	595	2	NO	0	0	0	12.0	–	–
23	LL	132^{l}	2	18.5	S	1	83	70	57	11.9	5⅛	10	12	50/45	A	NO	YES	640	1	NO	0	0	0	5.0	–	–
25	A	132	2	30.8	S	1	70	58	46	8.5	4¾	12	13	42/60	A	YES	NO	814	1	NO	0	0	0	–	–	–
25	AA	132	2	27.6	S	2	68	55	42	12.3	5¼	11	13	48/45	C	NO	NO	800	2	NO	0	0	0	–	–	–
25	B	132	2	119.6	S^{w}	2	70	54	40	20.5	5¼	11	13	66/45	C	NO	NO	830	2	NO	0	0	0	6.9	–	56
25	C	132	1	135.5	W	7	33	33	33	10.0	5¾	11	13	42/45	A	YES	NO	324	2	NO	0	(x)	10	–	–	60
25	E	110^{p}	2	143.6	S	1	70	60	50	8.3	5¾	9	8	36/45	C	NO	NO	755	1	NO	0	0	0	–	–	–
26	A	132	4^{h}	31.5	S	9	88	–	72	18.9	5	12	14	49/54	A	YES	NO	930	2	11-15	93	0	0	58	11.4	30
27	A	115	2	29.6	S	2	60	50	40	11.0	5	8	10	46/48	A	NO	NO	528	2	NO	0	0	0	–	–	–
29	A	130	2	54.7	S	5	72	59	46	10.3	4¾	10	12	30/45	C	NO	NO	698	2	NO	0	0	0	–	–	–

FOR FOOT NOTES SEE END OF THIS TABLE.

CONSTRUCTION OF 110 KV. TO 165 KV. LINES

GRADING & ARC DEVICES[1]	FAULT CLEARING TIME (CYCLES)			LINE OPERATION														LIGHTNING TERRITORY[2]	ISOCERAUNIC LEVEL	OPERATING RECORD SATISFACTORY	CONSIDERING IMPROVEMENTS TO BETTER LINE PERFORMANCE	COMPANY	LINE
	RELAYS		OCB (MIN)	YEARS IN SERVICE	OUTAGES DUE TO LIGHTNING ONE CIRCUIT / TWO CIRCUIT										AVG. OUTAGES/ 100 MILE OF LINE/YEAR	CASES OF							
	PRIMARY (MIN.)	BACK UP (MIN.)			1928	1929	1930	1931	1932	1933	1934	1935	1936	1937		BURNED DOWN CONDUCTORS	PROLONGED OUTAGES						
28	29	30	31	32	33	34	35	36	37	38	39	40	41	42	43	44	45	46	47	48	49	1	2
NO	–	–	–	9	0/0	0/0	0/0	1/1	1/0	0/0	1/0	2/1	0/0	1/0	1.0/0.4	0	0	M	38	YES	NO	18	L
NO	–	–	–	7	–	–	0/0	0/0	0/0	0/0	2/0	0/0	0/0	0/0	3.1/0	0	0	M	38	YES	NO	18	M
NO	–	–	–	9	0/0	7/2	3/0	2/0	3/1	0/2	2/0	1/2	3/0	1/0	3.3/1.2	0	0	M	38	NO	YES-GR.RDS.&CP.	18	N
LH	12	NO	–	26	17	20	14	13	2	3	5	8	3	7	5.6	2	2	M	35	YES	YES-GR.RDS&GR.WR.	20	A
LH-FC	12	NO	–	13-8	3/0	4/0	1/0	1/3	0/0	0/0	0/0	0/0	0/0	1/0	2.3/0.7	0	0	M	35	YES	NO	20	B
LH	12	NO	–	18	16	14	17	23	6	10	13	7	6	4	26.2	1	1	M	35	YES	YES-CP.	20	C
LH	3	NO	–	10	0	3	1	2	0	2	5	0	4	1	4.7	0	0	M	35	YES	YES-CP.	20	D
LH	1	NO	–	20	1	2	5	4	4	21	8	3	14	24	15.8	2	3	M	35	NO	YES-CP.&GR.RDS.	20	E
NO	3	3	18	23	2	0	0	2	1	2	0	1	1	0	1.8	0	0	L	25	YES	NO	21	A
NO	3	3	18	6	–	–	–	–	–	13	28	13/5	13/11	8/11	4.6/6.9	0	0	S	25	NO	YES-GR.W.-CP.&T.	21	B
NO	3	3	20	8	–	–	5/0	0/1	0/0	2/4	3/4	0/0	2/1	3/5	2.8/2.4	0	0	H	25	YES	NO	21	C
NO	3	3	20	18	0	0	0	0	0	0	0	1	1	1	0.44	0	1	H	25	YES	NO	21	D
NO	3	3	20	4	–	–	–	–	–	–	–	6	7	3	9.8	0	1	L	25	NO	YES	21	E
NO	3	3	18	9	–	–	1/1	1/0	2/1	5/1	2/0	2/1	1/2	4/0	2.2/0.7	0	0	M	25	NO	YES	21	F
LH	6	120	8	27	1/0	6/1	0/4	3/3	2/3	3/5	0/2	4/5	0/0	1/2	2.3/2.9	NIL	3	M	25	NO	YES-CP.INST.1938	22	A
LH	6	120	8	17	4/3	13/7	10/6	8/4	4/4	16/3	4/3	6/5	4/1	4/3	8.7/4.5	NIL	2	M	25	NO	YES-CP.INST.1938	22	B
LH	2	38	14	11	3/0	6/0	10/3	13/4	0/5	3/8	12/10	1/3	1/3	3/4	3.8/3.0	NIL	1	M	25	NO	YES-CP.INST.1938	22	C
NO	1	69	22	8	–	–	3	8	0	3	5	9	7	0	13.2	0	0	S	56	YES	NO	23	A
RR	1	29	22	8	–	–	4/4	4/2	3/5	2/2	2/5	3/13	5/4	0/5	4.5/8.0	0	0	M	27	YES	NO	23	C
RR-T[L]	1	29	8	11	6.3	10.1	14.5	11.3	3.8	8.1	2	1	1	0	18.8	0	0	S	55	YES	NO-T NOW INST.	23	D
RH-T[m]	3.5	20	8	11	5/2	19/11	7/4	15/15	4/2	5/2	15/1	5/2	32/8	10/0	18.0/7.3	0	0	S	53	YES	NO-T&CP NOW INST.	23	G
RH	1	28	8	11	1/1	6/3	2/3	6/2	0/0	3/4	3/3	1/3	3/4	4/0	10/8	0	0	S	54	YES	NO	23	H
RR	1	35	8	5	–	–	–	–	–	4/1	3/0	3/0	5/1	6/4	6/1.3	0	0	H	43	YES	NO	23	J
RH	1	41	8	9	–	0	3	16	3	5/1	5/1	5/1	6/0	4/3	8.3/1.9	0	0	H	44	YES	NO	23	K
NO	1	35	8	4	–	–	–	–	–	–	3/6	1/1	3/1	4/4	13.8/16.5	0	0	S	54	YES	NO	23	L
RR	1	46	8	9	–	1/0	2/0	0/0	0/1	0/0	2/0	1/0	3/1	3/0	4.6/0.8	0	0	H	45	YES	NO	23	M
RR	1	29	8	9	–	5/1	6/3	2/0	3/1	9/1	4/0	4/2	3/0	11/0	6.5/1.1	0	0	H	46	YES	NO-FB NOW INST.	23	N
RH	1	41	8	10	7	9	1	6	5	11	0	6	–	–	13.6	0	0	H	41	NO	YES-GR-CP INST. 1938	23	O
RH-T[P]	1	41	8	13	10/2	9/2	1/3	3/5	3/2	2/4	16/8	14/9	6/0	12/1	10.4/5.0	0	0	S	45	YES	NO	23	P
RR-RH	1	18	8	13	2	1	0/0	0/1	0/2	1/1	0/4	2/0	5/3	2/0	6.0/6.3	0	0	S	46	YES	NO	23	R
RR	1	24	8	8	–	3	3	5	3	9	5/3	7/2	8/2	11/6	7.8/3.3	0	0	H	45	NO	YES-GR-CP-T-FB.	23	S
RR	1	29	8	12	5/7	24/3	11/1	19/4	8/5	10/4	13/3	10/6	11/4	11/7	7.7/2.6	0	0	S	50	YES	NO	23	U
RR-T[L]	1	41	8	11	7.6	12.1	17.5	13.7	4.5	9.7	1	3	7	12	23.4	0	0	S	55	NO	YES-T REVAMP.	23	V
RH	1	29	8	12	6/4	11/5	8/3	15/11	4/6	3/2	7/8	6/2	23/20	10/15	21.7/18.0	0	0	S	54	NO	YES-GR.-CP.-FB.	23	W
RH	1	41	8	10	5	13	7	9	7	6	13	2	19	4	16.4	0	0	S	56	YES	NO	23	X
RR	1	24	22	7	–	–	–	4/5	2/2	4/3	2/1	1/1	8/0	2/4	9.4/6.6	0	0	S	51	YES	NO	23	AA
NO	1	18	8	8	–	–	11	17	7	7	19	17	21	20	37.2	0	0	S	54	NO	YES-GR.-CP.	23	BB
RH	1	41	8	10	16/2	11/8	7/1	10/7	8/8	6/7	3/9	6/6	13/7	9/7	18.6/13.2	0	0	S	54	NO	NO	23	CC
RH	1	24	8	10	5	16	9	21	16	9	6	16	22	5	32.2	0	0	S	55	NO	YES-GR.-CP-FB.	23	DD
RR	1	41	8	9	–	4/3	1/2	3/1	1/1	1/3	3/1	5/4	11/8	0/1	13.6/11.2	0	0	S	53	YES	NO	23	EE
RH	1	41	8	12	12/1	11/4	4/1	5/5	2/0	11/3	8/5	0/2	11/2	1/3	16.0/6.4	0	0	S	54	YES	NO	23	FF
T	1	29	8	2	–	–	–	–	–	–	–	2	1	0	2.1	0	0	S	52	YES	NO	23	GG
RR	1	35	8	8	–	–	3/0	0/4	2/2	5/4	6/5	6/1	3/3	4/2	9.5/6.9	0	0	H	40	YES	NO	23	HH
RH	1	24	8	9	–	7	3	10	7	6	8	3	4	7	9.3	0	0	H	42	NO	YES-2 CIRC. 1938	23	II
RH	10	35	8	13	0/0	1/2	1/0	0/0	2/1	0/0	0/0	2/0	0/1	1/0	14.3/8.2	0	0	H	41	YES	NO	23	JJ
HH	1	29	22	22	2/0	2/0	0/0	0/2	1/0	1/0	0/1	1/0	1/1	0/0	1.5/0.7	0	0	M	45	YES	NO	23	KK
RR	1	18	22	8	–	–	3/0	1/0	0/0	1/0	0/1	1/0	3/1	1/1	6.8/2.0	0	0	H	45	YES	NO	23	LL
RR	INST.	102	8	13	–	} –	–	1/1	3/1	0/1	7/0	8/2	1/0	6/0	6.4/1.2	0	0	S	55	YES	NO	25	A
RR	INST.	90	8	8	–	–	–	–	–	–	–	–	–	–		0	0	S	55	YES	NO	25	AA
NO	INST.	24	8	7	–	–	–	0/0	0/0	1/0	0/0	0/0	0/0	0/0	0.12/0	0	0	S	55	YES	NO	25	B
NO	INST.	24	8	7	–	–	–	0	0	1	1	0	0	0	0.21	0	0	S	55	YES	NO	25	C
HH	18	NO	–	26	6/0	2/0	0/0	2/1	6/0	0/0	5/0	2/0	2/0	1/0	1.8/0.07	1	1	S	55	YES	NO	25	E
NO	12	90	8	3	–	–	–	–	–	–	–	0	0	0	1	0	1	H	35	YES	NO	26	A
LH	18	51	12	8	–	–	0/0	1/1	0/1	2/2	1/0	1/0	0/0	1/0	2.1/1.7	0	0	H	45	YES	NO	27	A
LH[3]	28	INST.	12	24	0/1	0/0	0/1	0/0	0/0	1/0	0/0	0/0	1/0	1/1	0.6/0.6	1	1	M	35	YES	NO	29	A

TABLE II (CONTINUED) LIGHTNING PERFORMANCE AND

COMPANY	LINE	LINE CONSTRUCTION: K.V.	CIRCUITS	MILES (R.W.)	STEEL - WOOD	STRUCT. (FIG. Nº)	CONDUCTOR HEIGHT AT TOWER MIN.-(FT.): TOP	MID.	BOT.	GR.W. HEIGHT (FT.) ABOVE TOP COND.	INSULATORS: SPACING (INCHES)	NUMBER: SUSP.	D.E.	CLEARANCE-LINE TO STRUCTURE (IN.) COND. SWING (DEG.)	CONDUCTOR ALUM. - COP.	ARMOR RODS	VIBRATION DAMPERS	AVG. SPAN (FT.)	LIGHTNING PROTECTION: GROUND WIRES	COUNTER POISES: FIG. Nº	% OF LINE	GROUND RODS: PER TOW.	% OF LINE	GR. RES. OF STRUCTURES: AVG.: BEFORE CP.-GR.	AFTER CP.-GR.	MAX: AFTER CP.-GR.
1	2	3	4	5	6	7	8	9	10	11	12	13	14	15	16	17	18	19	20	21	22	23	24	25	26	27
29	B	130	2	79.5	S	5	72	59	46	10.3	4 3/4	10	12	30/45	C	NO	NO	633	2	NO	0	NO	0	—	—	—
29	C	130	1	54.6	W	7	41	41	41	6.3	4 3/4	10	12	35/45	C	NO	NO	546	2	NO	0	1	100	—	—	—
29	D	130	1	79.8	W	7	41	41	41	6.3	4 3/4	10	12	35/45	C	NO	NO	513	2	NO	0	1	100	—	—	—
29	E	130	1	82.1	W	7	41	41	41	7.3	5 1/8	10	12	41/45	C	NO	NO	612	2	NO	0	1	100	—	—	—
29	F	130	1	9.6	W	7	41	41	41	6.1	4 3/4	10	12	35/45	C	NO	NO	475	2	NO	0	1	100	—	—	—
30	A	110	2	21.8	S	2	58	49	41	5.6	5 3/4	7	9	42/45	C	NO	NO	483	1	NO	0	NO	0	—	—	—
30	B	110	2	21.0	S	1-2	58	49	41	7	5 3/4	7	9	42/45	C-A	NO	NO	476	1-2	NO	0	NO	0	—	—	—
30	C	110	2	56.0	S	2	58	49	41	7	5 3/4	7	9	42/45	A	NO	NO	508	2	NO	0	NO	0	—	—	—
30	D	110	2	109.0	S	2	58	49	41	7	5 3/4	7	9	42/45	A	NO	NO	538	2	NO	0	NO	0	—	—	—
30	E	110	2	71.5	S	1-2	58	49	41	7	5 3/4	7	9	42/45	A	NO	NO	485	1	NO	0	NO	0	—	—	—
31	A	132	1	80.9	W	7	40	40	40	8.1	5 3/4	10	12	76/45	C	—	NO	588	2	NO	0	NO	0	—	—	—
31	B	132	1	111.0	W	7	42	42	42	—	5 3/4	9	11	79/45	A	YES	NO	587	0	NO	0	1	100	—	—	—
32	A	120	2	38.1	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	800	1	NO	0	1	100	—	—	—
32	B	120	2	55.5	S	1	84	72	60	8.5	5	9	10	42/45	C	NO	NO	720	1	NO	0	NO	0	—	—	—
32	C	120	2	18.1	S	1	84	72	60	8.5	5	9	10	42/45	C	NO	NO	700	1	NO	0	NO	0	—	—	—
32	D	120	2	13.3	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	740	1	NO	0	NO	0	—	—	—
32	E	120	2	35.5	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	730	1	NO	0	NO	0	—	—	—
32	F	120	2	25.8	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	740	1	NO	0	NO	0	—	—	—
32	G	120	2	16.1	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	740	1	NO	0	NO	0	—	—	—
32	H	120	2	3.6	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	600	1	NO	0	NO	0	—	—	—
32	I	120	2	15.8	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	630	1	NO	0	NO	0	—	—	—
32	J	120	2	14.5	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	680	1	NO	0	NO	0	—	—	—
32	K	120	2	20.7	S	1	81	71	61	8	5	9	10	42/45	C	NO	NO	530	1	NO	0	NO	0	—	—	—
32	L	120	2	17.7	S	1	77	66	55	8	5	9	10	42/45	C	NO	NO	470	1	NO	0	NO	0	—	—	—
34	A	110	1	71.6	W	7	44	44	44	8	5 3/4	7	12	—	C	NO	NO	692	2	NO	0	1	100	—	—	—
34	B	110	1	84.1	W	7	48	48	48	9.5	6 7/16	8	12	—	A	YES[Y]	NO	902	2	NO	0	1	100	—	—	—
34	C	154	1	89.5	W	7	34	34	34	8.5	5 3/4	10	12	—	C	NO	NO	453	2	NO	0	1	100	—	—	—
34	D	110	2	48.6	S	2	63	53	43	7	5 3/8	7	8	—	C	NO	NO	645	2	NO	0	NO	0	—	—	—
34	E	110	1	129.5	S	3	56	56	56	12	5 1/2	10	11	—	A	YES[Y]	YES[Y]	970	2	NO	0	NO	0	—	—	—
34	F	110	1	97.4	W	7	49	49	49	8	5 3/4	7	12	—	A	YES	NO	828	2[E]	NO	0	1	100	—	—	—
34	G	110	1	38.6	W	7	36	36	36	7.9	5 3/8	7	8	—	A	NO	NO	713	2	NO	0	1	100	—	—	—
34	H	110	1	130.0	W	7	52	52	52	8.3	5 3/4	8	9	—	A	YES[Y]	NO	850	2	NO	0	1	100	—	—	—
34	I	110	1	58.8	W	7	45	45	45	6.5	6 7/16	8	11	47/30	A	YES	NO	910	2	NO	0	1	100	—	—	—
34	K	110	1	38.3	S	1	66	56	46	8	4 3/4	7	9	56/30	C	NO	NO	679	0	NO	0	NO	0	—	—	—
34	L	110	2	87.8	S	2	60	51	42	7.3	4 3/4	8	12	35/30	C	NO	NO	615	2	NO	0	NO	0	—	—	—
34	M	110	2	55.9	S	1	66	56	46	8.1	4 3/4	7	9	47/30	C	NO	NO	663	1	NO	0	NO	0	—	—	—
34	N	110	2	21.2	S	2	74	60	46	12.6	5	9	10	67/30	A	YES	NO	999	2	NO	0	NO	0	—	—	—
34	O	110	1	104.5	S	3	44	44	44	5.5	4 3/4	11	12	42/30	C	NO	NO	603	0	NO	0	NO	0	—	—	—
35	A	132	2	73.9	S	1	71	60	49	9.3	4 3/4	9	11	36/45	C	NO	NO	810	1	NO	0	NO	0	—	—	—
35	B	132	1	85.7	S	2	81	68	55	12.3	4 3/4	9	11	38/45	A	NO	NO	940	1	NO	0	NO	0	—	—	—

FOOT NOTES TO TABLE II

(1) RR-RING AND RING; RH-RING AND HORN; LH-LINE HORN ONLY; HH-HORN AND HORN; P-THOMAS SPOONS; T-PROTECTOR TUBES; LR-LINE RING ONLY; FC-FLUX CONTROLS; FB-FAST RECLOSING, HIGH SPEED O.C.B.

(2) S-SEVERE; H-HEAVY; M-MEDIUM; L-LIGHT.

(3) ONE CIRCUIT ONLY.

(4) TWO SINGLE CIRCUIT LINES ON ONE RIGHT OF WAY.

(5) OUTSIDE CONDUCTORS ONLY-CENTER CONDUCTOR 8 UNITS.

(6) TWO PER CIRCUIT.

(7) DEAD ENDS ONLY.

(8) LINES 15A AND 15B ON SAME TOWERS (279 TOWERS) 15A ALONE ON 37 TOWERS.

(a) ONE GROUND WIRE PER CIRCUIT.

(b) TOP CONDUCTOR ONLY.

(c) 15D AND 15E ON SAME TOWERS FOR 198 TOWERS.

(d) 2 TO 3 GROUND RODS AT TOWERS WITH CONCRETE ANCHORS ONLY.

(e) GROUND RODS INSTALLED APRIL 1937.

(f) LINE IN TWO SECTIONS - 1 CIRCUIT 29 MILES / 2 CIRCUIT 9.2 MILES.

(g) ON COUNTERPOISE SECTION ONLY.

(h) 25 CYCLE SINGLE PHASE.

(i) BUILT FOR 132 KV. - OPERATED AT 44 KV.

(j) BUILT FOR 132 KV. - OPERATED AT 66 KV.

CONSTRUCTION OF 110 KV. TO 165 KV. LINES

GRADING & ARC DEVICES[1]	FAULT CLEARING TIME (CYCLES)			LINE OPERATION														LIGHTNING TERRITORY[2]	ISOCERAUNIC LEVEL	OPERATING RECORD SATISFACTORY	CONSIDERING IMPROVEMENTS TO BETTER LINE PERFORMANCE	COMPANY	LINE
	RELAYS		OCB (MIN)	YEARS IN SERVICE	OUTAGES DUE TO LIGHTNING ONE CIRCUIT / TWO CIRCUIT										AVG. OUTAGES/100 MILE OF LINE/YEAR	CASES OF							
	PRIMARY (MIN.)	BACK UP (MIN.)			1928	1929	1930	1931	1932	1933	1934	1935	1936	1937		BURNED DOWN CONDUCTORS	PROLONGED OUTAGES						
28	29	30	31	32	33	34	35	36	37	38	39	40	41	42	43	44	45	46	47	48	49	1	2
LH[s]	36	INST.	14	24	0/0	0/0	1/1	0/1	0/0	1/0	0/0	0/0	0/0	1/1	0.4/0.4	0	0	M	35	YES	NO	29	B
NO	23	INST.	13	20	0	0	0	0	0	0	0	0	0	0	0	0	0	M	35	YES	NO	29	C
NO	34	INST.	22	20	1	0	0	1	0	0	0	0	1	1	0.5	0	0	M	35	YES	NO	29	D
NO	30	NO	10	11	0	0	1	0	0	0	0	0	3	0	0.5	1	1	M	35	YES	NO	29	E
NO	41	NO	14	14	0	0	0	0	0	0	0	0	0	0	0	0	0	M	35	YES	NO	29	F
NO	1	—	8	16	—	—	—	—	—	9/0	0/0	1/0	2/0	0/0	11.4/0	0	0	M	30	YES	NO	30	A
NO	1	—	8	8	—	—	—	—	—	1/0	0/0	0/0	1/0	0/0	1.8/0	0	0	M	30	YES	NO	30	B
NO	1	—	8	15	—	—	—	—	—	0/0	1/1	0/1	0/0	0/0	0.4/0.7	0	0	M	30	YES	NO	30	C
NO	1	—	20	12	—	—	—	—	—	7/5	7/1	6/1	0/2	11/3	5.7/2.2	0	0	M	30	NO	NO	30	D
NO	1	—	20	7	—	—	—	—	—	8/0	2/1	2/3	1/0	3/1	4.5/1.4	0	0	M	30	YES	NO	30	F
NO	20	INST.	7	11	—	—	—	—	—	—	—	—	—	1	1.2	0	0	L	25	YES	NO	31	A
NO	INST.	45	3	17	—	—	—	—	—	—	—	—	—	0	0	0	0	L	25	YES	NO	31	B
NO	2	17	5	14	8/0	5/2	1/0	2/0	1/1	2/0	1/0	0/0	0/1	5/3	6.6/1.1	0	2	H	35	YES	NO	32	A
LH	2	15	5	10	2	1	3/0	3/0	2/0	7/0	4/0	1/0	2/0	3/1	5.6/0.2	1	1	H	35	YES	NO	32	B
LH	2	26	5	11	1/0	1/0	0/0	0/0	0/0	1/0	0/0	0/0	4/0	2/0	5.0/0	0	0	H	35	YES	NO	32	C
LH	2	32	5	14	7/0	3/0	1/0	1/0	1/0	0/0	0/0	0/0	0/0	2/0	11.3/0	0	0	H	35	YES	NO	32	D
LH	2	36	5	14	9/0	1/0	1/0	3/1	4/1	1/0	2/0	2/0	1/0	4/1	7.9/0.8	0	0	H	35	YES	NO	32	E
LH	2	33	5	13	2/0	2/1	1/0	1/0	2/1	1/0	2/0	0/0	2/0	0/1	5.0/1.2	1	1	H	35	YES	NO	32	F
LH	2	19	5	13	1/1	0/0	1/0	2/0	3/1	1/0	2/0	0/0	1/0	7/1	11.2/1.9	1	1	H	35	YES	NO	32	G
LH	2	22	5	14	0/0	0/0	0/0	0/0	0/0	0/0	0/0	0/0	0/0	0/0	0/0	0	0	H	35	YES	NO	32	H
LH	2	24	5	14	2/0	0/0	0/0	1/0	0/0	1/0	0/0	1/0	4/0	0/0	5.9/0	1	2	H	35	YES	NO	32	I
LH	2	24	5	12	2/0	0/0	0/0	2/0	0/0	0/0	0/0	1/0	0/0	0/1	3.5/0.7	1	3	H	35	YES	NO	32	J
LH	2	27	5	12	2/0	2/0	1/0	0/0	0/0	1/0	0/0	2/0	4/0	3/1	7.3/0	2	2	H	35	YES	NO	32	K
LH	2	27	5	10	0/0	1/0	0/0	2/0	0/0	1/0	0/0	2/0	0/0	1/0	4.0/0	0	0	H	35	YES	NO	32	L
LH	1	75	8	13	22	6	2	4	5	3	8	0	11	3	9.0	0	2	M	68	YES	NO	34	A
RR[Y]	1	70	8	9	-	5	1	2	1	1	1	1	1	6	2.5	0	0	H	68	YES	NO	34	B
LH	1	60	8	11	10	6	7	4	7	9	6	7	12	7	8.4	3	10	H	68	YES	NO	34	C
LH	1	50	8	24	17/11	9/4	6/6	3/11	7/2	1/4	11/30	2/7	4/13	7/9	13.8/20	18	31	H	68	NO	YES	34	D
RR	1	60	8	10	37	14	7	17	18	19	23	15	38	17	15.9	0	5	S	68	YES	NO	34	E
LH	1	60	8	11	23	10	19	23	24	18	31	15	39	21	23.0	7	14	S	68	YES	NO	34	F
LH	1	50	8	15	11	7	9	8	5	7	15	7	14	7	23.4	2	5	H	68	YES	NO	34	G
LH	1	15	8	12	23	17	8	17	13	8	14	11	7	12	10.0	0	4	S	68	YES	NO	34	H
NO	20	NO	18	9	—	—	6	2	0	2	3	0	5	6	4.9	0	0	H	65	YES	NO	34	I
NO	INST.	NO	18	14	34	19	18	25	25	12	18	15	42	25	61.0	0	0	H	65	NO	NO	34	K
NO	10	60	18	25	7/6	6/1	10/2	4/7	6/3	4/4	10/6	3/4	27/8	4/1	9.2/4.4	7	1	M	65	YES	NO	34	L
NO	INST.	50	18	14	21/7	28/5	11/23	6/11	14/17	12/12	18/21	5/5	20/31	7/13	25.7/26.0	13	6	S	65	NO	NO	34	M
(s)	15	60	12	8	—	—	—	4/0	4/1	5/1	5/3	5/1	11/1	7/1	27.7/5.4	3	1	M	65	YES	NO	34	N
NO	INST.	90	8	13	56	24	27	37	29	13	46	16	50	41	22.8	0	4	S	65	NO	NO	34	O
LH	3	NO	5	13	—	—	4/2	2/4	4/3	6/2	7/1	3/1	1/4	2/3	4.9/3.4	1	2	H	45	—	YES - GR. RDS.	35	A
NO	5	NO	5	10	—	—	2	2	2	4	6	2	2	4	0.9	0	0	H	45	—	YES-GR. RDS.-C.P.	35	B

(k) AFTER INSTALLING GROUND RODS BUT BEFORE COUNTERPOISES.

(l) PROTECTOR TUBES INSTALLED SPRING OF 1934 AND RING AND RINGS REMOVED.

(m) PROTECTOR TUBES INSTALLED ON ONE CIRCUIT SUMMER OF 1933 - RINGS AND HORNS REMOVED FROM THIS CIRCUIT.

(n) TOP AND MIDDLE PHASES ONLY; BOTTOM PHASE 10 UNITS.

(o) SINGLE AND DOUBLE CIRCUIT OUTAGES NOT SEGREGATED.

(p) 25 CYCLE LINE.

(q) BUILT FOR 132 KV., OPERATED AT 33 KV.

(r) ONE CIRCUIT WITH PROTECTOR TUBES - AND SECTIONALIZED AT MID-POINT - PROTECTOR TUBES INSTALLED SEPTEMBER 1933.

(s) PROTECTOR TUBES EVERY FIFTH TOWER.

(t) RINGS AND RINGS ONE CIRCUIT - SPOONS ON OTHER CIRCUIT.

(u) LINE 2AA AND CONTINUING 2A LINE INCLUDED TOGETHER AS 38.8 MILES.

(v) SOME LONGER SPACED UNITS IN USE.

(w) WOOD ARMS.

(x) AS REQUIRED TO REDUCE RESISTANCE TO 60 OHMS OR LESS.

(Y) ON PART OF LINE ONLY.

(z) GROUND WIRES ON 67.3 MILES ONLY.

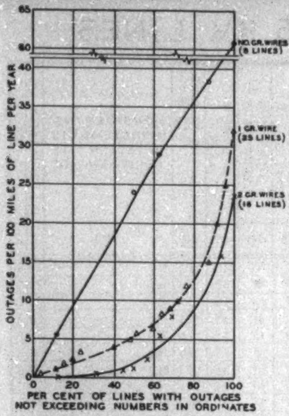


Figure 16. Effect of ground wires on lightning outages of single-circuit lines

of outages (from table III) is 1.40. While these figures are indicative of the benefit accruing from using a second ground wire it should be kept in mind that many of the two-ground-wire lines have been constructed with more insulation and other lightning resistant features than have generally been employed on some of the older single-circuit lines.

A clearer graphic picture of the benefits of the use of ground wires is shown in figure 16. On all single-circuit lines with and without ground wires, where the record is clear, it is shown that 50 per cent of the lines included here have outages not exceeding 24 for 100 miles with no ground wire, 5.5 with one ground wire, and 2.0 with two ground wires.

Considerable theory and discussions have been presented in the past on the desirability of locating the ground wire at a considerable height above the line conductors. In an attempt to obtain statistical data on this subject, the average line outages for 10 years, or the period reported, have been plotted against the height of ground wire above the top conductor. The record is shown in figure 17. While it appears that two lines, one with a ground wire 18.9 feet and the other 20 feet above the line conductor, have a practically perfect operating record, it should be pointed out, however, that both of these lines also have line insulation and clearance at the tower considerably higher than has been generally reported for other lines of the same voltage classification. With outages ranging from 0 to 61.0 per year where the ground wire heights range from 7 to 13 feet above the conductor, however, it would appear that there are other factors more important than the height of the ground wire above the conductor that control the outages.

Single-Circuit and Two-Circuit Outages

A comparison of single- and double-circuit outages is given in figure 18. It will be noted that for 50 per cent of the two-circuit lines the double-circuit outages do not exceed about 1.0 and the single-circuit outages 4.0 per 100 miles of line per year. It thus appears that the double-circuit outages are 20 per cent of the total outages on two-circuit lines for 50 per cent of the cases. For 80 per cent of the lines, 32 per cent of the outages are double circuit. As the yearly outages increase, single- and double-circuit outages become nearly equal.

A comparison of single-circuit steel and wood lines is also shown graphically in this figure 18. Here wood does not show up to any particular advantage against steel.

Tower Footing Resistance

The beneficial effect of tower footing resistance in reducing lightning outages has been presented both theoretically and from practical operating data previously. To throw some further light on this aspect of lightning protection, the pertinent data have been taken from table II and plotted against average tower footing resistance in figure 19. The trend to low outages with low resistance is quite marked here. The general conclusion that can be drawn from this curve is that if the line outages are to be kept to a low value, the tower footing resistance should be not in excess of 10 ohms per tower. It should be noted, however, that low tower footing resistance, or counterpoises, alone will not suffice, as shown by a few yearly outages from 5 to 22 even where the resistance is 10 ohms or less per tower.

Local Lightning Severity

Any accurate comparison of the lightning performance of lines must of necessity take into account the lightning conditions under which the line is called upon to operate. An attempt to reduce lightning outages to a common basis would require a reasonably accurate evaluation of the lightning severity in the territory where the line is located. No easy way of doing this presents itself. However, columns 46 and 47 of table II indicate to some degree the severity of the lightning territory. The data in column 46 is the estimate of lightning severity given by the reporting companies. The figure in column 47 is the isokeraunic level taken from the 20-year average isokeraunic data as published by the United States Government. The lightning territory in this tabulation ranges from lightning storms of a density of 22 storms per year up to a maximum of 68 per year, that is, a ratio of over three to one. No attempt has been made to evaluate line outages taking into account this variation of lightning density as it is felt that the data as a

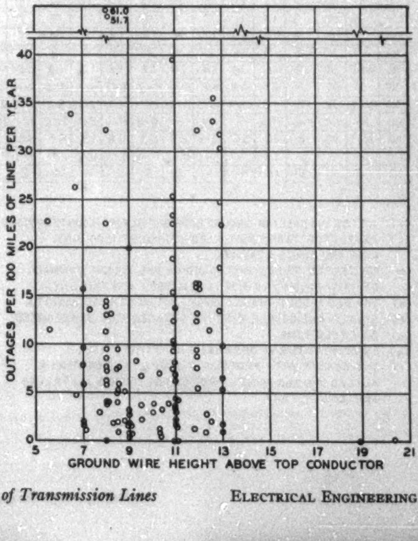


Figure 17. Effect of ground wire height on line outages. Steel-tower lines only

whole do not lend themselves readily to this type of analysis. The conclusions drawn from such an analysis might be so susceptible to error that no quantitative correlation has been attempted. In general, however, it is quite noticeable that the higher figures for line outages per year are associated, as might be expected, with the higher lightning density of the territory where the line is located.

INSULATORS AND LINE CLEARANCE

All insulators are, of course, of the suspension type and range in size from $4^3/_4$-inch spacing to $6^1/_2$-inch. The number of insulators used in suspension strings ranges from 6 to 8 for wood structures and from 6 to 11 for steel structures in the 110-kv class to 9 to 11 for wood and 8 to 12 in the 132-kv class. This is exclusive of the long spaced ($6^1/_2$-inch) unit, used on a few lines.

Where wood structures are used, it will be noted that there is no general tendency to decrease the number of insulators used. However, in some of the 110-kv wood-pole lines seven units are used, although there is little apparent indication that this number of units was used with the intention of utilizing the wood as normal-frequency voltage insulation.

In the matter of clearances at towers between the conductor and the tower structure, it will be noted that the clearance ranges from a minimum of 31 inches for 110-kv lines up to a maximum of 67 inches at 45 degrees and 30 degrees conductor swing, respectively. For the 132-kv lines the range of clearances is from 37 inches to 66 inches at conductor swings in the order of 50 degrees.

In general it is not apparent from an analysis of the line outage data that the amount of insulation provided in the insulator string itself has a great deal of bearing on the performance of the line under lightning conditions. In other words, any attempt to overinsulate a line beyond normal requirements does not appear to have much influence on its lightning outage record.

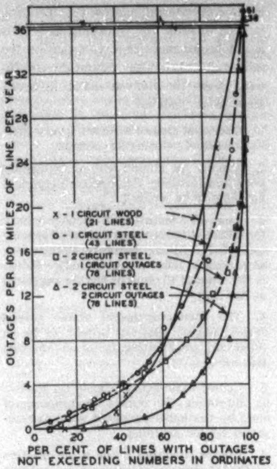


Figure 18. Lightning outages on transmission lines. Single- and double-circuit lines with wood or steel structures

HEIGHTS OF CONDUCTORS

The opinion has sometimes been expressed in the past that a line which was built low to the ground was more immune from lightning troubles. The data presented in table II, however, fail to bear out this contention. For example, a comparison of the operating record of the lines of company 4, where the height of conductors is 46 feet, with the record of company 8, where the height of conductors is 100 feet, shows on an average higher outages for the line built closer to the ground. A comparison of other lines given in the table gives the same type of conflicting data. From this record it might be concluded that whatever factor the height of the line above ground may have in influencing the lightning outages, it is frequently submerged in other factors of line construction.

Table III. Effect of Ground Wires on Lightning Outages of 165–110 Kv Transmission Lines

	Number of Lines	Line Miles	Outages per 100 Miles of Line per Year
No-ground-wire lines			
Single-circuit wood	5	250.9	30.2
Single-circuit steel	3	290.8	29.8
Total		541.7	30.0
One-ground-wire lines			
Single-circuit wood	1	68.3	0.44
Single-circuit steel	27	1,086.1	9.8
Double-circuit wood			
Double-circuit steel	51	2,236.3	7.1/4.2
Two-ground-wire lines			
Single-circuit wood	12	940.0	5.1
Single-circuit steel	4	241.9	7.0
Double-circuit wood			
Double-circuit steel	18	969.9	5.2/3.5

ARCING PROTECTION AND PROLONGED OUTAGES

In an attempt to determine whether high-voltage transmission systems in general are subject to prolonged outages, the data in columns 44 and 45 of table II have been listed. Analysis of these data shows that in general burned-down conductors and prolonged outages, except in the case of one company, are relatively rare and are confined almost entirely to those circuits where the most modern type of arcing protection is absent. Most of the cases reported are on lines having insulator strings equipped with arcing horns on the line side only, or where no arcing protection at all is applied to the insulator strings. A summary of the record shows the following:

	Rings and Horns or Rings and Rings	Line Rings Only	No Protection
Burned-down conductors	1	44	31
Prolonged outages	9	85	27
Miles of line	1,962	2,022	2,613

A study of the relay and breaker times to clear faults (columns 29, 30, and 31 of table II) does not clearly indicate that moderately fast clearing of the fault will mitigate line troubles unless some effective arcing protection is applied to the insulator assemblies. It should be pointed out, however, that the relay and breaker operating times in table II are those presumably in use at the present time, so that troubles reported in many cases may have occurred before means were taken to speed up relay and breaker time.

SATISFACTORY PERFORMANCE OF LINES AND IMPROVEMENTS CONSIDERED

In answer to the question as to whether the lightning outage performance of the lines as given by each company was satisfactory or not and whether improvements were being considered to better that performance, it will be noted from columns

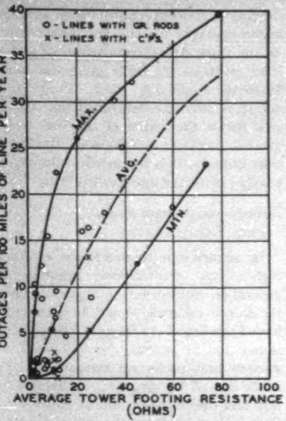


Figure 19. Effect of tower footing resistance on line outages for steel-tower ground-wire lines

48 and 49 in table II that in the large majority of cases, companies reported the operation was considered satisfactory. It should be pointed out, however, as mentioned by one reporting company, that while the outage record of some lines is not considered particularly low, the particular operating conditions do not require the high grade of line performance which might be required under some other operating conditions. For example, where two-circuit lines are in use, a high single-circuit outage record is not highly objectionable. On the other hand, where the outage record is high for double-circuit outages, or for single-circuit outages on single-circuit lines, the condition is entirely different. Again, where lines are operated in parallel with other lines between the source of power and load, or serve as a tie between two sources of power, the outage performance of such lines under lightning conditions may not be as exacting as in the case of lines which are the only source of power supply to important loads.

It will be noted that the general types of improvement being considered comprise lowering tower footing resistance, either by ground rods or counterpoises, use of protector tubes, and, as reported by one company, fast-reclosing high-speed breakers.

Conclusions

In presenting such a voluminous mass of data as given in table II and attempting to correlate and analyze the operating records of some 150 transmission lines it is not hoped more than to touch on the salient points. The following conclusions, from the analysis made, however, seem to be justified:

1. Overhead ground wires are a very effective means of reducing line outages.

2. Placing the ground wire at excessively great heights above the line conductor does not seem to be justified.

3. Low tower footing resistance is necessary to reduce line outages due to lightning (provided lightning discharge devices such as protector tubes are not used). Values in the order of 10 ohms or less are suggested as desirable.

4. The conductor height above ground (tower height) does not appear to be an important contributing factor in affecting line outages.

5. Considerable benefit should be possible in improving the outage performance of lines by the judicious use of wood as insulation against lightning.

6. Double-circuiting a line is an effective method of reducing service outages as shown by the low ratio of double-circuit outages to total outages on such lines.

7. Effective arcing protection on insulator assemblies is desirable to reduce line troubles. Horns on the line side only do not supply such protection.

8. The generally considered means of bettering the lightning performance of lines in the 110- to 165-kv class as reported here are (1) lowering tower footing resistance, by ground rods or counterpoises, (2) protector tubes, and (3) fast-reclosing high-speed breakers.

9. The committee welcomes discussions from any of the companies who have contributed data on which this paper has been based, particularly as they may throw any further light on special features of their line construction which have affected the line performance. Comments will also be appreciated on features not possible to cover fully in answers to the committee questionnaire, or not fully covered in this presentation and discussion.

Discussion

E. R. Whitehead (Duquesne Light Company, Pittsburgh, Pa.): This paper is of special interest and value to me at this time because the company with which I am associated has just completed a rather detailed study of the lightning performance of its 66-kv transmission ring. While there are minor variations in the tower configuration a sufficiently representative one consists of a double-circuit vertical arrangement as in figure 1 of the paper. Top and bottom phases are separated 18 feet between circuits, while the middle phases are separated 22 feet. The vertical separation is 8 feet between top and middle phases and 7 feet between middle and bottom phases. The single ground wire is 7 feet above the top conductor. Six units are used in suspension and seven units for dead end. Air clearance is generally $3^{1}/_{2}$ feet.

Lightning tripouts in outages per hundred tower mile years vary from a low of 3.97 to a high of 56.8. The two sections to which these figures apply lie on opposite sides of the city, and in the past it has been assumed without question that the great differences were attributable to "preferred storm paths."

In an effort to verify this assumption, several exposed 22-kv subtransmission lines without ground wires were chosen adjacent to these 66-kv line sections and tripouts from lightning compiled in the same terms over a five-year period. The figures are 61 and 70, respectively. It is apparent that "preferred storm paths" will not account for the great difference in performance.

Since the study was primarily directed toward the improvement of poorly performing line sections, the worst of these, 28.36 miles in length, was selected together with an adjacent section 15.08 miles long and 32 towers chosen at random for ground resistance measurements. Here again it had been tacitly assumed that the poor performance could be explained by the storm frequency and very high ground resistances. The belief in the presence of high tower footing resistances arose from an earlier survey which included measurements of ground rods disconnected from the towers.

Footing resistances from 1.6 to 78 ohms were measured, the average being 16 ohms. Calculations based upon this average resistance and upon perfect ground-wire shielding gave an over-all flashover figure of 4.25 per cent. The actual flashovers for this section average 11 per year, requiring the absurd total of 260 strokes to the tower line per year. As previously stated, exposed 22-kv lines in the same general area had an outage record of only 70 per hundred miles per year. From this sample, taking into account the relative heights of the 66-kv and 22-kv lines, it has been estimated that the actual number of strokes to the 28.36-mile 66-kv line section was approximately 32.

The first step toward more accurate analysis was the construction of a tower-footing-resistance distribution table from the field data and the recalculation of the over-all flashover percentage. This calculation included midspan flashover for midspan stroke tower flashover for midspan stroke, and tower flashover for tower stroke. The result in this case was 12.45 per cent. It is clear that average ground resistance is not at all a suitable datum upon which to base estimates of line performance, nor can one easily correlate the data in the present paper with calculations. Using this percentage and assuming perfect shielding, the required number of strokes to the line is 88. This figure is still more than double a reasonable number.

The second refinement was an attempt to evaluate the effect of the electrostatically induced potential on the line wire. As has been pointed out,[1] the effect is to lower the tower-top potential necessary to cause insulation flashover, and the per cent flashovers will increase correspondingly.

An approximate method was derived to include this effect by considering a cloud height of 1,000 feet, rectangular bound charge of 2,000 feet length, a lightning current of $^1/_{10}$ microsecond, and discharge increments of one microsecond each. At the point of stroke the maximum induced voltage is proportional to the stroke current, and a fixed constant of proportionality may be added to the tower footing resistance when solving for the current required to cause flashover. The result of this approximation was to increase the per cent flashovers to a value of 15.0.

With such a small increase attributable to the effect of induced voltage on the line wire, it was felt that the assumption of perfect shielding was invalid.

If X is the fraction of total strokes which strike a line conductor, then we may define the "shielding factor" as $(1 - X)$ and the equation governing flashovers is

$$Xn + (1 - X)\,Pn = F$$

where

n = total strokes to tower line
P = per cent flashovers for complete shielding
F = flashovers

Inserting

$$P = 0.15 \qquad F = 11 \qquad n = 32$$

in this equation, there is

$$0.85X + 0.15 = \frac{11}{32} = 0.344$$

$$0.85X = 0.194$$

$$X = 0.228$$

Shielding factor $= 1 - X = 0.772$, a value which has been deduced as the shielding efficiency of the Glenlyn-Roanoke 132-kv line which has a single ground wire.[2]

In order to test this analysis further, a number of tower-footing-resistance measurements were made on the line sections having the best performance. All values were under ten ohms, and since the distribution curve was taken in ten-ohm steps, this value was used to compute comparative performance of these sections.

The results were:

Line	Average Flashovers per Year	
	Actual	Calculated
Colfax-North	3.5	3.70
Colfax-Pine Creek	1.4	1.67
Pine Creek-North	3.0	2.29
B. I.-Woodville	1.7	2.01
Woodville-Dravosburg	1.5	2.23

With one exception, the calculated faults are in excess of the actual as would be expected when Megger resistance values are used instead of the surge values which are somewhat lower. Moreover, some subnormal insulation and clearances may exist on this line.

It appears that the effect of induced line voltage would be negligible on most of the lines whose performance is reported in the paper, but if shielding were generally of the order of 0.75 for single-ground-wire lines it should be evidenced by components independent of ground resistance in the curves of figure 19. Because of the absence of this component, one is tempted to conclude that practically complete shielding exists for the great majority of lines listed.

A partial explanation of this apparent disagreement may lie in the fact that while the ground wire on our lines is 7 feet above the top conductor on the drawing, it has been found as low as $3^1/_2$ feet above the top conductor in numerous instances because of the substitution of suspension towers for strain towers. The exact proportion of these cases is unknown at this writing.

For the transmission engineer who contemplates reconstruction of old lines to improve lightning performance the question of shielding and its numerical measure is of paramount importance, and the writer will welcome any comment which the authors of the paper may make with regard to this point.

REFERENCES

1. Discussion of PROTECTOR TUBE APPLICATION AND PERFORMANCE ON 132-KV TRANSMISSION LINES—II, **Philip Sporn** and **I. W. Gross**, by **L. V. Bewley.** ELECTRICAL ENGINEERING, September 1938.

2. Discussion of LIGHTNING INVESTIGATION ON TRANSMISSION LINES—V, **Lewis** and **Foust**, by **S. M. Zubair.** ELECTRICAL ENGINEERING, March 1936, page 277.

S. K. Waldorf (Pennsylvania Water and Power Company, Baltimore, Md.): In item 26*A* of table II of the paper are given data on the Safe Harbor-Perryville Line of the Pennsylvania Water and Power Company. The single case of burned-down conductor and prolonged outage on this line was due to sleet, and not to lightning. An additional year of operation without outage of this line has been obtained since the data were submitted to the committee, making four years in all.

In the committee's report an attempt has been made to evaluate the benefits to be derived from various factors which enter into lightning protection, such as overhead ground wires, insulation level, etc. As the committee has pointed out, it is very difficult to determine the influence of any one factor upon performance. For instance, in figure 17 of the paper, showing the effect of ground wire height on line outages, the points are widely scattered, indicating that there are other factors which are probably more important than this one item. However, in figure 19, showing the effect of tower footing resistance on line outages, the behavior of lines having counterpoise is shown to be definitely superior to those having ground rods. It should be noted that these data do not indicate any special benefits due to counterpoise other than as a means for reducing tower footing resistance, but they do indicate that tower footing resistance is a powerful factor in lightning protection.

In figures 10 to 15, inclusive, are given a number of general arrangements for counterpoise. The arrangement which has been most effective on the system of the Pennsylvania Water and Power Company has been a graded installation scheme where the number and length of counterpoise conductors have been varied to suit individual tower grounding conditions. It was on the Safe Harbor-Perryville 132-kv line that the first extensive use of this scheme of counterpoise installation was made by the Pennsylvania Water and Power Company. On this line as many as eight conductors in parallel were used as a counterpoise system connected to towers having high footing resistances initially. In such cases four geometrically and electrically parallel conductors were installed along the right-of-way on each side of a tower. Based on experiments in the field, later practice has been to limit counterpoise conductors to six in parallel, three conductors on each side of a tower. Where it has been necessary to install three parallel conductors, they have in many cases been extended to the adjacent towers and the wire used in this way rather than in adding a fourth wire.

Figure 16 indicates a superiority of two overhead ground wires over one overhead ground wire. During the past lightning season, the Pennsylvania Water and Power Company has had the opportunity of comparing the operation of two 69-kv lines of very nearly identical characteristics except as regards the number of overhead ground wires and their position. These two lines are the Holtwood-York line, 22.8 miles long, with two overhead ground wires 10 feet above the topmost line conductors at all towers, and the Holtwood-Coatesville line, 29.4 miles long, with a single overhead ground wire $3^1/_2$ feet above the topmost line conductors at strain towers and $7^1/_2$ feet above at suspension towers. Before the installation of counterpoise, the average tower footing resistances were within one ohm of each other at 133 ohms and after the installation were again within the same limit, but at 9 ohms. The Holtwood-York line has been operating for three years since improvements were made, and has had an average of 2.9 outages per 100 miles of line per year. The Holtwood-Coatesville line has operated for only one year since improvements were made, with one outage occurring, making an average of 3.4 outages per 100 miles of line per year. Although the limited period of operating experience makes it still too early to draw any conclusions, the similar behavior of these two lines indicates that the shielding provided by a single overhead ground wire may be better than heretofore has been believed to be the case.

Examination of the data on the relative frequency of single-circuit and double-circuit outages shows that on those lines having a large number of double-circuit outages, the high values of tower footing resistance are probably the principal cause of the poor performance.

The committee has not found any great dependence of performance upon insulation level. Perhaps a partial explanation of this conclusion is that the insulation level varied no more than about in the ratio of 5 to 3 whereas the footing resistances varied as much as 20 to 1. Variations in the latter factor probably obscured any effects of variation in the former. Lewis and Foust pointed out in 1934 that flashover of shielded lines occurs only when the product of tower footing resistance and tower lightning current exceeds the insulation level of the line. Experience in ensuing years has shown this to be essentially correct, which establishes the close relationship between line insulation and lightning performance.

When tower footing resistances are high, no reasonable amount of insulation will protect against flashover. Data from lines in this classification will show no effects of line insulation upon performance. Analysis of some data available on 19 110-kv steel-tower lines, 787 miles long; 5 132-kv steel-tower lines, 263 miles long; and 9 110-kv wood-pole lines, 460 miles long, all with overhead ground wires, other than those given in the paper, shows a more definite benefit due to increased line insulation. In the 110-kv class, steel lines having six insulator units in strings showed an average of 40 interruptions per 100 miles of circuit per year, those with seven units showed 10.9 interruptions, those with eight units had 5 interruptions, and those with nine units showed 2.2 interruptions. On 132-kv steel lines, those lines with 9 insulator units in strings experienced 21.1 outages per 100 miles of circuit per year, those with 11 units showed 2.8 outages, and those with 12 units, 2.1 outages. Thus, this study showed a decided benefit due to increased insulation on steel-tower lines, but the benefit is probably more apparent than real, because no account was taken of variations in footing resistance when classifying the data. With wooden construction, the investigation showed that on 110-kv lines the outages per 100 miles of line per year were 3.7 with six insulator units, 7.3 with seven units, and 7.2 with eight units. Apparently the effect of the wooden insulation in series with the porcelain masks the effect of the increased number of insulator units, in addition to any uncertainties introduced due to neglecting the effect of tower footing resistances.

In conclusion 7 it is stated that arcing protection on insulator assemblies is desirable to reduce line troubles. It would seem more logical on lines having overhead ground wires to use the money for improving grounding conditions, rather than for arcing protection, and thus greatly reduce the number of flashovers. With modern high-speed switches, severe burning of conductors is rare.

Line 3*A* in table II shows a considerable number of double-circuit outages but no single-circuit outages over a period of ten years. This behavior is most unusual and some reasonable explanation for it would be valuable. In this report the committee has tried to segregate the effect of the various means employed to reduce lightning outages on transmission lines. In making such a segregation the results of necessity must be more or less inconsistent as the three factors; overhead ground wires, insulation, and tower footing resistances, are interdependent. Each one is dependent on the other two for its proper functioning in this scheme of protection.

E. W. Knapp (Shawinigan Water and Power Company, Montreal, Que., Canada): The committee responsible for the collection and preparation of the data incorporated in this paper are to be congratulated on the concise and clear manner in which the wealth of information has been presented. A study of the general situation brings to mind a few additional points which might prove of interest.

The system which I wish to discuss consists of two double-circuit steel-tower lines 86 miles long on the same right-of-way. The lines are operated at 110 kv, and have seven units of insulation. Lines *A* and *B* have two overhead ground wires while lines *C* and *D* have no overhead ground wires. An analysis of the lightning outages during the past ten years reveals the data shown in table I of this discussion, based on outages per 100 miles of line per year.

Table I. Lightning Outages, 1929–1938, Inclusive

Single Line Outages					Double Line Outages		
Overhead Ground Wire		No Overhead Ground Wire		Total	Overhead Ground Wire	No Overhead Ground Wire	Total
A	B	C	D		A/B	C/D	
0.93	0.82	0.23	9.3	11.2	2.8	4.3	7.1
8.3%	7.3%	2.0%	82.4%	100%	40%	60%	100%

There are three points of special interest:

1. The line farthest away from the overhead ground wires suffered the most lightning outages.
2. The two lines without overhead ground wires suffered the greater number of double line outages.
3. There was a greater percentage of double line outages on the lines with overhead ground wires, although the total outages were considerably lower.

Philip Sporn and I. W. Gross: Mr. Whitehead's method of analysis to determine lightning outages on existing lines is quite interesting, and if experience continues to bear out the close relation between actual and calculated values, it should be a method decidedly helpful in predetermining the lightning performance of new lines before they are actually constructed and placed in service.

As Mr. Whitehead points out, the matter of shielding, that is, keeping lightning off the phase wires, is most important since it is quite apparent that no matter how low the tower footing resistance is, it will not be effective in preventing flashover if the stroke terminates on the line wires rather than the ground conductor. This subject well warrants further study and confirming field data.

Mr. Waldorf has drawn the conclusion (apparently based on figure 19) that "the behavior of lines having counterpoise is shown to be definitely superior to those having ground rods." While this appears to be a reasonable deduction, if the data of figure 19 only are considered, it will be noted in referring to table II that only four of the lines out of some 150 have counterpoises throughout 30 per cent of their length or over. Mr. Waldorf's 26*A* line, which has counterpoise for 93 per cent of its length, he reports as having no outages in four years. It will be noted that this line has an average structure ground resistance of 11.4 ohms. On the other hand, line 21*E*, which has counterpoise for 38 per cent of its length, with an average structure footing resistance of 10 ohms, shows 9.8 average outages per hundred miles of line per year. Line 15 *I* having ground rods only for 56 per cent of its length has an average structure resistance of 10 ohms and a yearly outage record of 0.9 per hundred miles of line. While it may be true that a reduction of tower footing resistance to a given level by counterpoises will result in less line outages than a similar reduction by the use of ground rods, it does not appear from the data which have been presented in table II that such a conclusion is wholly justified.

The specific cases mentioned by Mr. Waldorf, one where two ground wires were used and one where only one was used indicating the same relative order of line performance, he has interpreted as indicating that one single overhead ground wire may be giving far better shielding than generally believed. The summarized data in the paper tend to lend weight to this point of view, although as pointed out in the paper, the data in many cases are not complete enough to show definitely the influence of other factors such as number of insulators, line spacing, lightning territory, etc.

The citation by Mr. Waldorf of outages on steel-tower lines as affected by the number of insulator units tends to show some benefit by increasing the number of units from six to nine. This, of course, is an increase in insulation of 50 per cent between extreme limits of insulation. As he has clearly pointed out, it is necessary to take into consideration all of the lightning protective features of the line before definite conclusions can be drawn on the merits of any one influencing factor. An attempt to analyze data of this kind, as well as that given in table II of the paper, requires a rather detailed study of many of the line characteristics before any hard and fast conclusions can be drawn.

Mr. Waldorf suggests the improvement of ground resistance on a line with overhead ground wires, in place of supplying arcing protection, and we are in thorough agreement with this statement where modern, high-speed breakers clear the fault; and, in fact, in recent lines on our own system, we have eliminated the arcing protection where high-speed breakers are used. Where slow line relaying, however, still exists or is a necessity as a result of system operation, arcing protection may still be desirable.

In referring to line 3*A* in table II, we are inclined to believe that the absence of single-circuit outages in the reported figure of 35.5 outages per hundred miles of line per year is due to the absence of a breakdown of the total outages for the ten-year period. The figures are reported as received, but, as pointed out by Mr. Waldorf, they may well be questioned.

In conclusion, it is not expected in attempting to analyze the mass of data such as given in table II that all the various factors can be definitely evaluated. It was the hope of the committee in submitting this report to collect sufficient authentic data on a large group of lines in the country so that careful analysis might show the trends in the construction and the general experience in the operation of such lines under lightning conditions.

Low-Gas-Pressure Cable

G. B. SHANKLIN
MEMBER AIEE

DURING the past several years the company with which the writer is associated has been conducting an extensive research and engineering study of the possibilities of high-voltage cable utilizing gas as a pressure medium. The purpose has been to develop a practical cable system of this type and determine its best field of usefulness from an engineering and economic standpoint. The results of this work to date will be outlined in the present paper.

Since the need of high-voltage underground transmission arose some 40 years ago there has been and will always be a continual effort to obtain better and more economical designs of cable systems. Out of these years of experience certain principles and trends have been established. Of all the different insulating materials available, oil- or compound-impregnated paper is recognized as the best and most economical for cable rated 22 kv and above. It is also widely used at lower voltages. All of the various high-voltage cable systems that have been generally used, or subjected to full-size experimental field trial, in recent years have utilized impregnated paper as the basic insulation.

The cable systems mentioned utilize impregnated paper in different ways to accomplish certain desired results, but all of them follow, or try to follow, the same fundamental principles. These are:

(*a*). Use materials that are chemically and physically stable and of low dielectric loss and high dielectric strength.

(*b*). Eliminate all traces of impurities initially and prevent entrance of such impurities during shipment, field installation, and service.

(*c*). Prevent progressive ionization deterioration at all times.

Experience has demonstrated that the first step in accomplishing these results involves vacuum drying and impregnation under pressure after fabrication of the cable. Uniformly perfect initial impregnation is the only known method of obtaining full control of cable quality and economically meeting the requirements outlined. Incomplete initial impregnation in the factory leads to loss of uniformity, with weak spots along the cable length. Pretreatment of paper tape also makes the complete removal of impurities a questionable process.

The two high-voltage cable systems so far used to an extent that classifies them as standard are the ordinary "solid type" cable and the "oil filled" cable. The essential features of these two cable systems will be briefly outlined for purposes of leading up to a logical field of usefulness for the low-gas-pressure cable system that will be described.

Solid-Type Cable

This is the original type of high-voltage cable, used for 40 years or more. About 20 years ago a growing demand for higher operating voltages and reduced costs led to marked improvement in insulation quality and increase in working voltage stress (decrease in insulation thickness). Within recent years uniformity and practically perfect initial impregnation have been obtained by improvement of the impregnation treatment and the use of nonsolidifying impregnating compounds of lower viscosity.[1,2] Present-day insulation thicknesses represent an average working voltage stress of from 42 to 58 volts per mil.

Solid-type cable has now about reached its ultimate development. Present limitations are inherently physical. Complete impregnation has improved the insulation but has increased the physical problem. Today, we are faced with the following inherent difficulties:

(*a*). No method of controlling relatively wide pressure fluctuations due to temperature changes in service. Compound expansion during heating increases pressure and this puts an undue stress burden on the lead sheath, causing permanent stretching, fatigue splits, and other sources of leakage.

(*b*). During periods of cooling negative pressures (vacuum) develop. Air and moisture are drawn into the system through any leaks that exist.

(*c*). Stretching of sheath and migration of the nonsolidifying compound lead to void formation. During periods of negative pressure cumulative ionization damage is liable to occur.

Oil-Filled Cable

The success of oil-filled cable is too well known to require repetition here. It completely overcomes the inherent difficulties of solid-type cable just discussed.[3] Ionization is eliminated entirely and predetermined safe working stresses on the lead sheath are under control at all times. Positive oil pressure is maintained, preventing entrance of impurities at points of accidental leakage. Low working oil pressure (1 to 30 pounds, depending upon contour) greatly simplifies design, installation, and maintenance. This low pressure allows ordinary lead sheath finish up to 15 pounds and a simple reinforced sheath finish from 15 to 30 pounds. The cable can be installed anywhere that solid-type cable can, drawn into ducts, buried, etc.

Today, the average working voltage stress in oil-filled cable is from 80 to 143 volts per mil, depending upon voltage rating. This is more than twice that of solid type, and the ultimate reduction in insulation thickness has not yet been reached. It will depend upon required impulse strength to resist lightning and switching surges. Experience has shown that normal 60-cycle working stress is not a limiting factor in determining minimum insulation thickness. The rea-

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Paper number 39-71, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 22, 1938.

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A number of engineers associated with the author, too numerous to list in full, have contributed to this development. The writer particularly wishes to acknowledge the valuable help of J. A. Scott and J. B. Felter, who carried out the laboratory tests, S. J. Garahan, W. C. Hayman, C. A. Piercy, E. L. Crandall, F. W. Engster, F. H. Buller, L. L. Phillips, V. A. Sheals, and T. C. Aitchison, all of whom gave generously of their support and encouragement. The fine spirit of co-operation shown by the Consolidated Edison and Yonkers Electric Light and Power companies is gratefully acknowledged.

[1] For all numbered references, see list at end of paper.

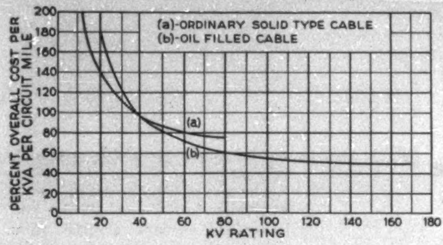

Figure 1. Unit over-all installed costs in underground ducts

son for this is that even one or two pounds positive oil pressure in the feed channels is sufficient to prevent void formation in a properly designed cable.[3] Accordingly, the oil-filled cable system is inherently a low-pressure system. There is no need of complicating matters by increasing oil pressure above that actually required by the contour and length of section to be fed. Technically, the standard low-pressure oil-filled cable system is correct and superior to all high-voltage cable systems so far devised. It is only from an economic standpoint that it leaves room for improvement.

Figure 1 gives comparative over-all installed unit costs for oil-filled and solid-type cable systems in underground ducts as a function of voltage rating. At any given voltage the costs will vary with load but the curves in figure 1 are representative averages for the most economical conductor sizes. It will be noted that the unit cost of oil filled increases more rapidly than that of solid cable systems with decrease in voltage rating. At 38 kv the costs are about the same. Below 38 kv solid-type cable is more economical in average cases.

The reason for this is that the reservoirs, stop joints, and other accessories used with oil-filled cable are relatively more costly than those used with solid cable. Total accessory cost decreases much more slowly than total cable cost with decrease in voltage rating, conductor sizes remaining the same. Also, total cable cost for oil filled, while considerably less at higher voltages, due to smaller diameter and lesser material, decreases more slowly than solid-cable cost as voltage rating decreases. Figure 1 explains why solid-type cable is still more commonly used below 38-kv rating.

Gas-Pressure Cable

Experience with oil-filled cable has demonstrated in a striking way the benefits of maintaining positive pressure during shipment, installation, and service. In attempting to develop newer and better cable systems these basic principles should be followed.

Maintenance of pressure requires a pressure medium. There are only two well recognized pressure mediums, liquid (oil) and gas. The development field utilizing oil as a pressure medium is now covered and is best exemplified by the standard low-pressure oil-filled cable system. The use of gas as a pressure medium leaves a more versatile field of exploration. In the way of simplicity, at least, it is attractive. There are no hydrostatic head pressures to deal with and the accessory designs are simple, at least for low pressure systems. Installation and maintenance are also simplified.

Soon after entering this field of development we concluded that a dry, inert gas in direct contact with and saturating the impregnated-paper insulation gave the best assurance of maintaining pressure through the whole cable cross section with minimum time lag during temperature and pressure variations. It was further concluded that longitudinal gas feed channels in the cable would facilitate control of uniform gas pressure along the cable length in service; in fact, were necessary fully to accomplish this purpose. The feed channels fulfill another important function in providing surplus gas at any point of accidental leakage. Without longitudinal feed channels that will readily convey surplus gas, pressure would be rapidly lost at such leakage points and cause failure.

The tests and development work leading up to present conclusions will be described in detail but first it will be explained why, at least for the time being, we are concentrating on low-gas-pressure systems of moderate voltage rating, and consider this the most promising field of development.

Figure 2 shows the voltage at which ionization starts as a function of gas pressure. The curve is a representative average of all the tests on single- and three-conductor gas-pressure cable that will be described, using nitrogen gas as a pressure medium. The measurements were made after load-cycle drainage of compound.

The voltage at which ionization started in these gas-filled voids is shown in figure 2. The drooping shape of the curve is significant and important. Fifteen pounds pressure comes within safe working limits for ordinary single lead sheath. The steepest part of the curve in figure 2, and a good part of the gain in ionization voltage, occurs in this relatively low pressure range. The ionization voltage at 15 pounds pressure is 80 volts per mil, which is the lower limit of working stress for oil-filled cable.

Forty pounds pressure comes within safe working limits for ordinary double reinforced sheath, as proved by some years of experience with oil-filled cable. This is the next steepest part of the curve and the ionization voltage at 40 pounds pressure is 112 volts per mil. This gain in voltage stress is not obtained, however, without increased cost. For average cable sizes double reinforced sheath increases cable cost from 10 to 15 per cent as compared with single sheath. This can be justified only when balanced by an equivalent saving in insulation cost (reduced thickness). Actually, it is not completely justified then, because years of experience with oil-filled cable has established the fact that single sheath at 15 pounds pressure has given a better service record than double reinforced sheath at 40 pounds pressure.

As a first stage in the practical development of gas pressure cable it is our intention to establish the ultimate safe operating voltage for cable with single lead sheath and 15 pounds pressure and resort to reinforced sheath with 30 to 40 pounds pressure only for higher voltages. At just what voltage this can be justified will depend upon field experience. We do not yet know ultimate safe working voltage stresses. At the present writing, and judging from laboratory tests alone, single-sheath cable at 10 to 15 pounds pressure will have an ultimate working voltage stress (average) of about 75 volts per mil, and reinforced sheath cable at 30 to 40 pounds pressure about 100 volts per mil. If this is proved by field experience then, economically, single-sheath cable will have a maximum operating voltage of about 40 kv. This voltage range happens to fill the economic gap caused by the relatively high cost of oil-filled cable below 38 kv as shown in figure 1. We believe that the greatest field of usefulness for gas-pressure cable is in filling this gap.

High-Gas-Pressure Cable

Referring again to figure 2, the ionization voltage at 200 pounds pressure is 162 volts per mil. This is by no means a sufficient gain in voltage stress, as compared with the 40-pound results, to warrant a fivefold increase in gas pressure, with the rather serious complications it introduces.

With few exceptions, important high-voltage cable lines in this country are installed in duct systems, mostly under paved city streets. Any cable used must be flexible so that it can be drawn from shipping reels into these ducts and trained around the manhole walls to form expansion bends and facilitate racking of joints. Gas-pressure cable with single sheath at 15 pounds and ordinary double-reinforced sheath at 40 pounds is readily adapted to these requirements. We are only concerned with the radial stresses on the sheath. Longitudinal stresses are of no great importance. Ordinary lead plumbing wipes are used and there is no tendency for the cable to move or "whip" due to excessive internal pressure. All of this has been demonstrated by experience with oil-filled cable.

At 200 pounds pressure a very elaborate type of sheath reinforcement would have to be used, taking care of both radial and longitudinal strains. All plumbing wipes would also have to be reinforced radially and longitudinally. Curvature in the duct run and expansion bends in the manholes would have to be dispensed with or the cable given rigid support with sufficient bearing surface to avoid crushing.

This special high-pressure reinforcement would add at least 20 to 25 per cent to cable cost as compared with ordinary single-sheath low-pressure oil-filled cable. This is, without question, more than the difference in cost of oil-filled cable accessories and high-pressure gas cable accessories.

On the above basis it is reasonable to conclude that the over-all installed cost of a 200-pound gas-pressure system for 138-kv service in underground ducts would exceed the cost of an equivalent low-pressure oil-filled system. In view of the experimental nature of reinforced sheath operation at 200 pounds pressure and the difficulties of maintenance and repair work, we feel it advisable first to determine the practical possibilities of low-pressure gas systems for moderate voltage use and leave the supervoltage field to oil-filled cable for a while longer.

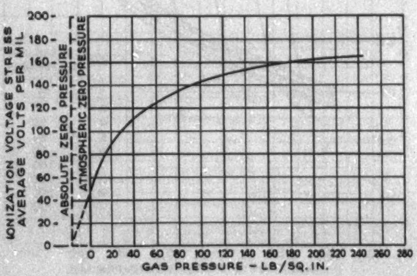

Figure 2. Voltage stress on solid insulation at which ionization starts as a function of gas pressure in voids of gas-pressure cable

Laboratory Tests

General Conditions

The series of laboratory tests on gas-pressure cable, started about five years ago, have included different types of impregnating compound, different densities of paper tape, various gases, and variations in method of constructing and treating the cable. Both single-conductor and three-conductor cable have been tested.

Single-Conductor Cable

All single-conductor cable used in these tests had either 350,000-circular-mil or 400,000-circular-mil stranded conductor with one-half-inch hollow core. The insulation was 0.300-inch impregnated paper tape, and over this a one-eighth-inch lead sheath. The cable was given a very complete vacuum drying and impregnation treatment and the test lengths (50 feet) were end sealed and sent to the laboratory without drainage, other than blowing the core free of compound and filling with gas to ten pounds pressure.

In the laboratory the lengths were drawn into 2.0-inch steel pipe and special high-pressure porcelain test terminals assembled at each end. The hollow core of the cable and the space in the steel pipe were interconnected through the terminals. Both were evacuated and filled with gas to required pressure. An insulated current transformer furnished load heating current to the conductor. The lengths were subjected to 60-degree-centigrade load cycles and simultaneous gas-pressure cycles from atmospheric to 100 and 200 pounds for a number of days until a capillary balance was reached and drainage of compound from the core ceased. Power factor versus voltage curves were then measured in pressure steps from atmospheric to 200 pounds at room temperature and 60 degrees centigrade. After this the lengths were subjected to load-cycle overvoltage endurance with periodic measurement of power factor and ionization. Two to three complete load cycles per day were applied from room temperature (25 to 30 degrees centigrade) to 60 and 65 degrees centigrade.

Three-Conductor Cable

The three-conductor test lengths were 75 feet long with 350,000-circular-mil sector conductors and 0.200-inch paper tape, shielded. All filler material was omitted from filler spaces, which acted as gas feed channels. The first test length had no support in the filler spaces. Remaining lengths had steel spiral support in the filler spaces similar to that used with oil-filled cable. The insulated and shielded conductors were cabled together with a metal-tape binder and a one-eighth-inch lead sheath was applied after treatment.

After vacuum drying and impregnation in the usual manner the lengths were left in the treating tank and drained of surplus compound in an atmosphere of the same gas later used as a pressure medium on test. Drainage was at 60 to 70 degrees centigrade for a considerable period of time. Afterward the lengths were leaded in an atmosphere of the same gas, end sealed, and sent to the laboratory under ten pounds pressure.

The lengths were subjected to short-time ionization tests up to 30 pounds pressure and long-time load-cycle overvoltage tests up to 15 pounds pressure. It was consequently not necessary to install them in steel pipe and all tests were made with bare lead sheath exposed. Ordinary porcelain pothead terminals were assembled in inverted position to facilitate drainage of compound from the cable, which, in effect, was arranged as an inverted U. The three conductors were connected in series for application of load-cycle current. In all other respects test procedure was as outlined for single-conductor cable, except that the load cycles were carried to 70 and 80 degrees centigrade.

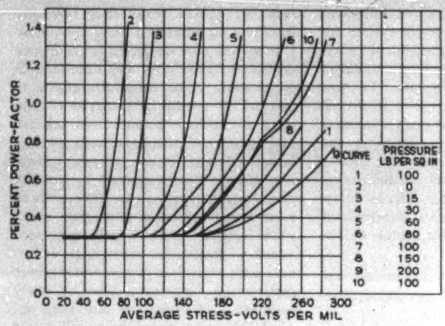

Figure 3. Test length number 5—ionization curves after prolonged load-cycle bleeding—measured at 25 degrees centigrade

Test Length Number 1. Single conductor, impregnated with relatively thin oil similar to that used in oil-filled cable and having a viscosity of 37 Saybolt at 100 degrees centigrade. Pressure medium, CO_2 gas.

Due to the thin oil and the large volumetric absorption of CO_2, the bleeding under load and pressure cycles was excessive, approximately 33 per cent of the original oil in the paper being expelled. Ionization was also poor, beginning at about 35 volts per mil atmospheric pressure. The length was submitted to a 65-degree-centigrade load-cycle endurance run at 300 volts per mil and 200 pounds pressure. Failure occurred in 22 hours.

Test Length Number 2. All conditions similar to those for length number 1 with the exception that the cable was impregnated with a heavier all-mineral-oil compound similar to that used in solid-type cable and having a viscosity of 95 Saybolt at 100 degrees centigrade

The expulsion of compound was improved, being about 25 per cent, and ionization was also improved. In spite of this, failure occurred in $7^{1}/_{2}$ hours on load-cycle endurance at 300 volts per mil, 200 pounds pressure.

Test Length Number 3. This length was a duplicate of length number 1, the only difference being that nitrogen gas was used instead of carbon dioxide. On load-cycle endurance at 300 volts per mil, 200 pounds pressure, failure occurred in 80 hours. This was an improvement compared with length number 1 but the thin compound still bled too freely.

Test Length Number 4. This length was similar to length number 2 with the exception that nitrogen gas was substituted for carbon dioxide. This gave decided improvement in both compound expulsion and ionization. On the same 65-degree-centigrade load-cycle endurance as length number 3 failure occurred after 801 hours.

Test Length Number 5. Exactly similar to length number 4 and all test conditions the same with the exception that the 60-degree-centigrade load-cycle endurance run was made at low pressure (15 and 10 pounds) and at voltage stresses just above the ionization point (80 and 100 volts per mil). The load-cycle endurance run was continued for a long period of time, more than two years.

Figure 3 gives 25-degree-centigrade ionization curves at different pressures after the length had been subjected to a large number of load cycles and alternate applications of vacuum and 200 pounds nitrogen pressure before the endurance run. Compound expulsion had practically ceased by the time figure 3 was obtained. The corresponding 60-degree-centigrade power factor curves are given in figure 4. It will be noted that ionization starts at a higher voltage at 60 degrees centigrade, as would be expected. It should also be noted that while the solid loss power factor at 25 degrees centigrade is practically horizontal before ionization starts the corresponding 60-degree-centigrade curves in figure 4 show a pronounced up slope of solid loss power factor as voltage increases. This is inherent in this type of cable and, since it obscures true ionization loss, only the room temperature results will be used as an indication of ionization in the load-cycle endurance charts that will be presented for the different test lengths. As a matter of fact, even the room-temperature measurements show this positive coefficient of solid loss power factor in most of the test lengths and this must be allowed for.

Figure 5 gives a chart of the 60-degree-centigrade load-cycle endurance run, 105 days at 15 pounds and 80 volts per mil, 255 days at 10 pounds and 80 volts per mil, and 470 days at 10 pounds and 100 volts per mil. The total length of the load-cycle voltage test was 830 days. At the end of that time the cable had stabilized so test was discontinued and the length carefully dissected for study of *X* wax.

Referring to figure 5, after the first 9 days at 15 pounds, 80 volts per mil, ionization practically disappeared and remained negligible until 100 volts per mil was applied at the end of 360 days. At the beginning ionization was more pronounced, as would be expected, but again decreased and practically disappeared after an additional 300 days. The 60-degree solid loss gradually increased from 1.0 per cent to approximately 2.0 per cent. It then decreased

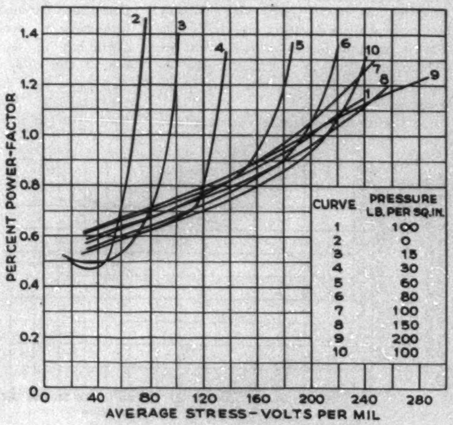

Figure 4. Test length number 5—ionization curves at same time and in same order as those in figure 3—measured at 60 degrees centigrade

and at the end of the run (830 days) had returned to the initial 1.0 per cent value. The "saw tooth" fluctuations in the power factor plots are typical of all tests made and are due to fluctuations in copper temperature. It is difficult to hold exactly uniform copper temperature during these measurements. It will be noted that the power factor and temperature fluctuations follow each other closely, particularly at 60 degrees centigrade where power factor is more sensitive to temperature changes.

Wax distribution was uniform throughout the insulation thickness and occurred only in the butt spaces between paper tape edges. There were no signs of tree-design overstressing or cumulative ionization trouble, only the limited *X* wax formation in the butt spaces, previously described.

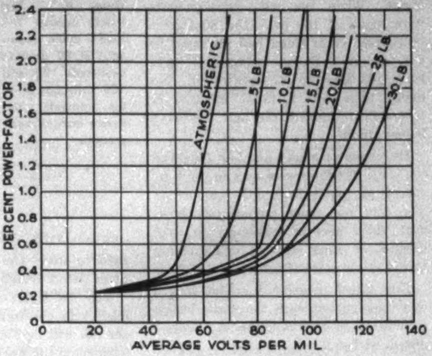

Figure 6. Test length number 6—Ionization curves after load - cycle bleeding—measured at 24 degrees centigrade

Test Length Number 6. Three-conductor cable, impregnated with same compound as length number 5 and also using nitrogen as a pressure medium.

Power factor curves (24 degrees) after stabilized load-cycle bleeding are given in figure 6. The 24-degree-centigrade solid loss power factor has a distinct upward slope with increased voltage stress. This not only obscures the starting point of ionization but must be allowed for in interpreting the room temperature ionization measurements on the load-cycle endurance run chart given in figure 7.

This chart shows that length number 6 was subjected to 80-degree-centigrade load-cycle endurance for 147 days at 75 volts per mil and 10 pounds pressure. Ionization was noticeable at the start but had practically disappeared at the end of 70 days (making due allowance for the upward slope of solid loss in figure 6). At the end of 147 days the cable was stabilized, so voltage stress was increased to 85 volts per mil. Again ionization appeared and gradually ceased after 49 days further testing. After that the cable was completely stable for a total time of 400 days. The same characteristic self-healing and self-extinguishing effect of ionization found with length number 5 is present in length number 6 after each endurance voltage step. The high-temperature solid loss also behaves in the same way, increasing some and then gradually decreasing to its initial value.

After 400 days the test was discontinued and the cable subjected to vacuum treatment to remove all nitrogen gas. It was then filled with Freon gas to determine whether the high dielectric strength of this gas (more than twice that of nitrogen) would prove beneficial. Power-factor curves at different pressures showed that Freon increased ionization voltage approximately 50 per cent.

The 80-degree-centigrade load-cycle endurance run was then continued at 85 volts per mil, 10 pounds Freon gas pressure. Solid loss immediately began to increase and after 24 days the test was discontinued. At that time the 80-degree-centigrade power factor had increased to 7.0 per cent, more than double its original value. We are not sure of the real cause of this increase. It may have been due to chloride impurities in the Freon or it may have been caused by dissociation of Freon from ionization discharge. We intend to study the possibilities of Freon and other high-dielectric-strength gases further. In the meantime we will use nitrogen as the best of the available common gases.

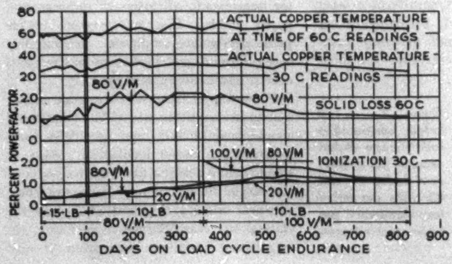

Figure 5. Test length number 5—load - cycle endurance at 80 and 100 volts per mil—15 and 10 pounds per square inch nitrogen pressure

The Freon test was stopped before the cable was seriously damaged and it was then dissected for study of *X* wax formation. Again wax was found only in the butt spaces, more or less uniformly distributed through the thickness, and without other signs of overstressing.

Test Length Number 7. This three-conductor length was exactly similar to length number 6 with the one exception that two layers of manila paper tape were bound over the metal shielding tape on each insulated conductor.

The power factor versus voltage curves at different pressures were similar to those in figure 6, but the upward slope of the solid loss power factor was more pronounced and calls for a greater allowance in the load-cycle endurance chart given in figure 8. Length number 7 was subjected to an 80-degree-centigrade ten-pound load-cycle endurance test at 85 volts per mil for 400 days. It will be noted in figure 8 that ionization during this time was more pronounced than in length number 6, figure 7, and required a longer time to heal itself. At the end of 300 days ionization had practically disappeared and the 80-degree-centigrade solid loss had returned to its initial value. Test voltage was then increased to 100 volts per mil and ionization became pronounced. Both ionization and solid loss showed a tendency to gradually decrease but a stable condition was not reached. At the end of 110 days at 100 volts per mil solid loss began to increase. When the last measurements were made at 160 days the 80-degree-centigrade power factor had increased to 2.9 per cent. Failure occurred 7 days later (167 days, or 567 days total). No doubt there was a rapid increase in power factor during the last few days before failure.

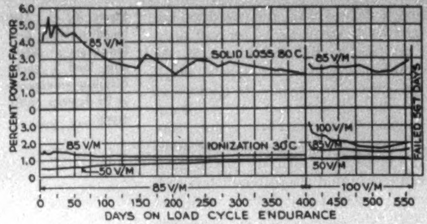

Figure 8. Test length number 7—load - cycle endurance at 80 degrees centigrade, 85 and 100 volts per mil—ten pounds per square inch nitrogen pressure

Dissection of length number 7 showed the same distribution of wax in the butt spaces as found in length number 6. The wax was more pronounced, however, there were no carbonization, tree designs, or other signs of overstressing except at the point of failure.

Test Length Number 8. This length was impregnated with the same heavy mineral oil as lengths numbers 6 and 7, mixed with 1.5 per cent of an experimental, viscous compound to increase viscosity. The viscosity of the mixture was 350 Saybolt at 100 degrees centigrade. While the viscosity was increased there did not appear to be an equal increase in tackiness and film tension, the compound bleeding at high temperature being about the same as previously.

The 80-degree-centigrade ten-pound load-cycle endurance-run data at 85 volts per mil will be found in figure 9. The run has continued for 220 days to date and the length is still on test. As far as it has gone, the endurance run has shown about the same results as obtained with length number 6.

This length, alone, would not indicate any real benefit from increased viscosity of impregnating compound. We attribute this mostly to a lack of film tension in this particular mixture. Good film tension is, of course, necessary to hold the compound in place by capillary attraction.

Test Length Number 9. Similar to length number 8 except that the heavy oil compound contained 20 per cent rosin. The mixture had a viscosity of 200 Saybolt at 100 degrees centigrade. This mixture had more tackiness and better film tension than that in length number 8.

The rosin used was not the modern water-white type but was an older and less highly refined grade. It was desired to study the stability of this older grade before trying the newer.

The 80-degree-centigrade ten-pound load-cycle endurance chart at 85 volts per mil will be found in figure 10. Allowing for the upward slope of the solid loss power factor the ionization from the start has been quite small and seems to confirm the opinion that an impregnating compound of high viscosity and high film tension improves ionization characteristics in gas pressure cable. The solid loss, however, began to increase rapidly after 37 days and, to date (170 days), appears to be still increasing. Test will be continued on this length.

Hot spots were found along the cable soon after the increase in dielectric loss was observed but the cable has not yet shown signs of final failure. The instability of this length is undoubtedly due to the grade of rosin used. It is recognized as lacking the stability of highly refined, water-white rosin.

Test Lengths Numbers 10 and 11. These two lengths of three-conductor cable are similar to length number 9 with following exceptions. Length number 10 has 20 per cent highly refined water-white rosin mixed with heavy oil. The viscosity of the mixture is about the same as that for length number 9. Length number 11 is impregnated with an experimental hydrogenated oil having a viscosity of 1,000 Saybolt at 100 degrees centigrade.

These two lengths are now being made ready for load-cycle endurance. It will be necessary to report results at a later date. We are planning to include other test lengths using high viscosity compounds and fully determine the best viscosity for gas-pressure cable of this type.

Summary of Test Conclusions

The series of tests already described lead to the following observations and conclusions.

Paper Tape

It is important that the paper tape be applied smoothly and uniformly with the smallest possible space between butt edges and freedom from wrinkling. Impregnating compound is held in the insulation by capillary attraction. This means that the largest spaces (voids) are drained first, leaving only a film of compound on the walls. One main object is to keep such voids to minimum size since ionization voltage is a function of void size at all pressures.

The thinnest paper tape allowed by practical limitations (five to six mils) should be used. Tearing of tape and wrinkling are the chief limitations in this respect. The need of freedom from impurities goes without saying.

Paper density over the usual commercial range from 0.8 to 1.2 seems to affect characteristics only slightly. If anything, the lower density paper appears to give a little better results but this is not fully demonstrated.

Impregnating Compound

The primary requirements, such as high resistivity, oxidation stability, etc., are common to all types of impregnated paper cable. The gas-pressure cable puts particular emphasis on stability and minimum gas evolution in the presence of ionization discharge.

Film tension and viscosity are other important requirements. The compound should have tenacity and not bleed readily from the paper insulation. Also, it should leave a thick film on the walls of the larger voids, thereby reducing their size and offering a source of *X* wax for closing up these larger voids and extinguishing incipient ionization locally

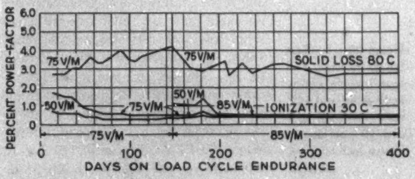

Figure 7. Test length number 6—load - cycle endurance at 80 degrees centigrade, 75 and 85 volts per mil—ten pounds per square inch nitrogen pressure

Table I. Insulation Thickness and Average Voltage Stress for Solid, Oil-Filled, and Low-Gas-Pressure Cable

Rating (Kilovolts)	Insulation Thickness (Mils)			Average Voltage Stress (Volts per Mil)		
	Solid	Gas	Oil-Filled,	Solid	Gas	Oil-Filled
15	203	157	110	42.7	55.2	78.8
23	266	206	145	50.0	64.7	92.0
27	297	229	160	52.6	68.2	97.7
34.5	375	283	190	53.2	70.5	105.0
40	422	316	210	54.7	73.1	110.0

before it begins to spread and become cumulative.

The compound should not be of such high viscosity as to offer serious difficulties in obtaining uniform initial impregnation, or in draining the cable and gas channels sufficiently to prevent slug stoppages in service. Also, the compound should allow ready transmission of gas pressure through the insulation wall. Optimum compound requirements for gas-pressure cable are not yet fully determined.

Gas Pressure Medium

Of the readily available commercial gases it has been found that nitrogen is the most desirable. It has good dielectric strength, does not affect the solid insulation chemically, and its absorption by the impregnating compound is small. The present sources of supply specify a maximum oxygen content of 0.3 per cent. We are going to try to reduce this oxygen content. It has undoubtedly increased solid dielectric loss to a more or less extent in the tests that have been reported, by oxidation action during load-cycle heating. Other special gases of higher dielectric strength, similar to Freon are being given further study.

Ionization Stability

The development of low-gas-pressure cable was prompted by the observed behavior of ordinary solid-type cable when exposed to ionization discharge. There is a tendency toward cumulative ionization deterioration in solid-type cable. Observation shows this cumulative action takes place during periods of low temperature and negative pressure in void spaces. Maintenance of positive pressure, even if relatively low, will counteract this cumulative tendency markedly. A study of the low-gas-pressure cable bears this out. Initial ionization behaves in just the opposite way to that in solid-type cable. Instead of being cumulative in its action it is self-extinguishing and self-healing without permanent increase in solid loss.

This self-extinguishing effect is produced by formation of *X* wax which partially fills up the larger voids. Apparently, positive-pressure ionization, at voltage stresses below what might be termed a critical voltage, is not accompanied by the same harmful by-products that result from negative-pressure discharge, including gas evolution (mostly hydrogen) and conducting impurities (water and acids).

No doubt, the difference between ionization under negative and positive pressures is, in part, one of degree. With gas-pressure cable the size of voids and the pressure in these voids are under control. The critical voltage stress can accordingly be more safely approached. With solid type cable, however, neither the size nor pressure of voids is known or under control in all cases and there is always the possiblility of overstressing beyond the critical limit, which is not only lower than that of gas-pressure cable but variable and indeterminate.

Thermal Resistance

Measurements made after a year or more of 80-degree-centigrade load-cycle endurance give an increase in thermal resistance of the impregnated paper of from 10 to 15 per cent. This will have no practical effect on current-carrying capacity and it is proposed to give gas-pressure cable the same load rating as solid-type cable.

The possibility of using dry paper instead of impregnated paper was studied at the beginning and discarded for a number of reasons, one of these being the material increase in thermal resistance. The ready absorption of moisture upon exposure, the special precautions against this exposure, and the larger size of voids, offered other obstacles to the use of dry paper.

Critical Voltage Stress

It is proposed to operate low-gas-pressure cable at a minimum no-load pressure of ten pounds. The tests were accordingly made mostly at this pressure. Judging from these tests the critical average voltage stress is close to 100 volts per mil. We accordingly believe that a working voltage stress of no more than 75 volts per mil will give sufficient factor of safety, in view of the close control that can be obtained with cable of this type.

Economic Working Voltage Stress

Economically, the low-gas-pressure cable is very similar to solid-type cable. The accessories are equally simple and of practically the same size and cost. The gas channels, the drainage operations in the factory, and the various gas operations in both factory and field add a few per cent to cable cost. On the other hand, the reduced thickness of insulation and smaller diameter offer practically the same saving. Additional experience will be necessary in determining exact cost figures but, approximately, the curve for solid cable in figure 1 holds also for low-gas-pressure cable. On this basis, it can be seen that gas-pressure cable fits

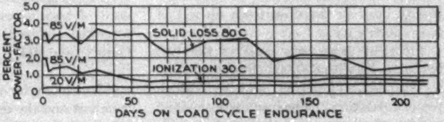

Figure 9. Test length number 8—load-cycle endurance at 80 degrees centigrade, 85 volts per mil—ten pounds per square inch nitrogen pressure

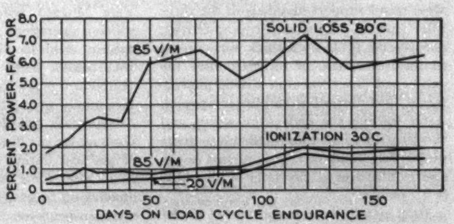

Figure 10. Test length number 9—load-cycle endurance at 80 degrees centigrade, 85 volts per mil—ten pounds per square inch nitrogen pressure

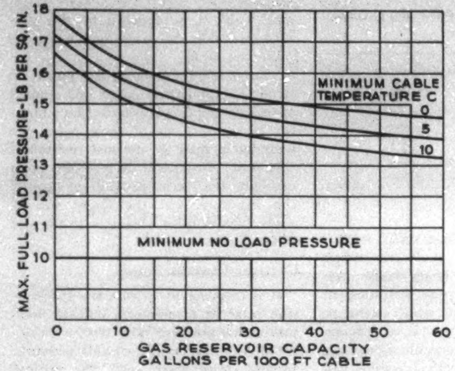

Figure 11. Fifteen-kv three-conductor gas-pressure cable system showing working pressure range as a function of auxiliary gas reservoir capacity, maximum copper temperature 81.5 degrees centigrade

into the economic picture best at 40 kv and below.

In determining insulation thickness for low-gas-pressure cable it is felt that a logical choice, as a starting point at least, is half way between the thicknesses for solid and oil-filled cable. Table I gives comparative thicknesses and average voltage stresses from 15 kv to 40 kv.

Field Installations

During the past summer (1938) the Consolidated Edison Company of New York and the Yonkers Electric Light and Power Company have ordered two trial installations of 15-kv low-gas-pressure cable. The first, consisting of a single three-conductor cable circuit 10,000 feet in length, was installed in Yonkers during August 1938, and is now in service. The second, consisting of two parallel circuits of three-conductor cable, each 5,000 feet in length, was installed a month later in the downtown district of New York City.

Identical cable was used in the two installations with 800,000-circular-mil sector-shaped conductors and 0.150-inch paper insulation. The insulated, shielded conductors were cabled together with steel spiral support channels in the three filler spaces. A single copper-bearing lead sheath 0.141 inch thick was applied in an atmosphere of nitrogen after the cable had been vacuum dried, impregnated, and drained of surplus compound. After leading the ends were sealed and the lengths shipped and pulled into ducts under ten pounds nitrogen pressure (average). The over-all diameter of this cable is 2.7 inches.

Both installations are paralleled in the same duct banks by solid-type cable of the same conductor size and voltage rating, also, the same current capacity rating and maximum copper temperature of 81.5 degrees centigrade. The Yonkers circuit has porcelain potheads at each end. The downtown circuits are spliced to solid-type cable at each end with stop joints at these points for segregation. No stop joints are used in either installation for sectionalizing, the gas feed channels having a free run from end to end.

These gas feed channels in each reel length were "blown out" with nitrogen gas before shipment and just before splicing in the field, to make sure no compound slugs were left. A few lengths showed a small amount of surplus compound in the feed channels. The majority of lengths were entirely free of slugs. The temperature and time of drainage before leading will be increased in the future until the correct values are learned by experience. It is expected that the need of blowing out slugs of compound from the gas feed channels, both in the factory and field, can be eliminated in this way.

When ready for splicing the cable ends were cut and the gas pressure reduced to two pounds. The exposed cable ends were plugged temporarily during the splicing operations with strips of sponge rubber, held in place and compressed with flexible band steel clamps. These temporary plugs were removed just before finally wiping the joint casing to the cable sheath and this last operation was done under gas flow. The total amount of gas lost during these operations was small after some experience was gained, amounting to less than 100 cubic feet of nitrogen per joint.

Because of the limited capacity of the filler-space feed channels and the desire to keep the gas pressure within a narrow working range from no load in the winter to full load in the summer, the Yonkers installation has galvanized steel tanks connected in the manholes to the cable joints at intervals of approximately 1,000 feet. These tanks are each of 53 gallons capacity and are, of course, filled with nitrogen gas to the same pressure as that in the cable. These tanks are simpler and more economical than the oil reservoirs frequently used with solid-type cable.

Figure 11 shows the pressure range of this 15-kv cable as a function of gas reservoir capacity. The curves are based on a minimum no-load winter pressure of ten pounds per square inch and show the maximum full-load summer pressure for different values of reservoir capacity. Three different winter ambient temperatures, zero, five, and ten degrees centigrade are assumed to illustrate their effects on cable pressure. With five-degree-centigrade winter temperature figure 11 shows the total working pressure range of the Yonkers installation to be from 10 pounds to 14 pounds.

For purposes of experiment no reservoirs were used in the downtown installation. Figure 11 gives a pressure range for this condition of seven pounds. To avoid stretching the lead sheath the maximum full load pressure is set at 15 pounds. This gives a total working pressure range from 8 to 15 pounds.

Accessories

The accessories used were much the same as those for solid cable of the same voltage rating.

Normal Joints

Similar to solid-cable joints with varnished-cloth tape reinforcement wrappings and metal shielding tape the full length of joint. The joints were filled with gas instead of compound and lead sleeve casings are seven inches diameter, offset toward the bottom to act as sumps for any surplus compound that might be drained from the gas channels. The casings are reinforced to withstand 15 pounds steady pressure.

In the future, on long lines where segregation for repair work is desired either stop joints will be used at intervals or semistops will be incorporated in some of the normal joints.

Stop Joints

Of the Herkolite core type used also with solid and oil-filled cable. The design has been described in a previous paper,[4] the only difference being that gas filling was substituted for oil filling and the cork-gasket semistops at each end omitted.

Potheads

Standard three-conductor porcelain potheads were used. A special cork-gasket semistop was incorporated in the wiping bell and the pothead above this semistop filled with petrolatum. The gas pressure below the semistop maintains the whole pothead under positive pressure.

Gas-filled potheads have worked successfully on laboratory and factory tests and will probably be used in the future.

Gas-Pressure Relays

Each installation has gas-pressure relays at the terminal ends, set to alarm at pressures a little above and below the normal working range. These pressure relays are of the bellows type similar in principle to those used with oil-filled cable but smaller and of lesser cost.

Profile

The profile of these first two installations is reasonably flat but, obviously, profile is of no consequence with gas-pressure cable. With solid and oil-filled cable profile must be taken fully into account on steep grades and at vertical risers. With gas-filled cable any height of vertical riser within the limits of mechanical strength can be safely used.

The gas-filled cable would appear to solve a real problem for use as risers in tall buildings and in localities where steep hills are encountered.

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Discussion

L. I. Komives (The Detroit Edison Company, Detroit, Mich.): We are glad to see a type of cable which in our estimation is designed primarily to relieve the lead sheath. It may seem that this aim is accomplished at the expense of the quality of the insulation; however, this may turn out to be merely an assumption. In any case, the ratio of the factor of safety of the insulation and of the lead sheath will be brought closer together by the means outlined by the author.

It seems that the remark in the paper that "profile is of no consequence with gas-pressure cable" would have to be proved in field installation. If this statement will be proved by successful field installations, we are looking forward to the use of this new type of cable for vertical risers.

In calculating costs, it should be borne in mind that most manholes are made for joints of more or less standard sizes. The author mentions joints seven inches in diameter with an offset toward the bottom which will certainly increase the size. Such large joints may necessitate the enlarging of present manholes.

When all accessories are taken into consideration, the low-gas-pressure cable differs from the oil-filled cable only in the use of accessories for gas supply instead of oil supply. From previous experience, we know that the oil-filled cable is a rather expensive installation. For this reason, until cost data are available, we seriously doubt the economy of this type of cable as against the standard solid-type cable for voltages below the 38 kv as mentioned by the author.

Robert J. Wiseman (The Okonite-Callender Cable Company, Passaic, N. J.): I have read with great interest Mr. Shanklin's paper as he describes the trials, tribulations, and hopes of an investigator of a cable design which is not too well known in commercial practice.

Cables in which gas is used as a means for maintaining the quality of the insulation are not new and we find many patents describing specific designs. Almost all of them pertain to the use of gas under high pressure. For some unknown reason, mostly everyone wants to operate the cable around 200 pounds per square inch internal pressure, yet it is possible to get commercially very good stability as low as 75 pounds per square inch pressure and we do not have the problems of leakage that exist at high pressures.

I am not so sure that solid-type cables have about reached their ultimate development. As I view it, we can improve solid-type cables if we loosen up slightly on the tightness of our sheaths and design our joints to permit free flow of oil between the joint and cable. This means the elimination of the varnished-cambric wrapped joint and the use of tubes as was done years ago with a fluid oil in the joint instead of hard or solid compounds. Tests we have conducted in our laboratories have shown an increase in life of a cable. A simple solution to the problem, yet it is to be regretted that we are wedded too much through custom to the use of tightly wrapped and solidly filled compound joints. Granted that the joint may not have as high a breakdown voltage as a tightly wrapped joint, it is more than ample for operation and at the same time, we reduce the pressures which a cable will be subject to and also prevent the cable dropping to a high vacuum when cooling.

It is true that the low-pressure oil-filled cable has contributed to the success of high-voltage cables and it is a question of economics whether it should be used or solid-type cable. The high-oil-pressure cables are equally able to meet the needs of high-voltage operation and are also dependent on the economics of the installation. The jointing is not as difficult as for the low-pressure oil-filled cable as the oil is of a higher viscosity and will not bleed from the cable during the making of a joint. It is true that the reinforcing is more expensive, but with the higher voltage breakdown which is obtained on the high pressure, it is a question of what factor of safety is desired and the price one wishes to pay for it.

The high-gas-pressure cable has been proved successful in Europe. We have the Beaver design which is the same design as Mr. Shanklin gets by drainage, as Beaver uses preimpregnated paper and his method of manufacture eliminates the filling in of the paper gaps and between layers with oil. Beaver uses 200 pounds per square inch N_2 gas pressure. The Hochstadter gas-pressure cable has a lead sheath between the insulation and the gas and the latter does not function as a part of the insulation. The Callender's Cable and Construction Company have two designs of high-gas-pressure cable, one uses impregnated paper and over the paper is wrapped varnished silk tapes before the shielding tape is applied. A loose lead sheath permits the filling of the space between the cable and the sheath with N_2 gas at 200 pounds per square inch pressure. The varnished silk tapes prevent the oil from draining from the cable and also the absorption of gas by the oil, thereby maintaining stability of the insulation. This type of cable has been under successful test for two years at Arnhem, Holland, and is the only design of high-gas-pressure cable with impregnated paper that has been successful. It has only been equalled by a low-pressure oil-filled cable which uses a heavier wall of insulation and a lower maximum stress. In addition, Callender's have been testing a similar type of impregnated gas-pressure cable in their own research laboratory for several years, first at 200 kv with the same current required at Arnhem, later they raised the voltage to 300 kv and to date the same cable has withstood 65 load cycles at 85 degrees centigrade on the copper. We also have made and tested an impregnated gas-pressure cable in our laboratories and have proved in its stability with tests on a 500,000-circular-mil round conductor, 0.450-inch wall, starting at 95 kv (312 volts per mil maximum electric stress) for 1,482 hours and 133 kv (438 volts per mil maximum electric stress) for 549 hours. The cable did not show any ionization between 35 volts per mil and 170 volts per mil both hot and cold for the last 20 days of test. The power factor was about 0.48 per cent at 70 degrees centigrade and 0.53 per cent at room temperature for the last 20 days of test.

I believe the reason for the failure of the cable which Mr. Shanklin tested at 200 pounds pressure was due to the absorption of gas into the oil with the resulting lowering of the dielectric strength and the increase in power factor. During our studies eight years ago on our Oilostatic cable system we used gas as the pressure medium and a gas cylinder was connected directly to the potheads at 200 pounds pressure. Every few months we noted an increase in the power factor of the pothead which required draining of oil and refilling. The drained oil was milky in appearance and had about 20 per cent gas content. We introduced an intermediate oil reservoir with a diaphragm between the gas cylinder and the pothead which eliminated the trouble. The important thing is to keep the gas from contact with the oil that is under stress. The success of the Callender's impregnated gas-pressure cable comes from the varnished silk between the insulation and the gas.

Mr. Shanklin is using for the low-gas-pressure cable, comparatively low electric stresses but it is the maximum stress at the conductor that is important. According to figure 2 at 15 pounds per square inch gas pressure, ionization starts at 70 volts per mil and to be safe, one should take not over 80 per cent of this or 55 volts per mil. This does not agree with the statement under "Critical Voltage Stress" in which Mr. Shanklin proposes a working stress of 75 volts per mil. In other words, it is necessary to increase his wall thickness. Although the mixing of gas and oil is not as serious as if high gas pressure were used, still with time as expressed in years it seems as if there will be sufficient gas absorption by the oil seriously to reduce the breakdown voltage of the cable. Perhaps today we are not as concerned about getting high-voltage breakdown of our cables as we did formerly, in which case the cable manufacturer can take advantage of it as suggested above for solid-type cables. The need of as low gas absorption as possible is shown by the different results which Mr. Shanklin obtained with CO_2 gas and N_2 gas. The former is easily absorbed into oil, the latter with great difficulty but also hard to remove once it is absorbed.

Mr. Shanklin very nicely describes the problems in manufacture of a low-gas-pressure cable where the oil is permitted to drain. The size of voids is the controlling factor in stability and, therefore, careful taping is essential.

I am surprised to note that the thermal resistance is only 10 to 15 per cent higher than solid cables. I would expect it to be much higher. Tests on insulation subject to high gas pressure show a value of about 660 thermal ohms per centimeter cubed. Perhaps Mr. Shanklin has in mind as a reference value the 700 thermal ohms per centimeter cubed which we now use for paper cables and, therefore, actually about 800 thermal ohms per centimeter cubed. New solid cable shows as low as 450 thermal ohms per centimeter cubed but due to oil migration and drainage, with time, the 700 thermal ohms value is used in calculations. His curves of load-cycle endurance indicate a higher power factor than we are getting for solid cables and, therefore, it is questionable that the same current-carrying capacity can be used as for solid cables when we also consider the higher thermal resistance.

I am glad to note that this cable design is to be limited for the present to low voltages as it is essential that operating experience be gained to find all the sources of weakness that may be present, such as porosity of joint wipes and the ultimate absorption of gas to the saturation point. I wish to congratulate Mr. Shanklin on a most interesting and well-prepared paper on another cable design which may have its usefulness provided we are willing to accept a lowering of some of our past requirements, namely, high-voltage breakdown, low power factors, and not too high thermal resistance.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): Mr. Shanklin's paper brings out the details of a very interesting new development in cable using a semi-impregnated paper cable supplied with moderate gas pressure to increase reliability by compensation for expansion and changes in the insulation due to load cycles.

The new design is one of the many new trends in cable progress which employ, more and more, various pressure devices to improve cable operation, increase allowable stresses, and remove some objectionable characteristics developing as a result of service practice.

It promises well but being intermediate between oil-filled and gas-filled cables must possess some good features of each and also some objectionable ones. Oil-filled cables under pressure have high impulse strength to transient voltages and since the insulation is voidless no damage should be present from overvoltage up to failure point. Gas-filled cables on the other hand have somewhat lower impulse strength but overvoltages usually produce self-healing faults.

The low-gas-pressure cable is midway between these and provides a composite dielectric of three components, oil, paper, and gas. Transient voltages sufficient to ionize gas-filled voids would not necessarily be self-healing and might produce permanent damage which would later develop into trouble. We are wondering if impulse tests have been made on these cables and faults obtained examined or if high-voltage life tests have shown the effect. The paper, under "Impregnating Compound," indicates that the weakness has probably been considered by the author. We note that long-time ionization is said to make self-healing voids by the formation of X. This takes time and would not take place with high transient voltages.

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York, N. Y.): Mr. Shanklin has presented an interesting discussion of a new type of cable which we are glad to welcome in the American market. It is encouraging to see progress being made in the direction of new types of cable because it seems that this offers one of the most encouraging means for reducing the over-all cost of cable systems.

My first specific comment is to quarrel with the use of the term "negative pressure." When we are dealing with gases or liquids, such a term is quite meaningless in spite of its use in technology and it has the serious objection of possibly leading us astray when we come to analyze the underlying phenomena.

In his discussion under the heading "Ionization Stability" the author suggests that there is a "difference between ionization for negative and positive pressures." For any fixed electrical stress, there is a difference in the intensity of ionization but I am unfamiliar with any theory which will account for a difference in the action. Is ionization of any degree objectionable and if so, what determines the border line? The author suggests that there is something more than a difference in degree and it would be of great interest to all of us to know something more as to the details.

L. J. Berberich (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The results given by the author on the low-gas-pressure cable indicate a better performance under load-cycle test than one would expect. Gas pockets exist and ionization admittedly occurs just as in the solid cable with the one important difference; namely, a positive pressure is maintained in the gas pockets at all times. This naturally raises the critical voltage at which ionization begins. The success of the cable, however, appears to be in the discovery made by the author and his associates that ionization as measured by power factor change with voltage becomes negligible after the cable has been subjected to load-cycle test for a time.

This diminution of ionization with time on load-cycle test is ascribed to a self-healing process in which the area surrounding the gas pockets is sealed with the solid polymerization product formed as a result of the discharge. If this is true, an oil that produces large quantities of wax on subjection to corona discharge may be desirable. In this connection, I would like to ask if the addition of some aromatic compound, such as diphenyl, to the impregnating oil has been tried. In my work along these lines (*Industrial and Engineering Chemistry*, volume 30, 1938, page 280), it was shown that compounds of the aromatic type decrease the gas evolution from oils markedly and may increase the wax formation; the latter, however, could be observed only in a qualitative way. It appears possible that such additions to the impregnating oil may have application in this type of cable.

The authors report that when the high-dielectric-strength gas Freon was introduced into the cable, a very short life was obtained. It is questionable whether any of the high dielectric gases will be satisfactory as pressure mediums in this cable. Most of these gases are hydrocarbons which are either chlorinated or fluorinated or both. Compounds of this type are known to split off halogens under conditions of corona discharge. Since oil is also present which is an abundant source of reactive hydrogen under these conditions, the formation of strong acids is very likely. Some of these gases have hydrogen in the molecule which may provide another source for hydrogen.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): Mr. Shanklin has described an interesting solution for some of the problems involved in paper insulation of high-voltage cables, where the ionization of gaseous voids seems to be the limiting factor in the voltage rating of the cable. The philosophy of his method seems to reverse that of the cable using oil under pressure, and appears to be that if the inevitable voids in oil-filled cable establish the working limit of voltage, let us then design the cable to have only voids, but control the effect of the voids by definite regulation of the pressure of the gas in them. This method of attack has led therefore to the use of gas at relatively low pressure and simple mechanical features, beyond which pressure, the economics and other factors involved indicate diminishing returns for any further increase in pressure.

There is one point in regard to which the language should be kept clear, however. This cable uses paper insulation, with gas under pressure as the saturating medium. I would prefer to classify this cable as a *gas-filled cable*, leaving the *pressure cable* designation open to future developments of cables at much higher gas pressures. The conclusions which the author presents indicate an optimum pressure of about 40 pounds per square inch pressure for this type of cable, whereas for real high-voltage cables using gas as the major dielectric, higher pressures appear to be economically desirable to realize the full benefit of the gaseous insulation properties. Mr. Shanklin's work is valuable in its field, but there is much room for development and research along the lines of a real gas-pressure cable using the higher optimum of pressure.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): This paper is probably destined to be one of the classics of cable engineering as it is one of a very few that have appeared announcing the development of an entirely new type of cable.

Like most new inventions many close approaches have been made to it but, for one reason or another, they did not, so to speak, "go over the top." There is nothing new in a drained cable or in gas pressure or even in the combination of a drained cable with internal gas pressure. The invention, as I see, lay in the realization that a limited gas pressure would give to a drained cable a sufficiently high ionization point to permit a cable of economical dimensions to be designed for the voltages at which three-conductor solid-type cables are used. Hitherto, workers with gas pressure cable had concentrated their efforts on the higher voltage field already occupied by the oil-filled cable.

Our laboratory has worked on a drained paper cable, with interstitial gas at atmospheric pressure. With this cable, the rise of the ionization point with continued application of voltage was as definite as in Mr. Shanklin's cable. Ionization started at a maximum stress of 50 volts per mil, calculated by the ordinary logarithmic formula. The stress was then raised to 60 volts per mil at which the dielectric loss was eight times as great as that at 50 volts per mil, and continued for three months at room temperature. During this period the ionization stopped. The voltage was then raised until ionization started, and it was found that the critical stress had risen to 65 volts per mil. I do not believe that ionization ceased at 60 volts per mil, as the result of filling the interstices with cable wax, as suggested by Mr. Shanklin, as very little wax was found.

Furthermore, a nitrogen-gas-filled cable at 30 pounds per square inch absolute pressure tested for 40 heat cycles at *zero voltage* showed a steadily decreasing ionization. Whereas for the first 10 cycles the power factor at 100 volts per mil ran over three times that at 60 volts per mil (average stresses), after 40 heat cycles, there was little difference between the power factors at the two stresses, that at the lower stress having remained constant. All I can say about the cause of this suppression of ionization is "I don't know." It would make a nice subject for a university thesis.

The ionization point of drained cable was also raised by the use of carbon-compound gases instead of air or nitrogen but these introduced difficulties peculiar to the gases employed.

The maintenance of pressure by the low-pressure-gas cable depends on the integrity of joint wipes and particular care will be required in testing for leaks. It is possible that a test similar to the soap bubble test will be useful, using instead of soapy water, such a material as Nekal, a product of I. G. Farbenfabrik, which is claimed to be much more efficacious than soap solution.

The impulse transient dielectric strength of drained cable is only about 15 per cent less than that of well-impregnated cable, so that on that basis, insulation walls would not have to be more than 18 per cent greater than those of oil-filled cables, which are based on impulse strength. It would therefore seem that Mr. Shanklin's table of thicknesses is conservative.

There are, of course, features of uncertainty in this cable which only time will clear. One of these is whether the eventual drainage of oil from the paper will not unduly lower the dielectric strength of the insulation. Another is the effect on the lead of long continuous application of the moderate pressure used. Laboratory tests indicate that while the stretch of lead is slow at such pressures, it is nevertheless definite and certain and that it occurs at an accelerating pace, as the lead becomes thinner. In view of such uncertainties inherent in a pioneer installation, the Consolidated Edison Company deserves the thanks of the industry for taking the initiative.

Joseph Sticher (The Detroit Edison Company, Detroit, Mich.): The paper by Mr. Shanklin brings out an apparently beneficial action of wax in cables. The beneficial action of wax referred to here consists of the closing up of larger voids and the extinguishing of ionization in these voids before it begins to spread and become cumulative. Until recently, cable wax was looked upon as visible proof that deterioration was occurring in the cable insulation and that this would eventually lead to breakdown of the cable. Wax was therefore abhorred and efforts were made to develop cable impregnants which would produce a minimum of wax when subjected to corona discharge.

During the past decade and more The Detroit Edison Company has studied the effects of corona discharge on oils and oil-impregnated papers with the view of determining changes in the electrical, chemical, and physical characteristics. From the results of this study, some of us gained a new viewpoint regarding cable wax, as follows:

> It appeared that possibly wax should not be abhorred but really welcomed since it might be looked upon as a sort of natural self-defense of the cable against the ravages of ionization occurring within it. This conjecture was expressed in a paper presented before the National Research Council in November 1938. ("The Effect of Corona Discharge on Cable Insulation," Joseph Sticher, D. E. F. Thomas, C. D. Robb, and F. M. Hull; presented at the Conference on Electrical Insulation of the Division of Engineering and Industrial Research of the National Research Council, November 3-4, 1938, at Pittsburgh, Pa.)

The results which Mr. Shanklin relates in connection with tests on various test lengths of cable are very interesting in that they show that ionization under a given set of conditions gradually disappeared, to reappear only after an increase of voltage. These results are in support of the suggested hypothesis of self-defense of the cable in the following manner:

> The gradual disappearance of ionization seemed to be due to formation of wax in voids. This wax tended to decrease the size of the voids with subsequent load cycles until ionization could not be maintained any longer at the prevailing voltage gradient.

When this apparently beneficial action of cable wax is considered still further, it appears that it might have potential possibilities to aid in the constant efforts to improve solid-type oil-impegnated paper cables somewhat as follows:

> If a cable impregnating compound were found or developed which would form wax very rapidly under ionization, voids in a cable impregnated with this compound would rapidly be filled with wax to the extent that ionization would be extinguished. A certain amount of increase in total void volume would result from this waxing because of the increase in the density of the compound during the polymerization or condensation. The voids making up the total void volume would, however, then consist of two classes: first, voids too small to be ionized even in the region of highest voltage gradient; second, all voids including maximum size located in regions of voltage gradient too low for ionization to occur. In this connection it should be pointed out that compounds which form wax readily under corona discharge, such as unsaturated and aromatic oils and certain of the naturally occurring sulfur compounds, are at the same time compounds which produce only a little gas under this treatment. This coincidence is of decided advantage in the present application. In the unsaturated class, olefins such as hexadecene and higher homologs; or turpenes such as pinene or higher homologs, or polymerized products of turpentine; or in the aromatic class, polyindene or homologs of xylene; or in the sulfur class, the sulfides, might be desirable. It must also be realized that the wax, to be of use in this cable, should not be soluble in the impregnant and should not carbonize in the time it takes for ionization to be extinguished.
>
> Whether a cable could save itself in this manner would depend essentially on what might be called the "waxing ability" of the impregnating compound. If this waxing ability is low, the insulation might fail before sufficient wax could be formed adequately to reduce the size of the affected voids.

Efforts "to fill the economic gap caused by the relatively high cost of oil-filled cable below 38 kv" would be best served, it seems, by improvements of the solid-type cable, which would allow this type of cable to remain self-contained. The *H*-type cable is an example of such an effort and that improvement has gone a long way toward solution of the problem. In other improvements of the solid-type cable a considerable amount of auxiliary equipment is usually entailed. The low-gas-pressure cable appears to come close to the "self-contained" solid-type cable. If, however, a freely waxing cable impregnant, used in place of the present impregnants, is developed to extinguish ionization as indicated, then a truly "self-contained" solid-type cable of satisfactory performance may possibly result.

G. B. Shanklin: Mr. Del Mar's kind and encouraging remarks about this development are very much appreciated. His comments regarding the self-healing effect of ionization on a test length of drained solid-type cable are quite interesting. Mr. Del Mar states that he obtained this self-healing effect without pronounced *X* wax formation which invariably accompanies the same effect in the low-gas-pressure cable. I think the difference is explained by the physical construction of the two cables. Mr. Del Mar's cable did not have longitudinal feed channels for maintaining uniform gas pressure. The pressure in voids accordingly must have varied over a wide range with load cycles, and ionization gradually improved as the cable became gas-saturated with a gradual increase in pressure.

I readily admit that formation of *X* wax may not be the entire answer to this improvement in ionization in the gas-pressure cable. Perhaps further investigation will bring out additional factors. Our work shows however that formation of wax is without doubt one of the major factors in this self-healing effect. To give the best results this wax formation should be accompanied by a minimum amount of by-products, such as gas evolution, water, and other conducting impurities.

In regard to the possibility of porous wipes and other sources of leakage, it might be pointed out that the gas-pressure cable system is self-maintaining and so far there has been no difficulty in locating and repairing leaks that in an ordinary solid-type cable system would not be discovered until service failure occurred. The two field installations described in the paper are now completely gas tight, and no real difficulty was experienced in obtaining these results.

Mr. Del Mar has analyzed the transient impulse strength characteristics of the gas-pressure cable very accurately. This cable has an impulse strength midway between that of oil-filled cable and solid-type cable. We are in no way worried about the sufficiency of impulse strength in service.

Referring to the question raised by Mr. Del Mar regarding the possibility of continuous drainage of compound in service, we have subjected lengths of this cable to severe load cycles over a period of two years or more and have found in every case that drainage of compound from the paper insulation stabilized in a relatively short time and after a balance was reached between gravity and capillary attraction. The total drainage before this balance is reached is a function of compound viscosity and film tension (tackiness).

Mr. Del Mar also raises the question of gradual and continuous sheath creepage in service. Fortunately we have had a good deal of experience with oil-filled cable and sheath creepage has also been exhaustively studied in the laboratory. This work has shown that when the stress on the lead sheath does not exceed 125 pounds per square inch of sheath no appreciable creepage takes place.

Messrs. Berberich and Sticher both have a very accurate and parallel conception of the low-gas-pressure cable. The principle upon which this cable is based is in many ways opposite that generally accepted for solid-type cable. In the past, as Mr. Sticher states, ionization and wax formation in a cable have been abhorred. I think the chief reason for this is that wax formation in solid-type cable is usually accompanied by wide pressure fluctuations and undesirable by-products, such as gas evolution, water, and other conducting impurities. The wax itself has not been the real culprit.

In the gas-pressure cable the goal aimed at is uniform pressure maintenance and maximum wax formation with minimum gas evolution and by-products. The test results given in the paper show that this goal has now been closely approached. We are actively continuing our study of the most favorable compound characteristics, and we hope in time to obtain further improvements in this direction. We intend to continue our present trials of all possible compound compositions. So far we have found that the compound requirements for this gas-pressure cable are not greatly different from those for ordinary solid-type cable as regards stability and long life.

Referring to Mr. Davidson's discussion, I plead guilty of loosely using the term "negative pressure." It is more an expression of convenience than scientific accuracy and is used for the purpose of avoiding confusion in referring alternatively to absolute zero pressure and atmospheric zero pressure.

The last question raised by Mr. Davidson can only be answered by a good deal of additional research. I can only say that our work seems to indicate that there is more than merely a difference of degree in the effects of ionization at different pressures. Ionization in a partial vacuum seems to lead to more harmful gas evolution and more conducting by-products than corresponding discharges at higher pressures. Whether this is a question of differences in mechanism, chemistry, or both, I do not know.

Replying to Mr. Komives, it is not our purpose intentionally to lower the insulation quality in gas-pressure cable for purposes of obtaining better sheath performance. Actually we hope to obtain both better insulation quality and better sheath performance as compared with solid-type cable. We believe that the tests described in the paper bear this out. The gas-pressure cable remained stable under severe load-cycle heating at an average stress of 85 to 100 volts per mil. If stability under these conditions is accepted as a criterion of quality, we do not believe that ordinary solid-type cable could show any improvement.

Mr. Komives raises a question regarding the relative size of the joint casings for gas-pressure cable. It is true that the joint casing diameter is larger for purposes of acting as a sump. The length of the joint however is exactly the same as that for solid-type cable of the same voltage rating. It is length and not diameter of joints that is affected by manhole dimensions.

E. W. Davis asks about the impulse strength of this gas-pressure cable. I believe this question has already been answered in Mr. Del Mar's discussion. We believe the impulse strength of this cable to be somewhere between that of corresponding oil-filled and solid-type cable.

Doctor Wiseman seems to feel that solid-type cable can be improved by providing more ready means of longitudinal flow of compound. He suggests that a loose-fitting lead sheath and joints without reinforcement wrappings will help in this respect. This happens to be a possibility we have given a good deal of study to in the past. The whole trouble is that these changes will introduce all of the difficulties of oil-filled cable without the compensating benefits. As with oil-filled cable, it would be necessary to accept cumulative hydrostatic head pressure and control this with stop joints, reservoirs, etc. The benefit of this complication would not be obtained however, since it would be impossible also to control and maintain positive pressure over the whole cable length during load cycles. This can only be done with a relatively thin oil and sufficiently large longitudinal feed channels, in other words, an oil-filled cable. There is nothing in between as far as oil feed is concerned.

I cannot see any real benefit in the type of high-gas-pressure cable described by Doctor Wiseman, having wrapped varnished silk tapes over the cable insulation which act as a semi-impervious membrane. It seems to me that these silk tapings would merely retard but not prevent gradual gas saturation of the impregnated paper insulation. They would also interfere with initial drying and impregnation treatment in the factory and maintenance of uniform pressure throughout the cable cross section in service. It was for these reasons that we abandoned the idea of semi-impervious wrappings in the low-gas-pressure cable, where, at best, pressure is at a premium and there is none left to waste.

Doctor Wiseman seems to have missed the point that the low-gas-pressure cable is gas-saturated before it is shipped from the factory. This is accomplished as part of the regular factory treatment, and the reel lengths of cable are shipped under gas pressure. He need have no worry, then, about the effects of gradual gas absorption during service.

Reverse-Rotation Test for the Determination of Stray Load Loss in Induction Machines

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VARIOUS methods for the accurate determination of stray load loss in induction machines have been devised in recent years. Also older methods have been improved and developed as the nature of the loss has become better understood. The employment of different methods of testing on a given machine seldom shows complete agreement in results, due to minor discrepancies and inaccuracies inherent in the methods themselves. Another difficulty is that all of the plans proposed to date require a high degree of skill and experience on the part of the testing technician. Thus industry today finds itself in need of a test which will give highly accurate results and at the same time be simple and easy to perform. The purpose of this paper is to describe a new method of testing—termed the "reverse-rotation test"—which will more nearly meet these requirements. The theoretical basis and the assumptions involved are discussed. Tests to determine the accuracy of the method have been made on two squirrel-cage motors of somewhat different characteristics by various methods of testing. The results of these investigations are described and curves are given showing the degree of exactness obtained. While the method includes assumptions regarding compensating effects involving minor components of the loss, its over-all accuracy for the motors tested is shown by comparison with the values obtained by other methods having high precision.

Advantages

While the accuracy of the reverse-rotation method of determining stray load loss has not yet been thoroughly established for all types of induction machines, the results of the experimental work described herein prompts the following claims:

1. This method will give highly accurate results and at the same time the test is simple and convenient to perform.
2. The value of the losses can be determined quickly without the use of special apparatus—the only requirements being a source of power having adjustable polyphase voltage and a driving motor for which the losses can be determined.
3. Laborious computations are avoided and final determinations of the value of the stray load loss can be obtained in a few moments by simple calculations from the measured quantities.

Test Procedure and Computations

The reverse-rotation test is carried out by applying reduced balanced polyphase voltage to the stator terminals of the machine being investigated while driving the rotor at synchronous speed in the direction opposite to that of the revolving stator field. The stator current may be set at any desired value by adjustment of the applied voltage. Two power measurements are necessary: the power required to drive the rotor and the power input to the stator circuit. Let the former be designated by P_r and the latter by W_s. The power required for rotation of the motor rotor (P_r) may be obtained by driving it with a sensitive dynamometer or a motor with known losses. The friction and windage losses at synchronous speed of the motor under test are also required and may be designated by P_f. The difference between these two quantities gives the component of the net input to the rotor which is supplied through the driving mechanical power. If this net power resulting from rotation be represented by P, then:

$$P = P_r - P_f$$

When a d-c motor is used to drive the induction machine, correction for losses in the d-c motor becomes a simple matter if the same value of field current is used for both the load and the no-load determinations, obtaining constant speed by armature voltage control. The difference in the armature-circuit brush and copper losses of the d-c motor is the only correction that must be applied to the difference of the two d-c input values in order to determine the net power P, since all other losses in the driving machine remain constant.

The total stator power input W_s is carefully measured by wattmeters for the particular value of current used. An accurate measurement of the stator d-c resistance is made at the winding temperature used during this power observation. The total polyphase stator copper losses are then computed and may be designated by W_{cu}. When subtracted from the total stator input (W_s) there remains the net stator power which may be called W, and:

$$W = W_s - W_{cu}$$

The value of stray load loss for the particular stator current used is then obtained as the difference between net rotor input and net stator power, or

$$\text{stray load loss} = P - W$$

Experience indicates that it is easier to obtain accurate test points over the range between full-load current and about double this value. If points are determined over this range of current the value of the stray load loss for any desired current can be computed readily by application of the law that the loss varies as the square of the current. The curve for the working range of the motor is obtained simply in this manner.

LIGHT-LOAD CORRECTION

In order to satisfy the definition of stray load loss it should be zero at no load. This desired result is obtained by calculating the loss for no-load current and subtracting its values at this point, that is, simply making the loss zero at no load. At the one-half load point one-half the correction necessary for no load may be applied, and at full load no correction is necessary.

Discussion of Principles

The stray load loss in induction machines is defined as the residual power

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Paper number 39-4, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 17, 1938; made available for preprinting November 28, 1938.

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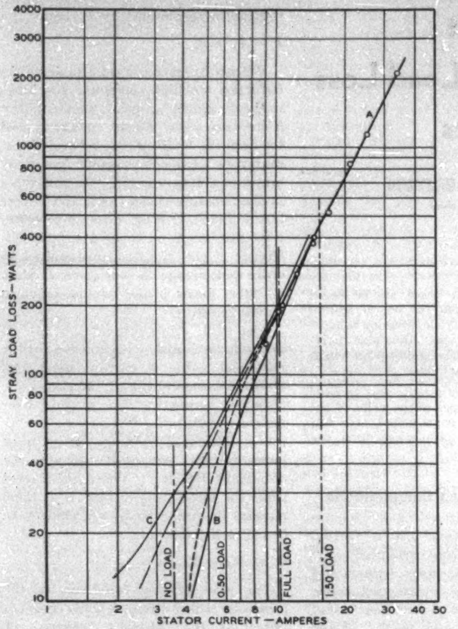

Figure 1. Stray load loss curves for motor 1

A—Loss by reverse rotation method. Dashed line is straight line extension; dotted line shows loss after light-load correction is applied

B—Loss by load-back through belt

C—Loss by load-back through belt, including added component for no-load current

loss after all other known losses are considered in accordance with specified methods of measurement.[1] The loss consists chiefly in the increase in iron and copper losses occurring as a result of the load current in the stator and rotor circuits while the rotor is revolving. The total stray load loss may have components at tooth frequency and at fundamental frequency, the former being of major importance. The reverse-rotation method of testing gives a measurement of all tooth-frequency components and includes the fundamental-frequency components only through compensating effects. The test procedure also assumes that the direction of rotation of the rotor with respect to the direction of the fundamental flux does not alter the magnitude of the tooth-frequency losses, since in both cases the tooth-frequency fluxes are superimposed upon fundamental-frequency fluxes.

When the rotor of the machine is turned at synchronous speed in the direction opposite to the field flux all power supplied is consumed in losses. The total power input consists of electric power through the stator and mechanical power supplied through rotation $(W_r + P_s)$. After subtracting stator copper losses and friction and windage losses from the total power input (both electric and mechanical) the remaining losses may be classified in three frequency categories: (1) fundamental-frequency losses caused by the leakage flux of the stator, (2) double-frequency losses in the rotor resulting from reverse rotation, and (3) tooth-frequency components of the stray load loss occurring in both rotor and stator. The stator fundamental-frequency losses caused by the leakage flux, which are apparently very small, are not considered at this point and only included in the final result through compensating effects to be explained later. The double-frequency rotor losses result from currents in the rotor conductors and fluxes in the rotor iron at this frequency. With rotation at synchronous speed in opposite direction to the stator field the double-frequency rotor losses are supplied in equal amounts from the power of rotation P and from power transmitted by the stator circuit through the air gap W. The tooth-frequency losses result directly from rotation, and consequently

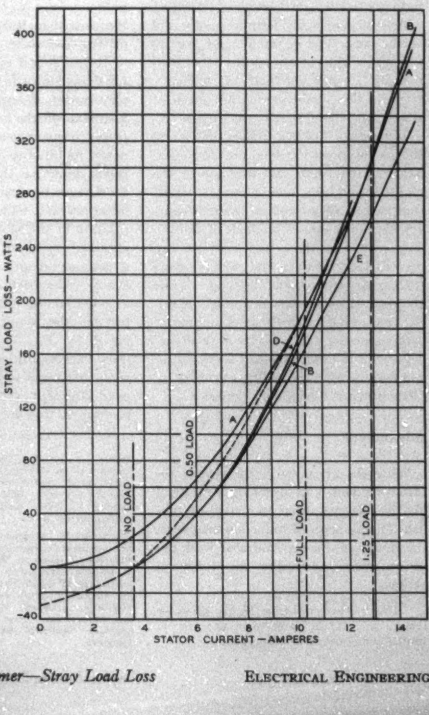

Figure 2. Comparison of stray load losses for motor 1 determined by different methods

A—Loss by reverse rotation method. Dotted line shows loss after light-load correction is applied

B—Loss by load-back through belt

D—Loss by load-back through d-c machines

E—Loss by d-c excitation of stator

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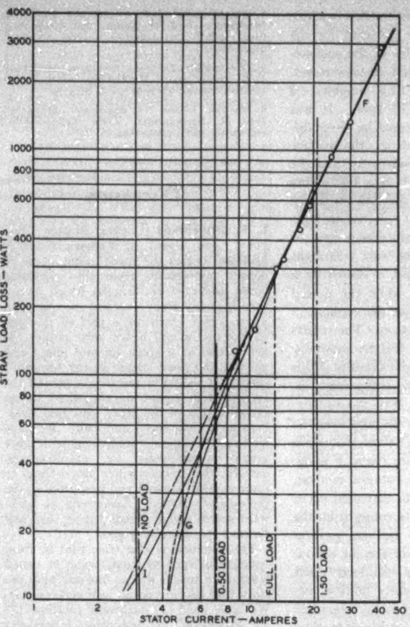
[1] For all numbered references, see list at end of paper.



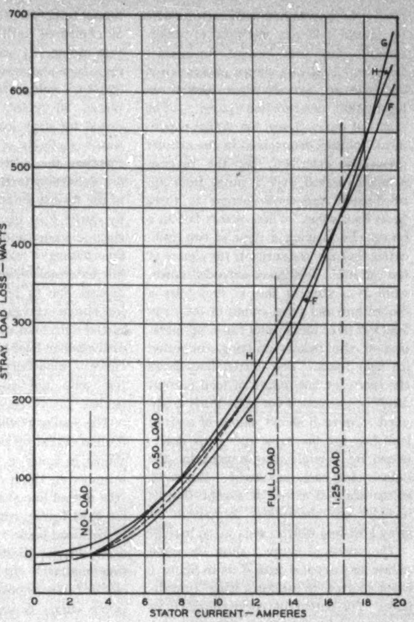


Figure 3. Stray load loss curves for motor 2

F—Loss by reverse-rotation method. Dashed line is straight line extension; dotted line shows loss after light-load correction is applied

G—Loss by load-back through belt

I—Loss by load-back through belt, including added component for no-load current

Figure 4. Comparison of stray load losses in motor 2 as determined by different methods

F—Loss by reverse rotation method. Dotted line shows loss after light-load correction is applied

G—Loss by load-back through belt

H—Loss by d-c excitation of stator

are supplied only by mechanical power. Thus the net power input through rotation P consists of one-half the double-frequency rotor losses and the total tooth-frequency losses. By subtracting from this quantity P the air-gap power supplied from the stator W, the tooth-frequency losses are separated out and given as the remainder.

The question may be raised regarding the amount of the error introduced by neglecting the component of the stray load loss which might be assumed to exist because of the fundamental-frequency leakage flux. As a matter of fact, the proposed treatment not only neglects this component of the loss but includes it in the stator value W which is subtracted from the net rotational power input component P, thus doubling this error. However, the fundamental-frequency component of the stray load loss is very small and any error resulting from this treatment is evidently compensated by other small errors involved in the method. These may be listed as follows:

(*a*). The iron of the motor is worked at low saturation during this test which tends to give a higher value of stray load loss than would exist under normal saturation conditions.

(*b*). Synchronous speed is slightly higher than normal speed of operation, giving a slightly larger value of stray load loss (which varies as the square of the speed).

(*c*). In the application of test results to motor performance the currents used during the test are considered as stator input currents under load conditions. During the test, due to the absence of the exciting component, the stator current has a corresponding rotor current, while under actual motor operation the rotor current is less than the stator current. This slight increase in rotor current under test conditions gives a larger value of stray load loss for a given stator current than actually exists under normal operating conditions.

The above items separately appear of minor importance but they all act in the same direction and tend to compensate for the omission of the fundamental-frequency component of the stray load loss, which is evidently a small quantity. Experimental results indicate that this compensation is approximately complete in the region extending from 150 per cent load to well below full-load. At light loads a small correction is necessary, and the purely arbitrary method previously proposed is probably sufficiently accurate for all practical purposes and it has the advantage of being simple and easy to apply.

Test Results

Motor 1

The first motor tested was of the squirrel-cage type and was rated at 10 horsepower, 550 volts, 10.3 amperes, 3 phase, 60 cycles, 1,750 rpm. This motor had previously been tested with extreme care by several different methods to determine its stray load losses.[4] In figure 1 stray load losses are shown as a function of armature current when plotted on logarithmic cross-section paper. The points of curve *A* are the actual experimental points, determined in the manner previously described, by the reverse-rotation method over a range from approximately full load current to three times that value. These points fall on a straight line having a slope of two, indicating that the loss varies as the square of the current. This curve extended downward as a straight line is shown by a dashed line and gives values of the stray load loss over the normal range of operation of the motor omitting correction for light loads. The dotted line shows the curve for low values of load current after the correction for light loads is applied. Curve *B* shows values of a stray load loss for the same motor as determined by careful measurement by the belted load-back method.[3] Curve *C* is the same as curve *B* except that it includes the theoretical component of stray load loss which exists at no load.[3,4]

The curves of figure 2 show the same values for curves *A* and *B* as in figure 1 when plotted to uniform-scale co-ordinates. The dotted line curve shows curve *A* corrected for light loads. On this plot values of stray load loss, determined with a high degree of precision by the load-back test through d-c machines, are given by curve *D*.[4] Values as determined by the d-c excitation method[2] are also given for comparison in curve *E*. The results shown by curves *B*, *D*, and *E* were given in a previous paper[4] and are repeated here so that the curve *A* determined by the reverse-rotation method may be compared with them.

The curves of figure 2 show that for motor 1 the reverse-rotation method gives values for the stray load loss that are in as close agreement with those from other methods of testing as exists among the other methods themselves. At the full-load point the reverse-rotation method value is only 7 watts above the highly accurate value obtained by loading back through d-c machines, and 14 watts above the result obtained by the belted load-back method. At 125 per cent load the values from curve *A* and curve *B* agree almost exactly. The reverse rotation method gives a value that is about 11 watts high at 50 per cent load.

MOTOR 2

This motor differed from motor 1 in that it possessed abnormally high iron losses, being 5.0 per cent for this machine as compared with 2.9 per cent for the first motor. It was also of the squirrel-cage type and was rated 10 horsepower, 220/440 volts, 26.6/13.3 amperes, 3 phase, 60 cycles, 1,710 rpm. It was tested for stray load losses by three different methods of testing: the reverse rotation, the belted load-back,[3] and the d-c excitation method.[2] The test points of the reverse rotation method are shown by curve *F* in figure 3, plotted to logarithmic scales, and again give a straight line having a slope of two. Straight line extension downward is shown by a dashed line as before while the dotted line shows extension after the application of the light load correction. The results of the belted load-back test are shown by curve *G*, while curve *I* gives results of this test with the no-load component included.

The results of the three tests over the normal operating range of the motor are shown in figure 4, where curve *F* is determined by the reverse-rotation method, (the dotted line showing corrected curve for light loads), curve *G* comes from the belted load-back test, and curve *H* is derived by application of the d-c excitation method. At the full-load point curve *F* lies approximately midway between curves *G* and *H* being about 23 watts above the belted load-back method value and 24 watts below the value obtained by the d-c excitation method. At 125 per cent load curves *F* and *G* agree and curve *H* is 30 watts higher. The reverse-rotation method gives a value that is about 7 watts higher than that from the belted load-back method at 50 per cent load, and it is about the same amount below the d-c excitation value in this region.

Because of difficulties involved in performing the tests and inaccuracies inherent in each method, complete agreement in results by application of the various methods employed has not been possible. However, the reverse-rotation method has given results for the two motors tested that are well within the degree of accuracy which might be expected from any of the methods used.

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2. MEASUREMENT OF STRAY LOAD LOSS IN POLYPHASE INDUCTION MOTORS, **C. J. Koch.** AIEE TRANSACTIONS, volume 51, 1933, pages 756–63.

3. STRAY LOAD LOSS TEST ON INDUCTION MACHINES, **T. H. Morgan and Paul M. Narbutovskih.** AIEE TRANSACTIONS, volume 53, 1934, pages 286–90.

4. STRAY LOAD LOSS TESTS ON INDUCTION MACHINES—II, **T. H. Morgan and Victor Siegfried.** AIEE TRANSACTIONS, volume 55, 1936, pages 493–7.

5. EFFICIENCY TESTS OF INDUCTION MACHINES, **C. C. Leader and F. D. Phillips.** AIEE TRANSACTIONS, volume 53, 1934, pages 1628–33.

6. POWER LOSSES IN INDUCTION MACHINES, **Paul M. Narbutovskih.** AIEE TRANSACTIONS, volume 53, 1934, pages 1466–71.

Discussion

L. E. Hildebrand (General Electric Company, Lynn, Mass.): It is known that stray load loss causes the speed-torque curve to depart appreciably from the calculated curve predicted by formulas which do not take high-frequency core loss into account. These losses constitute a part of the load on the motor and hence must reduce the net torque at a given forward speed and increase the gross torque at any backward speed. It should be possible to predict the correction in torque at any speed by an extrapolation of the stray load loss measured at normal speed and load. We have made fairly accurate predictions with quite simple assumptions for extrapolation, namely, high-frequency loss proportional to rotor current squared and frequency to the three-halves power. Agreement at backward speeds and at speeds greater than half forward speed are very good.

The converse is also true, that is, from tested backward rotation torque or tested breakdown torque we can find out what the stray load loss at normal load and speed is. We have used (1) backward rotation, (2) d-c excitation, (3) polyphase breakdown torque, and (4) single-phase breakdown torque, all to measure stray load loss. All agree quite well. Measurement of the single-phase breakdown torque seems to be a very good alternative method to d-c excitation and backward rotation.

William R. Hough (Reliance Electric and Engineering Company, Cleveland, Ohio): The paper under consideration presents a method of study of the stray load losses of induction machines, which is a definite contribution to progress in this field. This discussion is based on a limited experience with the method developed by the authors.

This limited experience has shown that of the advantages claimed by the authors for their method of testing, the degree of accuracy is the only point in question. The tests are simple and convenient to perform, can be done relatively quickly with a minimum of necessary equipment, and the computations of stray load loss from test information are not laborious.

An analysis of the steps taken in arriving at the value of stray load loss for a particular condition readily indicates that a high degree of accuracy is necessary in each test reading obtained, in order that the final result, namely, stray load loss, will be accurate.

The stray load loss is the difference between two quantities, each of which is the difference between two other quantities. In testing a normal induction motor having from one to two per cent stray load loss, it will be found that the stray load loss is small in comparison with the test values obtained by this method from which it is determined. In testing machines having higher than normal stray load losses, the relative accuracy should be greater, since the stray load loss for any condition would be larger in comparison with the values from which it is obtained.

The authors have pointed out that their method neglects one of the components of stray load loss, and have cited certain compensating items. They have not attempted to evaluate these discrepancies other than to demonstrate that their method gives substantially accurate results by actual test in comparison with load-back test results. The final proof by actual test results is, of course, the most important consideration in determining the value of this method of testing. The authors have, by the limitations they have cited, pointed out the necessity of proof of the method of actual test results, and have contributed two specific cases to support this proof.

The limited number of tests which are the basis of this discussion substantiate the method in principle, but do not substantiate the degree of accuracy obtained by the authors. Present experience would indicate the variance between the values of stray load losses determined by this method and those determined by load-back tests, to be in the order of one per cent of motor input. Continued experience is necessary before a more accurate opinion can be given.

P. C. Smith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This is the least complicated method so far proposed for squirrel-cage motors. It has no advantage, from a simplicity standpoint, over the Koch method for wound-rotor motors, since the same readings are required for both. The authors have pointed out the errors in principle and the assumptions made in this method and show by test that they are negligible or are compensated for in a ten-horsepower four-pole motor. There are, however, two factors which are increasingly important with increase in motor size and, if neglected, will lead to appreciable error. I have reference to the fundamental end-zone loss and the correction for magnetizing current at full load.

In large motors, particularly high speed, the end-zone loss is an appreciable amount. When the input to the stator is measured, this loss is included. Hence, when stator input is subtracted from rotor input, this end-zone loss serves to reduce the net result when actually it should be added. That is, twice the end-zone loss must be added to the answer obtained by this method to get the correct loss. This loss is partly compensated by neglect of the magnetizing current, but it does not necessarily follow that they balance. In fact, in large high-speed motors, it may lead to considerable error.

It is pointed out in the paper that the absence of magnetizing current, rather the fact that it is low, results in a secondary current which is too high. In low-speed motors, where the magnetizing current is large, some correction at full load will be necessary.

As pointed out in this discussion, this loss and the end-zone loss tend to balance, but the high end-zone loss goes with high-speed motors while high magnetizing current goes with slow speed and only in certain cases will they cancel out.

Further tests, over a wide range of horsepower and speed, are required to check this method.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): After listening to the paper by Morgan, Brown, and Schumer and some of the discussions, I believe that in view of the widely different results obtained, better progress could be made in evaluating the various methods under discussion if more data were given regarding some of the important design characteristics of the machines tested. At least it would be desirable to know the speeds, ratings, and various essential facts regarding the slot, tooth, and tooth-tip structures. If some such data were available, it might be possible to draw conclusions as to which of several methods can be used to best advantage for a given range of sizes or types of motors. Undoubtedly it is desirable to find methods giving satisfactory results for all ratings, but such ideal conditions cannot always be obtained and therefore it may be necessary to exercise a certain amount of discrimination.

C. J. Koch (General Electric Company, Schenectady, N. Y.): The test described in this paper is the easiest to perform with accuracy, of the methods so far developed for measuring the stray load loss of induction machines. In short the test procedure itself and the apparatus required become identical with the established short-circuit core-loss test of synchronous machines of similar sizes.

Does the reverse-rotation test determine the value of the stray load loss as it actually exists in the machine under load conditions? If we could calculate accurately the high-frequency core and stray load losses in induction machines this question could be answered from theoretical considerations. This we cannot do, however, and we must resort therefore to tests of large numbers of motors to determine the importance of the disturbing effects mentioned by the authors.

The result of our experience has been that these effects are small. This indicates the principle source of the high-frequency losses to be the action of the tooth harmonic fluxes. As far as the action of the stator-tooth harmonic fluxes on the rotor is concerned their frequency is very nearly the same for normal operation, reverse-rotation test, or normal-speed test with d-c applied to the stator. We have to offer one test confirming this. The motor tested was rated 150 horsepower, 900 rpm, and was of the collector ring type. At rated load the values are:

Stray load loss by pump-back between identical motors	615 watts
Stray load loss by reverse-rotation test	600 watts
Stray load loss by d-c excitation of rotor	600 watts

We have also measured the stray load loss of a number of motors by the reverse-rotation test and by very carefully made dynamometer tests. All of these motors were rated 25 horsepower at 1,800 rpm synchronous speed and were of the squirrel-cage type. The results are shown in table I of this discussion. It will be noted that the stray load loss varied greatly from motor to motor. The reverse-rotation test, however, follows the dynamometer result with very satisfactory agreement at rated load.

Table I

Motor	Stray Load Loss by Dynamometer (Watts)	Stray Load Loss by Reverse Rotation (Watts)
A	637	650
B	230	245
C	400	335
D	195	230
E	487	450
F	712	620
G	130	215
H	1,003	800
I	355	520
J	747	670
K	224	350
L	184	157
M	255	260

These results create confidence in the reverse-rotation test as a measure of stray load loss. The ease of making the test and the fact that one motor only is required should stimulate further testing along this line with the ultimate object of incorporating the reverse-rotation test in the test code.

F. D. Phillips (General Electric Company, Schenectady, N. Y.): The data given in this paper show a very good agreement between the various methods of determining the stray load losses. For the first machine these are, reading from figure 2,

By reverse-rotation method	194 watts
By load-back through belt	180 watts
By load-back through d-c machines	187 watts
By d-c excitation of stator	165 watts

A maximum difference of 29 watts and a variation from the average of 17 watts.

For the second machine the results obtained are, reading from figure 4,

By reverse-rotation method	270 watts
By load-back through belt	247 watts
By load-back through d-c machines	294 watts

The maximum difference is 47 watts and the variation from the average 24 watts.

The accuracy of the determination of these losses is apparent when we realize that a number of instruments are used and careful investigation has shown that on such tests the best accuracy of the instruments and of observation is in the neighborhood of 30 watts under carefully controlled laboratory conditions.

The results shown in Mr. Koch's discussion of a larger number of comparisons between the reverse-rotation method and the dynamometer method show a fair agreement. The difference between the two methods varies from 5 to 37 watts in seven cases and from 65 to 203 watts in six cases and the machines on which one method gives lower losses are the same in number as those which gave higher losses. These tests were taken under commercial factory conditions and the results would not be expected to be as accurate as those obtained in a laboratory.

The results given in this paper and the discussion lead to the belief that further study will confirm the validity of this testing method. It is attractive because of its simplicity and because it does not require special apparatus, and the calculation of the results is simple.

Q. Graham (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. Morgan and his associates have proposed an ingenious method of measuring stray load losses which has a fairly sound theoretical basis. There is a definite need for such a method, particularly for larger motors where other methods of testing are more difficult. The accuracy of the method, however, must be checked with smaller motors for which input-output tests are possible.

I have applied the reverse-rotation test to six motors ranging in size from 3 to 20 horsepower. The stray loss is given in table II of this discussion for 100 per cent and 125 per cent current and is compared with the loss by input-output test. For four of the six motors the reverse-rotation test shows about double the loss given by input-output test while for the other two there is close agreement although the reverse-rotation method still shows the higher values. For the last motor on the list the losses by input-output test were measured at a reduced voltage also and it was found that for the same values of current the stray loss was higher, thus more nearly approaching the loss measured by reverse rotation. This would seem to indicate that the presence of normal saturation may have a more important effect than the authors have assumed and may account for much of the discrepancy shown by these tests. The authors, of course, have a right to question the accuracy of the input-output tests which have been used for comparison in the results presented here. I can only say that they are the result of a great deal of painstaking work in perfecting a testing procedure in which we ourselves have considerable faith. The comparative tests are an honest attempt to judge the merits of the proposed method but are by no means presented as final evidence of its failure.

Table II

Motor Number	Horsepower	Poles	Per Cent Current	Watts Stray Load Loss: Input-Output	Reverse Rotation	Direct Current (Code)	
1	7½	2	100	225	500		
			125	400	720		
2	7½	2	100	240	425	375	
			125	405	620	600	
3	20	2	100	355	790		
			125	700	1,220		
4	7½	4	100	135	145	160	
			125	205	220	239	
5	15	4	100	415	495		
			125	615	785		
6	3	2	100	35	90		220 volts
			125	62	142		220 volts
6	3	2	*100	50	90		180 volts
			*125	89	142		180 volts

* Losses recorded are for same amperes as for 220-volt test.

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): The authors are to be commended on the development of a method which is simple and is performed with the most fundamental equipment. Having had experience in some of the previous work on one of these same machines in which the stray load loss was determined by loading-back tests, I can confirm the claims made for this present test as to the ease with which it is performed. The relative amounts of time consumed in the two tests are illustrated by the fact that a whole run was made and calculated up into final results in about the same time required for one point in the previous tests. It is further interesting to note that all that is required in this new method is some machine to drive the motor at synchronous speed backward and a source of reduced potential at rated frequency, in the order of 20 per cent of the original ratings of the machine on test, depending upon its constants.

The test itself is particularly noteworthy in that it measures the losses of the machine under conditions of loading although at reduced excitation, and the separation of these losses is very simple as outlined in the body of the paper. This gives a test whose final results for stray load loss are substantially in agreement with other tests of proved accuracy, and even though there are inherent errors in the method, the authors have logically demonstrated that these errors tend to compensate for each other.

T. H. Morgan: The discussion on this paper is on the whole encouraging. In considering a test method of this kind it would be unfortunate if it were to meet with complete approval before receiving the necessary verification covering a complete range in machine type and size. The fact that several tests have already been made and reported on in the discussions indicates interest in the matter. It is our hope that this interest will continue so that the limitations of the test and its degree of accuracy may be definitely established.

It is the authors' opinion that the industry possesses too little knowledge regarding these losses, both as to the mechanism that produces them, and their exact location in the machine. As an example, P. C. Smith believes that an end-zone loss takes place in the stator of large high-speed motors, thus decidedly limiting the accuracy of the method described; that if this end-zone loss is an appreciable amount of the total stray load loss, the proposed treatment would produce a result considerably too low in amount. This reasoning seems correct. On the other hand, the results obtained by Q. Graham would indicate that the reverse-rotation method gives a value of loss which is high in all cases of motors of two poles tested by him. There are several similar conflicting differences embodied in the discussion and this one is mentioned only to illustrate the point that as yet we do not know which view is the correct one. The discussion indicates the need of a better understanding of the nature of stray load loss if we are to make progress in reducing it by improvement in design.

The apparent lack of agreement between the results of tests made by different people to determine the accuracy of the proposed method is not surprising. The experience of the authors has been that it is very difficult to secure the same measured value of the loss from any two of the recognized standard methods of testing. Those who work on the problem of stray-load-loss measurement will always agree on one point—namely, the difficulty of making accurate determinations. It therefore seems hardly sufficient to compare the results of the reverse-rotation test with values obtained by only one other method. It takes more time and energy to use several methods but a much more accurate comparison will result. In this connection the test results obtained by C. J. Koch on the 150-horsepower, 900-rpm motor are encouraging.

The suggestion of R. E. Hellmund that design information be given whenever possible is well taken. Only through a large number of tests on motors of different design will it be possible to determine the limiting point where a new method of testing fails to give sufficiently accurate results. It is quite possible that any one method may apply to only a certain range in size and type of motors. If so it would be advantageous to know the limitations. Design data regarding motor number 1 of the paper were previously given in reference 4. Exact design details of the other motor are not known but it can be said that they are similar in character.

The remarks of L. E. Hildebrand regarding the application of the laws of variation of stray load loss as a function of current and speed are pertinent. The authors have made investigations of these effects over the complete range of reverse rotation of the motor, and plan to have their findings ready for publication in the near future.

In closing the discussion it should be again pointed out that the authors feel that while the general results from the limited number of tests taken to date indicate possible high accuracy for the reverse-rotation method of testing, many more tests should be made employing different methods of testing before a satisfactory conclusion can be reached.

Inductive Co-ordination With Series Sodium Highway Lighting Circuits

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Synopsis: This paper describes the wave-shape characteristics of the sodium-vapor lamp and discusses the relative inductive influence of various series circuit arrangements in which such lamps are employed. A method is outlined by means of which the noise to be expected in an exposed telephone line may be estimated. Measures are described which may be applied in the telephone plant or in the lighting circuit to assist in the inductive co-ordination of the two systems. These measures need be considered only when a considerable number of lamps is involved, since noise induction is negligible when there are only a few lamps as, for instance, at highway intersections.

DURING the past few years, a new type of lamp has been developed for highway lighting purposes, making use of ionized sodium vapor as its luminous element. While lamps of this type can be operated in parallel, the most common application has been in the conventional series types of lighting circuits supplied through constant-current transformers.

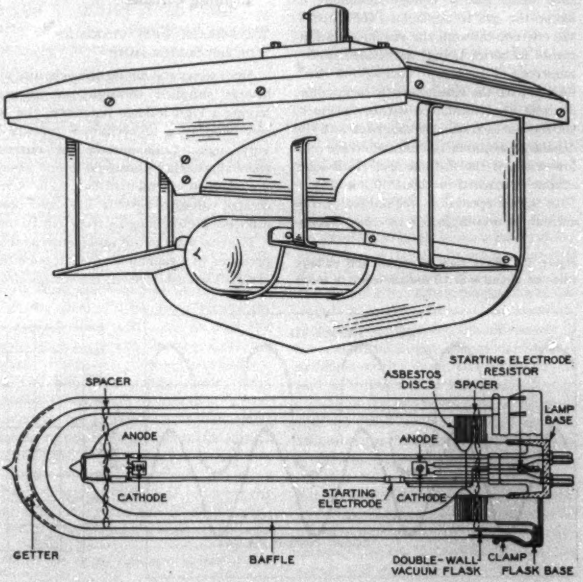
The wave-shape characteristics of sodium-vapor lamps are such that where series lighting circuits supplying a considerable number of such lamps are involved in exposures with open-wire telephone lines, attention must be given to the co-ordination of the two systems from the noise standpoint. The present paper gives the results of an investigation of the various factors involved in situations of this character. It is based largely upon a study conducted by project committee 1*A*, on noise induction, of the Joint Subcommittee on Development and Research of the Edison Electric Institute and the Bell Telephone System.

The wave-shape data included herein were obtained in the laboratory of one of the manufacturers and, through the cooperation of one of the power companies, on a number of field installations of sodium lamps of the same manufacturer. One of the situations tested, in which a lighting circuit some 16 miles in length was involved in an exposure with an open-wire telephone toll line, afforded an opportunity for co-ordinated inductive influence and noise measurements.

Operation of Sodium-Vapor Lamps

A brief review of the construction and operation of the sodium-vapor lamp[1,2] may be of interest as a preface to the wave-shape discussion which follows. The essential details of the sodium lamp designs of two of the manufacturers, together with sketches of the luminaires in which they are mounted, are shown in figures 1 and 2. The following is a brief description of the lamp shown in figure 1.

Figure 1. Ten thousand-lumen sodium lamp and luminaire—manufacturer A



The lamp consists of a long evacuated glass bulb enclosing, at each end, a tungsten filament or cathode and an open-end molybdenum anode. Each anode is connected to one side of the adjoining filament, the leads to the latter passing through a seal at one end of the bulb to a four-prong tube base which, in turn, makes contact with the socket. The luminous arc occurs between the anode at one end of the tube and the cathode at the opposite end. The connections are so arranged that the two anode-cathode combinations function alternately as the sign of the impressed voltage becomes alternately positive and negative. As indicated in figure 1, the anode-cathode assemblies are symmetrically located in the tube. Differences which occur between the positive and negative portions of the arc voltage curve in a particular lamp, as discussed hereinafter, are therefore due to vagaries of the arc rather than mechanical dissymmetries in the lamp. The bulb is insulated from the outside air by an evacuated glass bottle similar to a Dewar flask. The flask is required to retain heat generated by the arc for vaporizing sodium, which is solid at room temperature. Co-ordinated designs of flask and lamp are required to minimize the effects of external temperatures on the tempera-

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Paper number 39-33, recommended by the AIEE committees on communication and power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 18, 1938; made available for preprinting December 21, 1938.

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[1] For all numbered references, see list at end of paper.

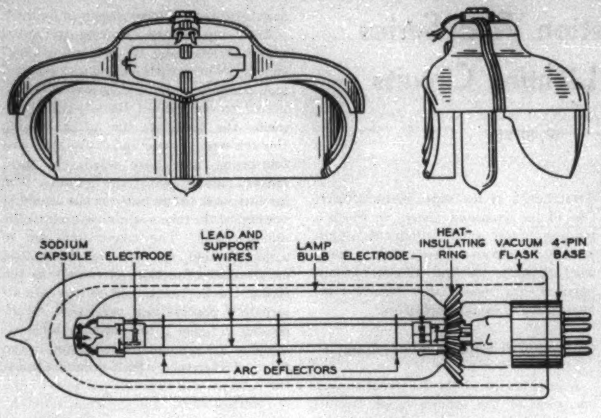


Figure 2. Ten thousand-lumen sodium lamp and luminaire—manufacturer B

ture within the lamp, and consequently on the light which is produced. Neon at a few millimeters pressure (for starting) is included in the bulb.

During the first few seconds after a lamp of this type is energized, the anodes are short-circuited by a time relay. During this period the normal current of 6.6 amperes is passed through the two filaments in series providing a degree of preheating. The short-circuiting contacts then open and a voltage sufficient to ionize the gas is applied to the anodes, the current through the arc and the filaments in series being maintained at 6.6 amperes. The lamp then glows brilliantly with the characteristic neon color. As heat is accumulated within the bulb, the sodium is gradually vaporized and the discharge acquires the characteristic yellow color of the sodium arc. Full light output is reached in about 30 minutes.

In series operation, regulation of the current is accomplished by the conventional constant-current transformer which has a relatively high leakage reactance. In multiple-circuit operation this regulation is effected by means of a special high-leakage-reactance transformer located in or near the luminaire.

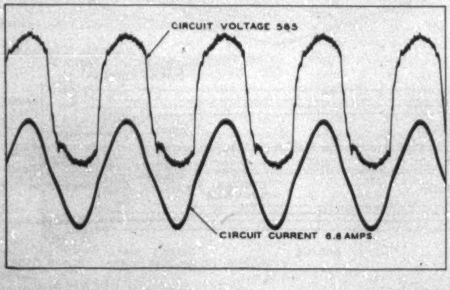


Figure 3. Wave form of current and voltage taken on circuit with 18 sodium lamps in series

Inductive Influence of Series Lighting Circuits

Wave-Shape Characteristics of the Sodium Lamp

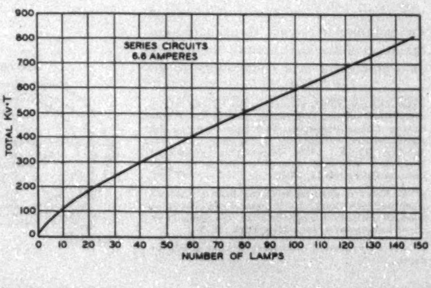
Since series sodium lighting circuits are always supplied through transformers having a high leakage reactance, the series impedance to harmonic currents is very high. Consequently the current wave approaches sinusoidal form. Measurements on several installations have indicated values of current TIF (telephone influence factor) ranging from four to ten.

It is characteristic of an arc such as that produced in a sodium lamp that, once the arc has been established, the voltage drop tends to be constant irrespective of the current. This gives rise to a flat-topped voltage wave, more nearly square than sinusoidal in form. This is illustrated by the oscillogram in figure 3, which was taken on a circuit employing 18 sodium lamps in series.

A harmonic analysis of the voltage wave across a single sodium lamp is given in the second column of table I. Only harmonic components in the range up to 1,500 cycles are listed although the measurements indicated the presence of a practically continuous band of harmonic frequencies extending well above 3,000 cycles. The magnitudes of the various harmonics, particularly those at the higher frequencies, varied over a considerable range from time to time. The figures in the table represent average values observed over a relatively short interval. It will be noted that many of the even as well as the odd harmonics were present, indicating that the positive and negative halves of the voltage wave were not exactly alike. In the case of this particular lamp, some of the higher even harmonics were at times, larger than the adjacent odd harmonics and controlled the voltage TIF.

In a series sodium lighting circuit the various lamps can be considered as serially connected harmonic generators. The equivalent series reactance of the supply transformer is high compared to the impedance of the lighting circuit including the lamps. Furthermore, in circuits of the lengths under consideration, attenuation and phase shift are not important. If all lamps were identical, therefore, the per cent harmonic voltages and the voltage TIF at the supply end of a long circuit would be the same as for a single lamp. The *Kv·T* (kilovolts × voltage TIF) would be equal to that for a single lamp multiplied by the number of lamps.

Figure 4. Total Kv·T for sodium lighting circuits having various numbers of lamps



The fourth column of table I gives a harmonic analysis of the voltage as observed at the supply end of a series lighting circuit comprising 149 lamps. In general, the odd harmonic voltages (in per cent) observed on the long circuit are somewhat smaller than those measured on a single lamp, indicating some differences in the relative magnitudes and phases of the odd harmonics generated in the individual lamps. The even harmonics are greatly reduced in the long circuit and are not important contributors to the voltage TIF. This indicates that the even harmonics generated in the individual lamps are fortuitous in character, tending to cancel when a number of lamps are connected in series. Additional data taken on operating circuits of various lengths and showing the variation of the total *Kv·T* with the number of lamps in series have been plotted giving the curve shown in figure 4.

DISTRIBUTION OF BALANCED Kv·T ALONG THE CIRCUIT

Since each lamp of a series circuit acts as a serially connected generator of harmonic voltages, the *Kv·T* across the circuit changes at each lamp, decreasing progressively from a maximum at the supply end to the value for a single lamp at the distant end of the circuit. While no experimental data are available on the rate of change in the influence, it is probable that a plot of the influence against distance from the far end of the circuit would have a shape similar to the curve in figure 4. In estimates of noise in the practical case, however, a straight-line variation is usually assumed.

Table I. Analyses of Voltage on Sodium Lighting Circuits

	Single Lamp Current = 6.6 Amperes. Root-Mean-Square Voltage = 30 Volts		Series Circuit, 149 Lamps Current = 6.6 Amperes. Root-Mean-Square Voltage = 4,850 Volts	
Frequency	Per Cent of Root Mean Square	TIF	Per Cent of Root Mean Square	TIF
120	*	*	*	*
180	21.9	3	13.9	2
240	*	*	*	*
300	12.9	27	8.7	18
360	*	*	*	*
420	6.1	36	4.2	25
480	*	*	0.6	5
540	2.55	32	3.18	40
600	*	*	0.54	9
660	0.53	12	2.70	61
720	1.82	54	0.43	13
780	1.98	81	1.82	74
840	1.98	109	0.33	18
900	0.83	60	0.61	44
960	1.66	156	0.20	19
1,020	0.41	48	0.14	16
1,080	1.66	199	0.14	17
1,140	0.99	110	0.54	60
1,200	1.82	173	0.17	16
1,260	0.73	58	0.62	49
1,320	0.64	42	0.15	10
1,380	2.12	116	0.80	44
1,440	0.89	42	0.18	9
1,500	1.16	51	0.52	23
TIF	500–625	400**	166.	155**
Kv·T	15.3–17.4		805.	

* Value measured controlled by adjacent harmonics or "background."

** TIF calculated from analysis up to 1,500 cycles.

It has been found that the leakage reactance of the constant-current transformers used to supply series circuits of this type is usually sufficiently high to prevent the transfer of the voltage distortion from the lighting circuit back into the supply circuit.

MAGNITUDE AND DISTRIBUTION OF RESIDUAL Kv·T

In the case of a single-wire ground-return circuit, the residual *Kv·T* at any point is, of course, the total *Kv·T* of the circuit at that point.

On a two-wire circuit where both wires are on the same pole line, the magnitude and distribution of the residual voltage† depend upon the number of lamps, the location of the lamps, and the location of any ground which may be on the circuit. Lighting circuits of the usual lengths are in general electrically short so that the series impedances and the capacitance between wires have a relatively small effect. In the case of a circuit isolated from ground, the voltages to ground are determined by the location of the lamps and the capacitances to ground of the circuit conductors.

Figure 5 shows schematically a number of circuit arrangements and indicates the variation, along the circuit, of the voltages to ground and the residual voltage.

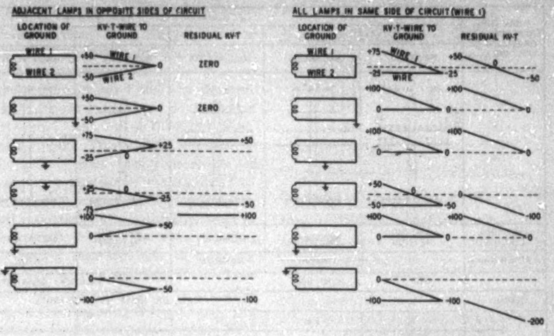


Figure 5. Distribution of Kv · T along a series sodium lighting circuit

Assumptions: capacitance and leakage to ground of the two wires the same; all lamps of identical characteristics

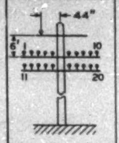
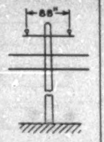
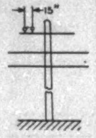
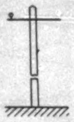
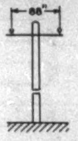
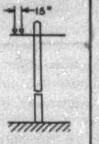
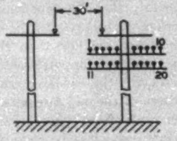
Note: Numerical values indicate percentages of *Kv · T* at transformer terminals

The smooth variations in these quantities shown in this figure would occur only for lamps extremely close together. In practice there is, of course, an appreciable distance between lamps, and the curves under such conditions consist of a series of steps, the influence being constant over each interval between lamps. The diagrams in figure 5, also involve the assumption that the wave-shape characteristics of all the lamps are identical.

It is evident from figure 5 that the lowest residual voltage occurs where the lamps are staggered, that is where adjacent lamps are in opposite sides of the circuit, and where the circuit is ungrounded or grounded at a balanced point only. For a large number of lamps spaced at finite distances and arranged as above, the residual voltage in each interval between lamps is, theoretically, one-half the voltage generated by a given lamp. The sign of this voltage is opposite in adjacent intervals. In practice, however, there may be considerable differences in the magnitudes and phases of the harmonics resulting from different lamps as well as differences in the capacitance to ground of the two wires (due to the presence of other conductors, etc.) or even differences in the leakance of the two wires to ground. The theoretical reductions in residual voltage due to staggering the lamps, therefore, may not be fully realized. In one case tested, involving an ungrounded circuit 6.6 miles long with staggered lamps, the residual *Kv·T* at the supply end was found to be about 13 per

---

† Vector sum of the voltages to ground of each wire.

INDUCTION FROM RESIDUAL OR BALANCED VOLTAGE →		CASE						
		1	2	3	4	5	6	7
		RESIDUAL	BALANCED	BALANCED	RESIDUAL	BALANCED	BALANCED	BALANCED
								
LIGHTING CIRCUIT →		GROUND RETURN	METALLIC	METALLIC	GROUND RETURN	METALLIC	METALLIC	METALLIC
SPACING →			WIDE	NARROW		WIDE	NARROW	WIRES AT ROADWAY SPACING
		JOINT USE			ROADWAY SEPARATION * *			JOINT USE AND ROADWAY
CIRCUIT		METALLIC NOISE IN NOISE UNITS						
SIDE	1-2	70	24	17				
	3-4	2	16	8				
	5-6	55	55	6				
	7-8	30	16	2				
	9-10	25	24	2				
PHANTOM	1-4	200	100	46	105	12	5	50
	7-10	4	100	0	12	1	0.5	8
	5-16	230	0	24	60	7	3	90
		LONGITUDINAL NOISE IN NOISE UNITS						
SIGMA	1-20	14	0	0.4	2.5	0.3	0.2	6

* FOR 2-WIRE CIRCUIT, BALANCED KV·T BETWEEN WIRES; FOR GROUND RETURN CIRCUIT, RESIDUAL KV·T FROM WIRE TO GROUND

* * 30-FOOT SEPARATION BETWEEN TELEPHONE AND LIGHTING CIRCUIT WIRES, WITH GROUPS 1-4 NEAREST LIGHTING CIRCUIT

Figure 6. Calculated noise induction in untransposed telephone circuits exposed to a series sodium lighting circuit

cent of the balanced *Kv·T*. While direct measurements of the residual voltages were not made at other points along the line, tests under various grounding conditions indicated that the distribution of the residual *Kv·T* was similar to that shown in the upper right-hand diagram of figure 5. A comparison of the value of 50 per cent for residual *Kv·T* shown in this diagram (for lamps all in one side of the circuit) with the measured value of 13 per cent indicates a four-to-one reduction in the maximum value obtained by the staggered arrangement of lamps. The net effect is small near the center of the line, since the residual *Kv·T* approaches zero for either lamp arrangement.

As indicated in figure 5, when the lamps are staggered, the presence of a ground at any point other than at the far end of a series circuit (or at the midpoint of the supply transformer) greatly increases the residual voltage. A practical example of such an effect was experienced in one situation where an accidental ground on one side of a six-mile lighting circuit increased the phantom-circuit noise on an exposed telephone toll line from about 400 noise units to the order of 2,000 noise units.

In some cases lighting loops are laid out on an all-metallic basis but with the outgoing and return wires quite widely separated. Near the supply end of either the outgoing or the return wire, the residual voltage (on one wire), for a circuit having the lights uniformly spaced, is about half the total voltage across the transformer. This may be seen by a reference to the upper figure in the left-hand column of figure 5. The residual *Kv·T* is, in this case, the *Kv·T* to ground on the particular wire involved.

Inductive Coupling

Since distortion of the current wave form is not an important factor in the co-ordination of series sodium lighting circuits and exposed telephone circuits, no consideration need be given to magnetic induction from the load current. Furthermore, since the length of circuit is relatively short, the effect of the ground-return charging current resulting from the action of residual *Kv·T* can usually be neglected, especially if the circuit is reasonably well balanced to ground. Noise resulting from electric induction from the harmonic voltages on the lighting circuit is, therefore, the only component of importance. It depends upon the relative magnitudes of the balanced and residual *Kv·T*, the configuration of the exposure, and the number and locations of telephone circuit transpositions.

If either circuit is in metallic-sheathed cable, the shielding effect of the sheath practically eliminates the induction.

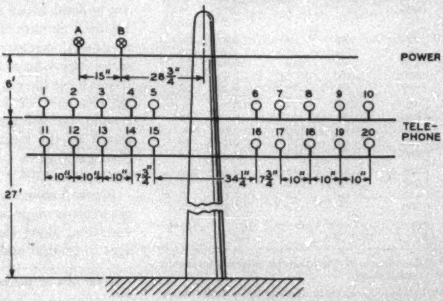
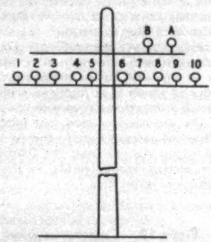


Figure 7. Exposure configuration

EXPOSURE CONFIGURATION

In order to indicate the relative influence of various configurations, calculations have been made of the noise induction for a one-kilofoot uniform section of exposure, in which the average lighting circuit $Kv \cdot T$ was taken as 100 and throughout which the telephone circuits were assumed untransposed. These noise induction data are shown in figure 6. The noise-metallic values are for a circuit terminated at each end in its characteristic impedance. The noise-longitudinal values are totals, and will divide in the two directions from the exposure inversely as the respective longitudinal circuit impedances.



$N_{nA} = 0.1 \cdot C_{nA} \cdot K_f \cdot Kv_{g_A} \cdot T$ NOISE UNITS

$N_{nB} = 0.1 \cdot C_{nB} \cdot K_f \cdot Kv_{g_B} \cdot T$ NOISE UNITS

$N_n = N_{nA} + N_{nB}$ NOISE UNITS

$N_{MS} = 0.25 (N_a - N_b)$ NOISE UNITS

$N_{MPh} = 0.2 [(N_a + N_b) - (N_c + N_d)]$ NOISE UNITS

$N_L = 0.5 (N_a + N_b) \cdot 10^{-3}$ NOISE UNITS

N_{nA}—Noise current in telephone wire n due to induction from voltage to ground of lighting-circuit wire A

N_n—Total noise current in telephone wire n due to induction from voltage to ground of both lighting-circuit wires A and B

N_{MS}—Metallic-circuit noise in side circuit terminated in its characteristic impedance (N_{MPh} same for phantom circuit)

N_L—Noise-longitudinal per wire

C_{nA}—Direct capacitance between wire n and wire A, micromicrofarads per kilofoot

K_f—Length in kilofeet of section of uniform configuration

$Kvg_A \cdot T$—Average value, in a section of uniform configuration, of product of voltage to ground of wire A in kilovolts and its TIF

a, b, c, d—Subscript letters indicating the four wires of a phantom circuit, as 1, 2, 3, 4 or 7, 8, 9, 10

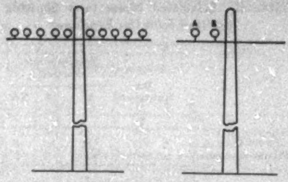
Figure 8. Formulas for estimating noise in open-wire telephone circuits in joint use with a series sodium lighting circuit

(Applying to sections of uniform configuration)

The advantage of a closely spaced all-metallic circuit with staggered lamps over a ground-return lighting circuit, at joint-use separations, may be observed by comparing cases 1 and 3 of figure 6. These figures indicate an advantage of between 5:1 and 10:1 in favor of the metallic circuit. This applies, of course, to the average noise conditions across the lead. A comparison of cases 2 and 3 indicates that, except for longitudinal circuit induction, most of the advantage of the metallic circuit is lost if the two wires are widely separated on the crossarm. The figures given for cases 2 and 3 assume the residual $Kv \cdot T$ to be negligible due to the staggering of the lamps. If a residual $Kv \cdot T$ of ten per cent of the $Kv \cdot T$ between wires were assumed, the figures for case 3 would be increased, on an average, by about ten per cent. However, this would not greatly decrease the advantage of the two-wire balanced circuit over the ground-return arrangement.

At roadway separation the data of figure 6 show the two-wire closely spaced arrangement (case 6) to compare even more favorably with the ground-return circuit (case 4). Even the wide-spaced two-wire circuit (case 5) has a decided advantage over the ground-return circuit at a 35-foot separation. Here again the figures given for the two-wire circuits at roadway separation neglect the effects of residual voltage. If a residual $Kv \cdot T$ of ten per cent were assumed, the figures for case 6 at roadway separation would be increased in a ratio of about 2:1. However, the advantage in favor of the two-wire narrow-spaced circuit as compared to the ground-return circuit would still be of the order of 10:1.

A two-wire arrangement in which one wire is located on each side of the road (case 7) is about equivalent, from the noise standpoint, to a ground-return circuit located across the road from the telephone line.



$N_{ab} = 0.1 \cdot C \cdot K_f \cdot Kv \cdot T$ noise units

$N_{MPh} = 0.2 (N_{ab} - N_{cd})$ noise units

N_{ab}—Longitudinal noise current in telephone pair ab due to induction from lighting circuit voltage*

N_{MPh}—Metallic-circuit noise in phantom circuit terminated in its characteristic impedance

C—Longitudinal circuit coupling capacitance* between a telephone pair and the lighting circuit—micromicrofarads per kilofoot

$Kv \cdot T$—Average value, in a section of uniform configuration, of product of lighting-circuit voltage* and its TIF

a, b, c, d—Subscript letters, indicating the four wires of a phantom circuit, as 1, 2, 3, 4 or 7, 8, 9, 10

K_f—Length in kilofeet of section of uniform configuration

*Note: If lighting circuit is unbalanced to ground, carry through computations both for balanced and residual voltage

Figure 9. Formulas for estimating noise in open-wire telephone phantom circuits exposed at highway separation to a series sodium lighting circuit

(Applying to sections of uniform configuration)

TELEPHONE CIRCUIT TRANSPOSITIONS

Telephone circuit transposition systems are of maximum effectiveness when (1) the power circuit influence is constant throughout the exposure, (2) the exposure configuration is uniform, and (3) neutral points in the telephone transposition layout occur at the ends of the individual exposure zones. In the case of exposures to series sodium lighting circuits, there is a continuous variation in influence from a maximum at one end to zero at the other end of the lighting circuit. However, if the second and third conditions just mentioned are realized, the variation in influence may not seriously impair the transposition effectiveness. This is because the unneutralized induction in one portion of a transposed section will generally be opposed by that in a succeeding portion. The length of section in which this takes place depends upon the transposition system and also upon the particular circuit under consideration. Thus in the exposed line system,[3] neutralization takes place between successive eight points for side circuit 1–2, while for side circuit 3–4 a half section is required. On the other hand, in the K-8 phantomed system the transposition pattern is more complex and such simple relations do not hold. This effect is illustrated by the calculated values of noise given in table II for idealized exposure conditions. A uniform joint-use exposure, having the configuration shown in figure 7, was assumed between a 6.5-mile ungrounded lighting circuit and a 20-wire telephone line. The influence of the lighting circuit was assumed balanced with the $Kv \cdot T$ varying uniformly from

Table II. Calculated Noise in a Six-Mile Idealized Joint-Use Exposure

	Untransposed	Transposition Section A	E	KA
Side circuit 1–2	2,200	25	0	15
Side circuit 3–4	950	50	80	15
Phantom circuit 1–4	5,800	1,900	0	0

800 to zero from one end of the exposure to the other. The telephone transposition arrangements within the six-mile exposure were taken as (1) untransposed, (2) the first six miles of an eight-mile *A* section (standard system[3]), (3) first six miles of an eight-mile *E* section[3] (exposed line system), and (4) a six-mile *KA* section[3] (*K*-8 phantomed system).

The magnitude of the noise for phantom circuit 1–4 in the case of the *A* section results largely from the fact that in this type of section the quarter points are not neutral points for phantom 1–4.

The degree of neutralization indicated by the tabulated values for the *E* and *KA* sections would not be expected to obtain in a practical joint-use exposure of this character, because of the effect of inevitable departures from absolute uniformity of exposure conditions and because of other differences from the idealized conditions assumed. It does appear, however, that in exposures likely to be encountered in practice the variation of the influence along the lighting circuit will be of less importance than the degree of uniformity of the exposure, the relative locations of the ends of the individual exposure zones, and the neutral points in the telephone transposition layout.

Summary of Conclusions

The following is a summary of the more significant facts brought out in the above discussion including an outline of measures which have been found effective in the noise-frequency co-ordination of series sodium lighting circuits and paralleling telephone lines:

1. The wave form of the current on a series lighting circuit supplying sodium lamps is approximately sinusoidal. The voltage wave, however, is distorted and somewhat irregular in character, roughly approximating a square wave (see figure 3). An analysis of the voltage wave indicates the presence of all the odd and many of the even harmonics of the fundamental supply frequency.

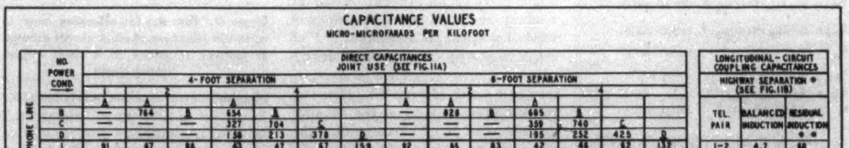
2. The wave-shape distortion on the lighting circuit is not transferred to the circuit supplying the constant-current transformer because of the relatively high leakage reactance of the latter.

3. Since only the voltage wave is distorted, electric induction in exposed telephone circuits is the only type of importance. Consequently, if either the lighting circuit or the paralleling telephone circuits are in metallic-sheathed cable, the shielding effect of the sheath will prevent appreciable noise induction.

4. The line-to-line *Kv·T* varies from a maximum at the constant-current transformer to a minimum at the far end of the circuit. The maximum *Kv·T* depends upon the number of lamps operating. However, the total *Kv·T* is not directly proportional to the number of lamps, indicating that the harmonic components from the individual lamps are not exactly equal or exactly in phase. The curve in figure 4 gives the total *Kv·T* observed on circuits supplying various numbers of lamps.

5. Since the influence varies from a maximum at the supply end to a minimum at the far end of the lighting circuit, the direction of feed may have an important effect on the magnitude of the induction. This will be most noticeable for nonuniform exposures—for example, where a section of joint use exists at one end of an exposure, the remainder of which is at highway separation. If in such a situation a choice were available as to the direction of feed, the location of the constant-current transformer at the end of the circuit remote from the joint-use exposure section would result in the lower magnitude of induction.

Figure 10. Capacitance values



CAPACITANCE VALUES

MICRO-MICROFARADS PER KILOFOOT

DIRECT CAPACITANCES JOINT USE (SEE FIG. 11A)

10 WIRE TELEPHONE LINE

NO. POWER COND. →	4-FOOT SEPARATION 1	2		4				8-FOOT SEPARATION 1	2		4			
	A	A	B	A	B	C	D	A	A	B	A	B	C	D
B	—	764		654				—	828		685			
C	—	—	—	327	704			—	—	—	359	740		
D	—	—	—	158	213	378		—	—	—	195	252	425	
1	91	67	86	43	47	67	159	92	65	83	42	46	62	132
2	81	58	78	38	43	62	144	80	56	73	37	42	56	113
3	83	59	83	38	45	67	152	80	56	74	37	42	57	106
4	97	69	99	44	54	82	162	92	65	84	45	51	67	113
5	118	83	118	56	69	103	167	108	76	97	54	59	75	111
6	178	130	166	103	115	136	117	133	98	93	77	74	81	86
7	177	134	154	111	111	115	83	126	93	101	76	70	72	67
8	191	149	145	131	110	97	64	131	101	98	84	70	66	58
9	213	173	141	157	110	87	56	180	127	115	115	87	78	60
10	258	212	154	197	123	90	60	188	156	129	142	94	81	61
Σ	1490	1900	1990	2080	2400	2310	1910	1210	1720	1790	1960	2310	2210	1770
G	920	726	670	616	461	469	636	1120	850	809	698	535	552	735
TOTAL	2410	2620	2660	2680	2870	2780	2550	2320	2570	2590	2660	2860	2760	2520

20 WIRE TELEPHONE LINE

NO. POWER COND. →	4-FOOT 1 A	2 A	2 B	4 A	4 B	4 C	4 D	8-FOOT 1 A	2 A	2 B	4 A	4 B	4 C	4 D
B	—	763		654				—	827		690			
C	—	—	—	325	704			—	—	—	380	730		
D	—	—	—	155	211	375		—	—	—	188	245	424	
1	65	47	64	30	35	51	131	71	50	67	32	35	50	110
2	56	41	57	25	30	46	118	61	43	58	27	31	44	92
3	62	44	65	27	34	54	132	61	43	58	27	32	45	86
4	75	52	78	32	43	69	143	73	51	68	34	40	55	94
5	90	63	96	41	55	85	144	82	58	76	38	45	59	87
6	147	107	142	83	101	119	90	104	74	87	57	57	64	66
7	148	111	131	92	97	100	68	105	76	82	61	57	60	53
8	159	124	121	110	94	80	48	106	78	78	67	58	53	43
9	178	147	117	134	92	71	41	141	110	97	99	73	64	46
10	219	185	130	171	105	74	44	170	136	109	121	82	68	48
11	32	25	32	17	18	25	56	37	26	33	18	19	25	33
12	23	18	23	13	14	19	40	26	19	24	13	14	18	36
13	23	17	22	12	13	18	35	24	17	21	12	12	16	28
14	27	19	26	14	18	22	36	25	18	22	13	14	17	27
15	38	27	36	20	22	30	44	31	22	26	16	17	21	29
16	49	37	42	30	29	33	35	39	28	31	23	21	24	26
17	43	33	34	28	24	26	24	35	28	26	22	19	19	20
18	49	38	35	34	26	24	19	39	30	27	24	20	18	17
19	63	52	41	46	31	28	19	52	41	34	35	25	22	18
20	95	78	56	72	44	34	24	80	64	50	56	37	31	24
Σ	1650	2040	2120	2160	2490	2420	2040	1380	1840	1910	2040	2330	2290	1870
G	780	626	560	532	398	400	536	974	741	693	610	460	471	633
TOTAL	2430	2670	2680	2700	2900	2820	2570	2350	2580	2610	2650	2790	2770	2470

LONGITUDINAL-CIRCUIT COUPLING CAPACITANCES

HIGHWAY SEPARATION * (SEE FIG. 11B)

TEL. PAIR	BALANCED INDUCTION	RESIDUAL INDUCTION **
1–2	4.7	68
3–4	2.3	33
5–6	1.8	26
7–8	1.5	21
9–10	1.75	25
11–12	3.0	44
13–14	.9	6.1
15–16	.47	6.8
17–18	.42	6.1
19–20	.76	11
21–22	2.7	37
23–24	.68	9.8
25–26	.36	5.1
27–28	.3	4.2
29–30	.53	7.6
31–32	3.2	46
33–34	1.25	18
35–36	.76	11
37–38	.57	8.1
39–40	.68	9.8
Σ	26.	400

* FOR 30-FT. SEPARATION BETWEEN NEAREST TELEPHONE AND LIGHTING CIRCUIT WIRES. SEE FIG. 12 FOR CORRECTION FACTOR TO BE USED IN OBTAINING VALUES FOR DIFFERENT SEPARATIONS.

** FOR A SINGLE-WIRE LIGHTING CIRCUIT MULTIPLY THESE VALUES BY 1.5

6. The influence of a sodium lighting circuit (assumed ungrounded) is a minimum when the two wires of the circuit are kept close together (for example, adjacent pin positions) and when adjacent lamps are connected in opposite sides of the circuit. Figure 6 gives an indication of the relative effects of various lighting circuit arrangements.

7. With the lighting circuit arranged for minimum influence, that is, with no grounds, with the wires closely spaced, and with the lamps staggered, the induction in joint-use exposures is of the order of ten times that occurring at roadway (35-foot) separation.

8. For the lighting circuit arrangement just described (item 7), telephone circuit noise results chiefly from direct induction into the metallic circuit, since the longitudinal circuit induction is relatively small. Consequently, telephone circuit transpositions are effective in reducing this noise.

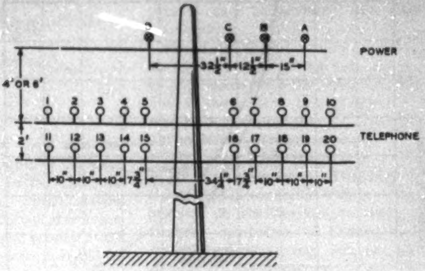
9. It appears that for reasonably uniform exposures at highway separation between open-wire telephone toll lines and a series lighting circuit, the noise induction will not be important if:

(*a*). The telephone lead is transposed according to the exposed line transposition system, or other systems having equal or greater frequency of transposition, and

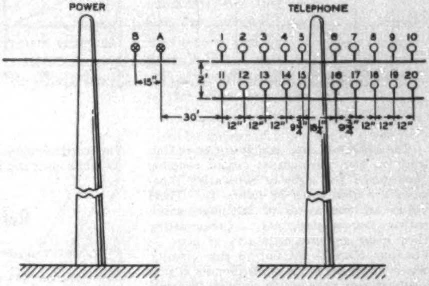
(*b*). The lighting circuit is not grounded (or is grounded at a balanced point only), the two wires of the circuit occupy adjacent pin positions, and adjacent lamps are connected in opposite sides of the circuit.

10. In situations where only a small number of sodium-vapor lamps are used as, for example, at highway intersections, noise induction is negligible.

Figure 11. Line configuration



A — JOINT-USE CONFIGURATION, TYPE B CROSSARMS



B — ROADWAY CONFIGURATION, TYPE A CROSSARMS

Appendix. Method of Estimating Noise

The usual methods of estimating noise[4] in transposed telephone circuits involve an empirical factor to take account of the effectiveness of the telephone circuit transpositions. The value of this factor depends upon the particular exposure and type of transposition arrangement. For reasonably uniform exposures with co-ordinated telephone-circuit transpositions, the factor can be obtained from accumulated experience data with a fair degree of accuracy. Where the telephone circuits are not co-ordinated or where the exposure conditions are quite irregular, the degree of effectiveness of telephone-circuit transpositions may vary over a wide range. The fact that exposures to series sodium lighting circuits may frequently involve both joint use and highway separation has made it necessary to use a method of estimating noise differing somewhat from those generally used.

The formulas employed for noise estimates in joint-use exposures are given in figure 8. The metallic-circuit noise is arrived at by first computing the total noise current to ground in each telephone wire caused by induction from the voltages to ground on each of the lighting-circuit wires. (It should be noted that where the lighting circuit is balanced to ground, the net noise current to ground in a given telephone wire is the *difference* between the induction from the two lighting wires.) This calculation makes use of the direct capacitances[5] between the lighting and telephone circuit conductors. The noise in a given metallic circuit is then obtained from the difference between the total longitudinal currents in the two sides of the circuit. This method is employed rather than a direct computation of noise metallic because of the difficulty of keeping track of the relative signs of the induction in various sections if the latter method is used.

As is indicated by the legend in figure 8, the formulas given are applied to a length of the exposure in which the configuration is uniform. The variation of the influence in such a section is taken into account by using the average value for the section. The usual type of exposure will need to be divided into a number of such sections, points of discontinuity being determined chiefly by the location of the telephone circuit transpositions and major changes in separation between the telephone circuits and the lighting circuit. In the case of toll circuits transposed as phantom groups, the computation of the total noise current in a given wire as, for example, wire 1 (N_1) may involve sections in which wire 1 is on each of the pin positions 1, 2, 3, and 4. The total noise current in wire 1 is then obtained by adding directly the components arising in the various sections.

Figure 9 shows the formulas employed for estimating noise in exposures at highway separation. These are generally similar to those in figure 8, except that the computation is carried out in terms of the balanced and residual components of the lighting circuit voltage instead of the voltage to ground of the individual wires. It will be noted that these formulas make use of "coupling capacitances" rather than the "direct capacitances" employed in the joint-use case.

Values of capacitances required in these formulas are given in figure 10 for the line configurations shown in figure 11. Direct capacitances are given in figure 10 for several joint-use exposure configurations. The "1 power conductor" data apply to a ground-return lighting circuit (or to a metallic circuit, the two sides of which are not on the same pole line) when the lighting circuit conductor is the only one on the power crossarm. If a single-wire lighting circuit were on the crossarm with three power conductors, the capacitance data for that wire would be selected from values for the "4 power conductor" case since the latter take into account the shielding effect of the other power conductors. These same considerations govern the selection of data for the case of a two-wire lighting circuit with both wires on the same pole line. While only a few of the possible number of combinations are covered, it will usually be adequate in a particular case to use the values for the condition most nearly representing the actual situation. In the practical case there are always present various irregularities in exposure conditions which it is not feasible to take into account, and hence noise estimates are necessarily approximate.

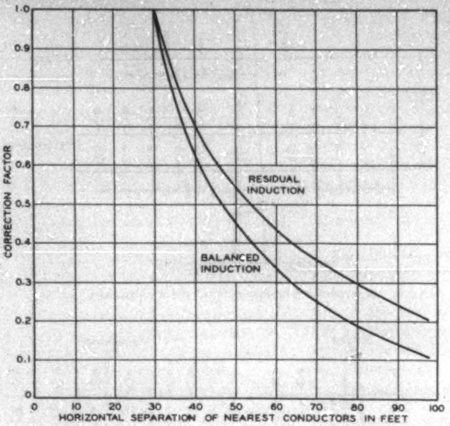


Figure 12. Correction factor for effect of separation for longitudinal - circuit coupling factors of figure 10

The available data, which are quite limited, on the longitudinal circuit coupling capacitance for highway separation exposures are also given in figure 10. These values are for a pair of telephone wires, rather than a single wire. Consequently they make possible estimates of noise in phantom circuits but not in side circuits. Since induction in phantom circuits is generally greater than in side circuits, the noise estimates will show the upper limits of noise to be expected. As an approximation, these capacitance values may be corrected for other values of separation between the lighting circuit and telephone circuits by means of the factors in figure 12. For a single-wire lighting circuit the residual induction values, increased by 50 per cent, should be used. If there are other conductors on the pole line in addition to the lighting circuit, the coupling capacitances will be reduced by the resulting shielding. This reduction may be of the order of 50 per cent.

COMBINATION OF INDUCTION FROM VARIOUS SECTIONS

In view of the apparent differences in phase of the harmonics arising in different lamps, it appears that the best method of combining the noise arising in the highway sections and the joint-use sections is to take the square root of the sum of the squares.

An opportunity to check these methods of estimating noise has been afforded in one field situation. In this case the open-wire telephone and toll circuits were transposed according to the *ABC* system and were exposed for 12 miles at highway separation and for 1.7 miles in joint use. The lighting circuit consisted of three sections, each approximately six miles in length. The two wires of the lighting circuit were located on adjacent pins (15-inch spacing) and a staggered arrangement of lamps was employed. The average contribution of the lighting circuits to the telephone circuit noise was estimated as 100 noise units for side circuits and 250 noise units for phantom circuits. The corresponding measured values were 150 noise units and 350 noise units.

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Discussion

C. W. Frick (General Electric Company, Schenectady, N. Y.): Sodium lighting is a development which utilizes the high efficiency and other desirable characteristics of gaseous-discharge lamps. It does not displace incandescent lighting since each has its own field. In common with other devices in which conduction takes place through ionized gas, either the voltage or the current is distorted, in this case the voltage. The series arrangement does not lend itself to the application of devices for wave-shape improvement.

From the co-ordination standpoint there are three types of situations: first, where the number of lamps is small and the distorted voltage has negligible effect; second, where either the lighting circuit or the paralleling telephone circuits are in metallic-sheathed cable so that shielding practically eliminates the effect of distorted voltage; third, where fairly long parallels exist between open-wire circuits and the distorted voltage may cause noise induction under some conditions.

In the early installations the situations were either of the first or of the second type, and this still applies to a majority of the installations. A good example is the illumination of a traffic circle, an intersection, or a grade crossing. When the utility of this type of lighting for highways had been demonstrated situations of the third type began to appear. It is worthy of note that co-operative studies were undertaken before any of these situations actually developed. The co-ordinative measures described in the paper resulted from these studies. If installations such as the one 16 miles long referred to by the authors had been planned without any thought of co-ordination, the pin position nearest the road might have been chosen for the lighting circuit and all the lamps connected in that wire. The other wire might then have been put in a similar position on the opposite side of the crossarm. The studies showed, however, that such an arrangement would not have given the best results. Actually, arrangements have been chosen which were recommended for minimizing the inductive effects. Thus the lighting circuits have been planned with wires on adjacent pins and the lamps alternated between wires, as recommended in the paper.

When an installation is contemplated where conditions of the third type exist, the data in the paper should be carefully considered from the point of view of both the lighting circuit and the telephone circuit. For roadway separation the problem is practically solved when the recommendations are followed. Up to the present time there has been only a limited amount of experience with these open-wire circuits at joint-use separation.

At the present time there are over 6,000 sodium luminaires in service and we know of no reports of telephone interference from this cause.

P. W. Blye: As mentioned by Mr. Frick in his discussion, it is important, where a parallel is to be created between a series sodium lighting circuit and an open-wire telephone line, that the inductive co-ordination aspects be considered in planning the arrangement of the lighting circuit. A co-operative study of the situation by the power and telephone companies involved, including an estimate of the noise induction, will indicate whether or not special co-ordinative measures will be necessary either in the lighting circuit design or in the telephone line. In cases where special measures are necessary their installation in advance of the energization of the lighting circuit will generally be found not only most economical but also most desirable from the standpoint of avoiding inconvenience to either utility. The present paper has been arranged to provide the technical data required in the co-operative consideration of such problems.

Electromagnetic Horn Design

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Synopsis: The principles of designing electromagnetic horn "antennas" to obtain beams of specified angular spread, smoothness of contour and power gain are disclosed. Quantitative curves are given from which the design of sectoral and pyramidal horns may be readily made.

THIS PAPER deals with the transmission and reception of ultrahigh-frequency electromagnetic waves by flared horns of metal. Several earlier papers (see bibliography) have described the operation and some of the applications of these new "antennas" and have reported fundamental research of both experimental and theoretical nature. In this paper, some of the more important principles of design will be discussed. Numerical data based on theoretical considerations will be presented that permit the specification of horn dimensions for given radiation performance.

General Considerations

The electromagnetic horn comprises a formed sheet of conducting material flared from a "throat" or small end to a "mouth" or large end. In applications to transmitting, electromagnetic energy delivered to the throat propagates through the interior of the horn to the mouth as "horn waves." At the mouth, substantially all of this energy is radiated as free-space or ordinary radio waves. In applications to receiving, a similar but reverse process occurs. It may be shown by the general theorem of reciprocity for electromagnetic systems, that a given horn will have characteristics as a receiver similar to those that it has as a transmitter, hence the material to follow applies to both applications.

Although a wide variety of shapes is possible, a shape having a rectangular cross section perpendicular to the central axis of the horn is preferable, because it is capable of producing a linearly polarized wave. The rectangular shape also permits an independent control of the width of the radiated beam in the horizontal and vertical planes. Horns having straight sides in the longitudinal cross section have been most used, because of the economy and ease of construction and because thorough analysis of this structure has been made. Pyramidal and the sectoral horns, having both sides flared and having two opposite sides flared and the other two opposite sides parallel, respectively, are the most important examples. The analysis of this paper applies directly to sectoral horns and indirectly to those of pyramidal shape.

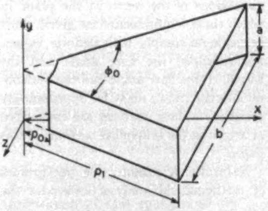

Figure 1. Sketch of sectoral horn showing dimensions

The material from which the horn is made may be any of the highly conducting metals. Galvanized iron sheeting and thin electrolytic copper foil cemented to plywood have both proved satisfactory. Screen or other semiopen construction may also be employed. Dielectric supports and insulators are not required in any region of intense field, consequently dielectric losses are low. For protection from weather, however, the mouth may be tightly covered with a dielectric material, such as silk, fabric, or plywood.

Two general methods of exciting the horn are available; namely (1) hollow-pipe feed, and (2) direct excitation. In the first case a hollow-pipe transmission line is connected to the throat, and in the second case an exciting rod or other radiating means, such as a vacuum tube, is disposed directly in the throat. The first method is mainly useful for wave lengths less than about 20 centimeters, but the second method is applicable to longer waves. Experiment has shown that substantially the same radiation patterns may be produced with either arrangement.

Figure 12 shows a large pyramidal electromagnetic horn that was designed in accordance with the principles outlined in this paper. It is constructed of plywood and copper foil. It is designed for optimum sharpness of beam in the vertical plane when operated at a wave length of 50 centimeters. This horn was constructed in connection with a research program on the instrument landing of airplanes conducted at Massachusetts Institute of Technology for the Civil Aeronautics Authority.

The sketch of figure 1 shows a sectoral horn and serves to define the following quantities: flare angle ϕ_0, horizontal aperture b/λ, vertical aperture a/λ, radial length ρ_1/λ, and the cut-off length ρ_0/λ, where λ is the wave length. The same unit of length is used for all dimensions and for the wave length.

Propagation within the horn may take place by means of any of several different types of horn waves, or by a combination of them. Which of the types is present depends on the configuration of the exciting system at the throat or of the waves delivered there by a hollow-pipe transmission line, and also on the flare angle ϕ_0 and the cut-off length ρ_0/λ. Expressions for the field configurations and transmission properties of these waves may be obtained by solving Maxwell's equations in cylindrical co-ordinates and satisfying the boundary conditions on the surfaces of the horn.[6] In general, two distinct groups of waves result; namely, E waves, having no radial component of magnetic intensity, and H waves, having no radial component of electric intensity.[*] For most applications of the horn, the H waves are employed, particularly the two waves of lowest order, $H_{0,1}$ and $H_{1,0}$. The reason for this choice is found in the fact that the configuration of the field of these waves inside the horn is such as to produce single-lobe beams of linear polarization in the radiated wave.

Sketches of the field configurations of the $H_{0,1}$ and the $H_{1,0}$ waves are reproduced in figure 2. The electric intensity in the $H_{0,1}$ wave is everywhere parallel to the y axis, is uniform in intensity in this direction but has a half-sinusoid distribution in intensity along an arc between the two flared sides. The magnetic lines lie in planes perpendicular to the y axis. This wave may be excited by a current-carrying rod in the throat disposed par-

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Paper number 39-45, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 5, 1938; made available for preprinting December 30, 1938.

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[6] For all numbered references, see list at end of paper.

[*] Two subscripts, m and n, are required to define a particular wave. These subscripts give the number of half sinusoids in the field distribution between the two parallel sides and between the two flared sides, respectively.

allel to the y axis, or by feeding an $H_{0,1}$ wave into the throat from a rectangular hollow pipe. In the $H_{1,0}$ wave, the electric lines of force lie along arcs between the two flared sides; they have a uniform distribution along the arc, but a half-sinusoid distribution in the y direction. The magnetic lines lie in planes passing through the y axis. This second type of wave may be excited by a current-carrying rod disposed centrally in the throat parallel to the x, z plane and along an arc about the vertex, or by feeding an $H_{1,0}$ wave into the throat from a rectangular hollow pipe. Both waves have constant phase on cylindrical surfaces about the y axis.

Design Considerations

In the design of electromagnetic horn radiators, two aspects of the horn are of fundamental importance. The first of these has to do with the excitation within the horn of the desired type of wave to the exclusion of waves of other types. In addition to the provision of an appropriate disposition of the exciting rod or rods, it is also necessary to make the size of the throat and the radial length of the horn of such values that the desired wave only will be produced for radiation at the mouth. The determination of these values will be discussed in the section entitled "Throat Design." The second important aspect of design concerns the radiation into space of a beam that meets the given requirements as to smoothness, sharpness, and concentration of the radiant energy in one direction. Two of the dimensions of the horn, in the plane in which these requirements are given, must be made to comply with definite values. For example, the flare angle and the length, or the flare angle and the horizontal aperture, etc., must be appropriately designed. These matters are considered at length in the remaining sections of this paper.

Naturally, a considerable background of mathematical analysis antecedes the quantitative curves and the discussions of this paper. This analysis, which is in part published elsewhere,[3-6] has been omitted from this paper in order to emphasize the design of actual horns for specific applications. Experimental measurements have been likewise omitted, although on hand in abundance. Some experimental results have already been published.[3-5]

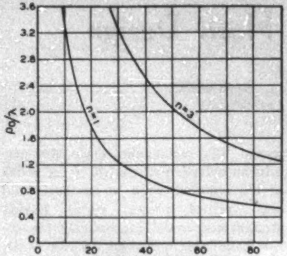

Figure 3. Throat design curves

Optimum cut-off length ρ_0/λ for the $H_{0,1}$ wave ($n = 1$) and the radial extent of the high attenuation region for the $H_{0,3}$ wave ($n = 3$)

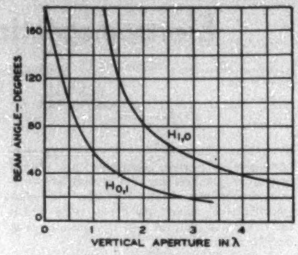

Figure 4. Beam angles in the vertical plane for $H_{0,1}$ and $H_{1,0}$ waves

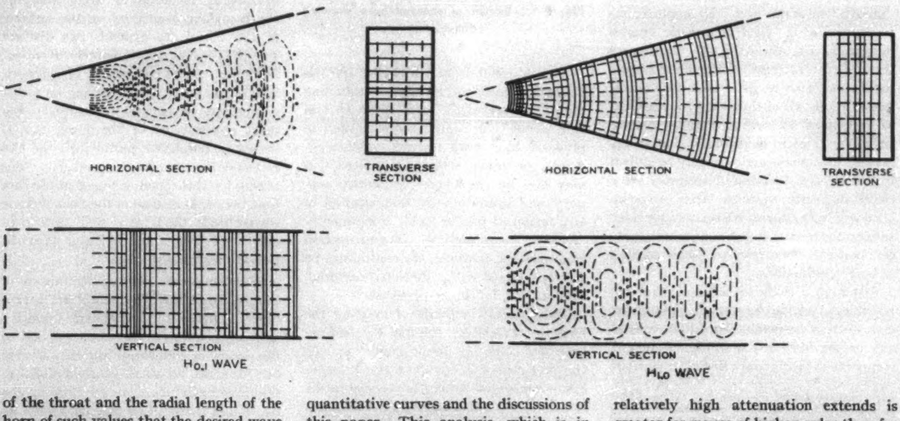

Figure 2. Field configurations for $H_{0,1}$ and $H_{1,0}$ waves in a horn

Solid lines represent electric lines of force and dotted lines represent magnetic lines of force

Throat Design

In the vicinity of the exciting rod, a plurality of the different types of horn waves will be generated. In many applications a single wave, say $H_{0,1}$, should obtain near the mouth, as the radiation pattern will be distorted by the presence of other waves. A single wave near the mouth can be realized by appropriate design of the throat. The magnitude of the attentuation of each wave is relatively large near the apex but is progressively smaller at greater radial distances. The radial distance to which the region of relatively high attenuation extends is greater for waves of higher order than for waves of lower order. Consequently, in a horn of given flare angle, a particular value for the cut-off length ρ_0 can be given that permits the $H_{0,1}$ wave to reach the mouth substantially unattenuated but which affords almost complete at-

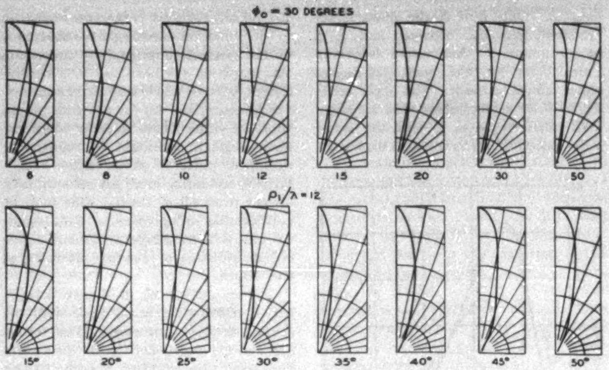

Figure 5. Two typical series of horizontal radiation patterns for the $H_{0,1}$ wave

Upper series for constant flare angle of 30 degrees and variable radial length. Lower series for constant radial length of 12 and variable flare angle

tenuation or filtration of higher order waves. This value of ρ_0 will be referred to as the "optimum cut-off length." Horns for the production of single-lobe smooth beams should have their cut-off lengths not too different from this optimum value. Figure 3 shows graphically the relation between the optimum cut-off length and the flare angle for the $H_{0,1}$ wave. For comparison, the radial extent of the high-attenuation region for the $H_{0,3}$ wave is given by the curve $n = 3$. The vertical dimension a affects neither the transmission characteristics of the $H_{0,1}$ waves nor the filtration of higher order waves except when $a > > \lambda$.

When radiation by means of the $H_{1,0}$ wave is desired, the $H_{3,0}$, $H_{5,0}$, . . . waves must be eliminated or filtered, which may be achieved by adjusting the dimension a. The $H_{1,0}$ wave is able to travel freely in the radial direction when $a > \pi/2$, but the $H_{3,0}$ wave requires $a > (3\lambda)/2$ for free transmission, etc. Therefore, to eliminate the higher order waves, the dimension a must be slightly greater than one-half wave length but less than three-halves wave lengths. The $H_{2,0}$ and $H_{4,0}$ waves can be prevented by constructing the exciting system with even symmetry about the plane $y = a/2$ equidistant from the two parallel sides of the horn.

Special cases may arise wherein several wave-types may be used simultaneously. It will be assumed in the remainder of this paper, however, that the construction of throat and exciting means is such that either an $H_{0,1}$ or an $H_{1,0}$ wave alone exists in the horn.

Radiation Characteristics

When the horn waves reach the mouth, they become free from the guiding surfaces and spread out as radiant energy. The relative amplitude of the electric field intensity on a sphere of fixed radius large compared to wave length and aperture comprises the space radiation pattern. The radiation pattern along the intersection of this sphere and the x, y plane will be referred to as the vertical pattern and that along the x, z plane as the horizontal pattern. The discussions of this paper are confined to these two plane radiation characteristics.

In evaluating the effectiveness of a given horn to produce a directed beam of radiation, recourse to the following knowledge is useful: (1) the detailed shape of the radiation pattern, such as the presence and relative amplitudes of secondary lobes; (2) the angular width of the beam, or the "beam angle," defined here as twice the angle measured from the forward axis of the radiation pattern to the nearest radial line in this pattern along which the magnitude of the electric field intensity is ten per cent of its value on this axis; and (3) the relative power gain, defined as the ratio of the power radiated from a dipole to that radiated from the horn to produce, in each case, the same magnitude of electric field intensity at a fixed remote point on the x axis.

The radiation patterns were computed by means of Huygens' principle from the distribution of the Hertzian vector at the mouth.[3] This distribution was assumed to be the same as that which would exist at the plane of the mouth were the horn infinitely long: experiments have justified this assumption for most practical cases. The radiation patterns were plotted both in rectangular and in polar co-ordinates, and the beam angle was measured from the rectangular plots. The power radiated by the horn was obtained by integrating the Poynting vector over the mouth with the field at the mouth adjusted to give unity power density at a fixed distance r on the x axis from the origin. The power radiated by the dipole for the same effect is $(8\pi r^2)/3$. Al-

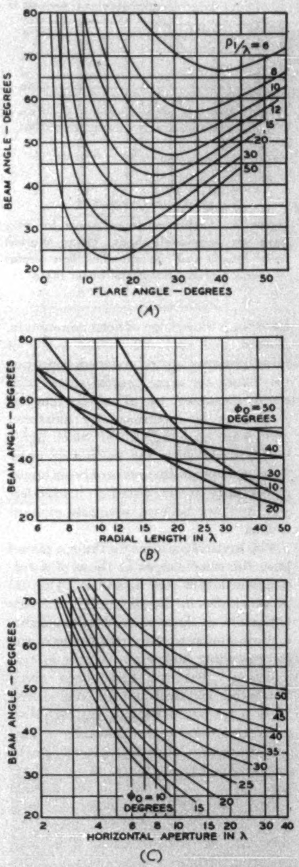

Figure 6. Beam angle in horizontal pattern for $H_{0,1}$ wave versus (A) flare angle, (B) radial length, and (C) horizontal aperture

though the power gain obtained in this way does not include copper losses in either horn or dipole, it is believed sufficiently accurate for most purposes.[6]

In the course of this research over a hundred radiation patterns were calculated for a wide range of horn parameters, plotted, and analyzed. Only a few of these patterns are reproduced here, but they show the same general shapes and trends possessed by all of the curves. A number of experimentally measured curves are on hand,[5] all of which agree satisfactorily with the calculated ones. Thus, the composite curves of beam angle, power gain, etc. are based on a considerable and, we believe, adequate amount of data.

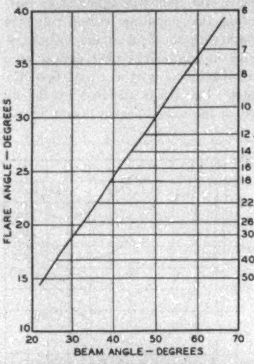

Figure 7. Optimum design curve for $H_{0,1}$ wave on beam-angle basis, giving shortest radial lengths and corresponding flare angles for a specified horizontal beam angle

The vertical patterns (in the x, y plane) have the same shapes as those of a rectangular hollow-pipe radiator. The explanation lies in the fact that the distributions of the horn waves in the y direction are similar to the distributions of the corresponding hollow-pipe waves in this direction. Patterns have been given elsewhere[3] and will not be reproduced here. The shape of the patterns comprises a principal lobe centered on the x axis and secondary lobes of relatively small amplitude. The sharpness of the principal lobe depends mainly on the vertical aperture a/λ. Figure 4 shows curves of beam angle versus vertical aperture for both $H_{0,1}$ and $H_{1,0}$ waves.

$H_{0,1}$ WAVE

Two typical series of horizontal patterns for the $H_{0,1}$ waves are shown in figure 5. Only half of the beam is reproduced, but the opposite half is a mirror image of that which is given. Calculations for the back 180-degree sector cannot be made. The upper series for $\phi_0 = 30$ degrees shows the variation with radial length ρ_1/λ, and the lower series for ρ_1/λ shows the effect of flare angle ϕ_0. A glance at these patterns reveals that (1) for constant flare angle the sharpness of the beam is improved by increasing the length of the horn, up to a certain length, beyond which very little improvement occurs, and (2) for constant length there is a value of flare angle for which the beam angle is maximum. Although the radiation patterns show slight irregularities, secondary lobes are substantially absent from all of them. This fact, of considerable significance to some applications, is attributable to the half-sinusoid distribution of electric intensity at the mouth.

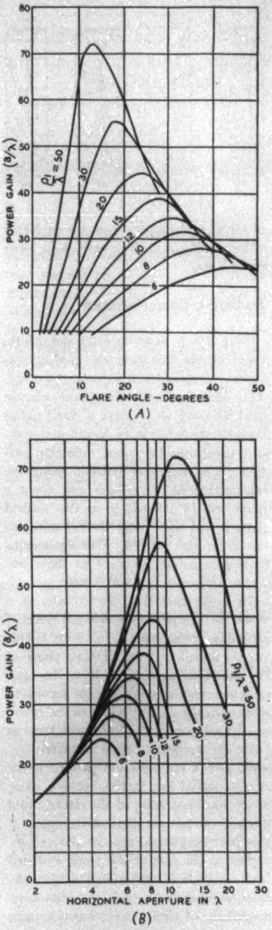

Figure 8. Power gain for $H_{0,1}$ wave versus (A) flare angle and (B) horizontal aperture

The values of power gain as read from the ordinate scale must be multiplied by the vertical aperture a/λ of the horn

Curves are shown in figures 6A, B, and C relating the beam angle with the several design parameters. The trends (1) and (2) mentioned in the preceding paragraph may be traced in each of these curves. In particular, each curve in A has a minimum, and a line connecting these minima would be approximately a straight line through the origin. Such a straight line defines the shortest horns that may be employed to produce a beam of specified angle. A corresponding envelope line could be drawn in B.

Optimum conditions may be expressed numerically by plotting associated values of optimum flare angle and length versus the beam angle, as has been done in figure 7. This important design curve permits the ready specification of optimum horn dimensions for a beam of given angle.

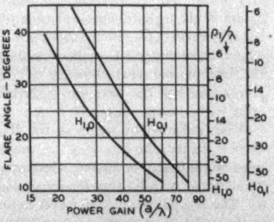

Figure 9. Optimum design curve for $H_{0,1}$ wave and $H_{1,0}$ wave on a power-gain basis, giving shortest radial lengths and corresponding flare angles for a specified power gain

The values of the power gain as read from the abscissa scale must be multiplied by the vertical aperture a/λ of the horn

Curves showing the variation of power gain with flare angle for a number of constant radial lengths are reproduced in figure 8A. Here, too, one finds that for a horn of any given length there is an optimum flare angle. In this case, it provides maximum power gain. The values of the maxima occur at smaller flare angles for increasing lengths of horn. The magnitude of the maximum power gain increases with increasing length. Figure 8*B* shows the variation of power gain with horizontal aperture. If the abscissas are multiplied by a/λ, this curve will show the variation of power gain with the transverse area of the mouth. Clearly, the gain is not a simple function of the mouth area.

The peaks of the curves in figure 8 provide optimum design conditions on a power gain basis. These optimum values of flare angle and length that provide maximum power gain are plotted in figure 9. This curve, as well as the accompanying one for the $H_{1,0}$ wave, allows the specification of optimum horn dimensions for a given power gain and supplements the curve of figure 7.

The absolute magnitude of the power gain is plotted for a vertical aperture a/λ = unity, but one should note that the power gain is directly proportional to the vertical aperture. Consequently, the values of power gain given in the curves are to be multiplied by the value of a/λ of the horn in question. Clearly, quite enormous power gains may be obtained. For example, for a horn of length $\rho_1/\lambda = 50$ having an optimum flare angle of 14° and a vertical aperture of a/λ of 10, the power gain is 720. A horn of these dimensions is entirely feasible and of moderate size at wave lengths of, say, ten centimeters. The power gain of the horn shown in the accompanying photograph is calculated to be 50.

$H_{1,0}$ WAVE

Two representative series of horizontal radiation patterns (in the x, z plane) are shown in figure 10. The upper series is for horns of a constant flare angle of 35 degrees and the lower series for horns of a constant radial length of 15. The same general trends are found here that were noticed in figure 5 for the $H_{0,1}$ wave. However, the order of magnitude of the secondary peaks in the patterns is considerably greater with $H_{1,0}$ waves. For horns of equal length, increasing the flare angle from a small value at first will sharpen the principal part of the beam. It also increases the magnitude of the secondary peaks. For sufficiently large flare angles, these secondary peaks become larger than the principal beam. As a consequence, the beam becomes broader as the flare angle is increased. For horns of constant flare angle the tendency is also observed for the beam to broaden as the length of the horn is increased. However, for sufficiently great lengths, the width of the beam is substantially equal in magnitude to the flare angle.

The explanation of the exaggerated secondary peaks in the pattern of the $H_{1,0}$ wave lies in the uniform distribution of the field across the mouth in the horizontal direction and the abrupt discontinuity at the edges. The irregular shape of the horizontal patterns makes it impractical to define a beam angle and reference must be had to the actual radiation patterns.

Power gains for the $H_{1,0}$ wave are given by the curves of figure 11*A* and figure 11*B*. The general behavior of these curves is similar to those of the curves for the $H_{0,1}$ wave. The small oscillation of power gain at large flare angles is associated with the shift of the energy from principal lobe to secondary lobe, as described above. The power gain is a

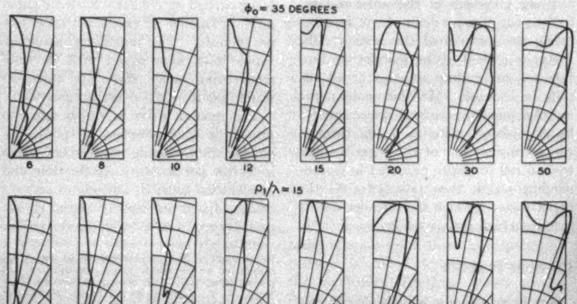

Figure 10. Two typical series of horizontal radiation patterns for the $H_{1,0}$ wave

Upper series for constant flare angle of 35 degrees and variable radial length. Lower series for constant radial length of 15 and variable flare angle

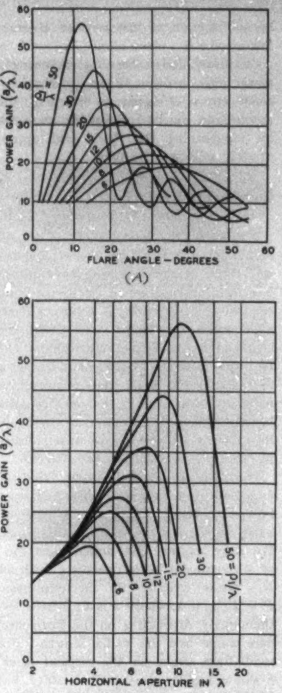

Figure 11. Power gain for $H_{1,0}$ wave versus (A) flare angle and (B) horizontal aperture

The values of power gain as read from the ordinate scale must be multiplied by the vertical aperture a/λ of the horn

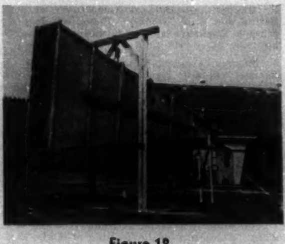

Figure 12

linear function of the vertical aperture a/λ for the $H_{1,0}$ wave also.

Optimum design for $H_{1,0}$ waves on a power gain basis is shown by the indicated curve of figure 9. Clearly, the power gain can be indefinitely increased by lengthening the horn and appropriately reducing the flare.

Concluding Remarks

Comparing the operation of the sectoral horn with the $H_{0,1}$ and the $H_{1,0}$ waves we arrive at the following conclusions. The $H_{0,1}$ wave gives a clean-cut beam comprising a principal lobe without appreciable secondary lobes, and the beam angle can be well controlled. The $H_{1,0}$ wave also gives a clean-cut beam for flare angles less than those required for maximum power gain, but an irregular beam for greater angles. On a power gain basis, there appears to be substantially no difference between the two waves.

Horns of pyramidal shape; that is, flaring in both transverse directions, may be designed on an approximate basis by means of the data presented in this paper. We may express the power gain $P_{0,1}'$ of a pyramidal horn operating with an $H_{1,0}$ wave roughly by the expression $P = kP_{0,1}\, a/\lambda$, where k is a numeric less than unity depending on the horizontal flare angle and the radial length. For small flare angles in the vertical plane, k will not differ greatly from unity, but for greater flares it will be substantially less than unity. When using an $H_{0,1}$ type of wave in a pyramidal horn, the vertical flare angle should be kept small if a clean-cut vertical radiation pattern is desired.

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Electric Drives for Supercalenders in Paper Mills

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A TYPICAL electric drive for a modern 84-inch supercalender and winder consists of a 200-horsepower adjustable-speed driving motor, two 20-horsepower motors for winder drums, a 150-kw variable-voltage motor generator set with 30-kw winder generator and exciter, full magnetic control with large motor-operated rheostat, and an operator's panel. The drive will provide a threading speed of 50 feet per minute and a wide range of many productive calendering speeds to a maximum which may be 2,000 feet per minute. With auxiliary equipment on the winder slitters, and pressure-applying arrangement, the operator has full control of the calender speed, starting and stopping, roll pressures, and winder tensions at a central point.

In the last two decades progress in the development of electric drives for paper-mill supercalenders has resulted in their adaptation to many existing calenders as well as in their selection for new calender installations.

The Supercalender

The supercalender is a machine consisting of a vertical stack of smooth-surfaced rolls, usually alternately steel and composition fiber, either cotton or paper, with means for applying and regulating pressure on the top roll, thereby creating pressures in the order of one-half to one ton per inch of roll face between the surfaces of the several rolls. Power is applied to the shaft of the bottom roll to produce rotation of all the rolls in contact. Machine-made paper or subsequently processed paper such as heavily coated material is run through the successive nips of the supercalender from a roll of paper mounted in an unwinding stand, then rewound. As the paper passes through the calender rolls it receives a high surface finish.

Previous Practice

For many years supercalendering was carried on with the paper traveling not over 500 feet per minute. Machines were provided with belt drives from mill line shafts, and mechanical clutches. Threading of the web of paper through the rolls to get each roll started was accomplished by slipping the clutch, or in some cases by the use of back gearing. Each machine was limited in respect to its productive speed. Higher productive speeds were seen to be desirable to increase calendering operations with such grades of paper as could be properly finished with faster operation, but some grades of paper could not be handled and properly finished at increased speeds. This situation called for means for selecting calendering speeds to suit a variety of conditions. At this time it was recognized that with some grades of paper, slower productive speeds might result in less loss of product and less interruption due to paper tearing and the web breaking, and particularly that means were needed for providing a stable, very slow, calender speed for threading, because considerable material and time were being lost because of paper breaks occurring in the course of getting paper started through the calender.

Essential Requirements

As adjustable-speed drives were applied to supercalenders to make available higher speeds, improvements took place in the design of bearings and methods of lubrication, and in calender roll properties. By 1926, supercalendering was being carried on regularly by some paper mills at speeds in excess of 1,000 feet per minute. The machinery manufacturers have kept apace with developments and today there are calenders which operate at 2,000 feet per minute.

The modern drive is called upon to provide a wide selection of speeds, including the threading requirement which is 50 feet per minute. Acceleration and deceleration must be smooth to prevent pulling apart the web of paper in the calender nips and to avoid uneven pull in

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Paper number 39-43, recommended by the AIEE committee on industrial power applications, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 23, 1938; made available for preprinting December 20, 1938.

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the span of paper from the unwinding roll to the calender, and in the span from the calender to the roll winding-up. Quick stopping is essential in order to keep the passage of wrinkled paper between the rolls to a minimum when a paper break does occur because the calender's composition rolls are made of compressed cotton or paper. The smooth surfaces of these rolls become marred ultimately from wrinkled paper in the machine and time taken for regrinding rolls cuts into the productivity of the calenders.

Electric Drive System

In the course of securing adjustable speed by electric drives and working up from the previous 500 feet per minute maximum productive speed, there have been several methods employed. Where direct current was available or provided the shunt-design d-c motors were used and productive speeds obtained by field weakening the motor, with constant armature voltage. Threading speed was obtained by reduced armature voltage using external resistances in series with and shunted across the armature. Variations of this system to serve several drives in the same calendering department involved the use of two or more voltages in the power circuit, for example both 250 and 125 volts for the motor armature with further range of speed adjustment by field weakening available with the armature connected to either voltage, and 35 volts as a source of armature voltage for threading operation eliminating external armature resistance.

Drives were developed to operate on a-c circuits, these being of the wound-rotor induction-motor type. The selections of speeds for productive operation were made with adjustable secondary or rotor resistance. Threading speed was provided, either by use of an auxiliary squirrel-cage induction motor, driving through a suitable reduction and overrunning gear, or by a separate reduced-frequency and alternating-voltage circuit for the wound-rotor motor.

The variable-voltage, or adjustable direct-voltage system was introduced for paper mills not having direct current available, but where the characteristics of the d-c motor were wanted. This involved a motor generator set operating on the mill a-c circuit, with rheostatic field control of the generator to provide adjustable-output direct voltage for the armature circuit of a shunt-design d-c motor used for driving the supercalender. A separate exciter, usually coupled into the motor generator set, furnished a source of constant voltage for the shunt field and control circuits. Threading speed was provided by impressing a low voltage on the motor armature, smooth acceleration and deceleration by adjusting the voltage gradually and smoothly over a wide range between minimum and maximum available armature voltage.

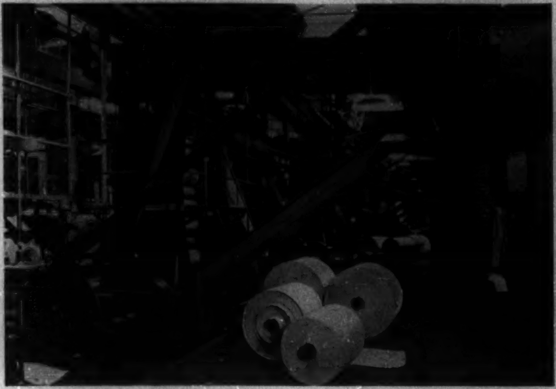

Figure 1. Belt drive for supercalender, commonly used before development of modern electric drives

The recent supercalenders installed for operation over wide speed ranges to maximum speed of 1,200 to 2,000 feet per minute are equipped with variable-voltage drives. The use of a low armature voltage to the motor, or of an auxiliary threading motor with suitable reduction gears and an overrunning clutch, are alternate methods of obtaining the threading speed. In many cases, speed adjustment by motor field weakening supplements the range of speed change available by voltage control giving the drive the characteristics of the shunt d-c motor with constant armature voltage over the normal productive range of calendering speeds.

Load Characteristics

The power requirement of a supercalender is made up of three principal components, (*a*) the work done in overcoming the mechanical frictional losses in the bearings and gearing, (*b*) the work done in the constantly changing deformation taking place in the calender rolls in rotation under contact pressure, and (*c*) the work done on the paper being calendered.

From a large number of observations studied, it appears that for a given set of conditions each component has a constant torque characteristic wherein the horsepower requirement is directly proportional to the linear speed. This pertains over the productive speed range of the calender within the normal capacity of the machine in respect to the pressures applied to the rolls, roll condition, bearing design and lubrication. Tests have shown repeatedly that when the normal conditions for which a machine is designed are exceeded, the load requirement at increased speeds rises abruptly. Recognizing the constant torque characteristic for a given set of conditions, the maximum torque requirement of a supercalender is in some cases that required to drive the calender with heavy roll pressure for a hard finish on a relatively heavy paper normally calendered at some intermediate speed rather than the torque required for calendering the grades of paper which are run through at the maximum linear speed.

The following load information was reported on a modern nine-roll calender, 75 inches wide, equipped with antifriction bearings, the loads expressed in terms of calender roll face and linear speeds:

1. Running without paper and with no pressure applied in addition to weight of rolls themselves............0.031 horsepower per inch face per 100 feet per minute
2. Running without paper but with pressure applied of 1,780 pounds per inch width in addition to weight of rolls...0.091 horsepower per inch face per 100 feet per minute

3. Running with paper 65$^{1}/_{2}$ inches wide, basis weight 35 pounds per ream of 500 sheets 24 by 36 inches, with pressure applied of 1,780 pounds per inch width. . 0.132 horsepower per inch face per 100 feet per minute

This is typical in respect to the division of the total load requirement of a supercalender.

Figure 2. Electric drive for supercalender and two-drum winder having individual motor driving each winder drum

Operator's panels at left mount starting and stopping push-button stations, switches for individual slitter motors, instruments indicating calender speed and motor load, and controls for adjusting roll pressure and winder tension

Supercalenders made up of alternate steel and composition cotton rolls and used for calendering heavily coated paper require more power than calenders with steel and composition paper rolls used for putting a higher finish on paper as it comes from a paper machine without any further processing, as indicated by a comparison of the following load factors which are averages of observations taken over a long period on several antifriction-bearing calenders:

Cotton and steel rolls—medium weight papers, normal pressures. . . . . . 0.140 horsepower per inch face per 100 feet per minute

Paper and steel rolls—medium weight papers, normal pressures. . . . . 0.10 horsepower per inch face per 100 feet per minute

Supercalender Winders

Many supercalenders are equipped with the conventional type of two-arm rotating reels for mounting the unwinding rolls and the cores on which the paper is rewound. A friction brake on the shaft of the unwinding roll produces tension in the web of paper as it enters the calender, and the shaft of the rewinding roll is driven by a belt from a pulley on the shaft of the bottom roll of the calender. As some paper-mill managements found it possible to improve calendering operations with improved equipment and to supply their customers with the paper rolls as they were rewound at the calender without further rewinding to remove imperfect paper, other types of winders were brought into tandem operation with the calenders. These provide for trimming the web of paper and slitting it to desired widths in rewinding.

Surface-Type Winders

There are various designs such as the four-drum winder having an individual motor driving each drum, the two-drum and single-drum winders which wind by the surface principle, the motors for which are controlled automatically to keep in step with the main motor driving the calender and to produce desired tension for handling the sheet properly and securing good finished rolls. Many of these winders are driven by d-c motors with adjustable-voltage control for the overall linear speed range with supplementary or vernier speed control actuated by a counterweighted roll or dancer roll, over which the sheet passes as it travels from the calender to the winder. One electrical manufacturer has supplied a winder system making use of wound-rotor induction transmitter driven by the calender motor and the wound-rotor induction receiver mechanically connected to drive the winder. The primary or stator circuits are commonly excited from the mill a-c circuit and a wound-rotor induction differential is introduced in the rotor circuit. By properly controlling the angular position and rotation of the differential unit, the receiver gains on the transmitter from which it is principally controlled sufficiently to develop the tension desired for proper winding of the roll.

Figure 3. Modern supercalender with variable-voltage d-c driving motor

Magazine reel holds unwinding roll, additional rolls to follow in process, and the electric drive for shaft of core on which paper is rewound

Center-Driven Winders

Some supercalenders are equipped with motor-driven winders of the center or core-shaft drive type. With these the motor driving the winding roll must continually fall off in speed as the roll of paper increases in diameter while no change is taking place in the linear speed of the calender, and simultaneously uniform tension of the sheet must be maintained to produce a good roll. The prevailing practice of using three-inch and four-inch diameter cores for paper rolls for the printing industry has been a limitation preventing the adaptation of tension-controlled motor drives using a field-control d-c motor and automatic regulator working in the field of the motor to maintain constant armature current because the full rolls are 32 inches to 40 inches in diameter. The same limitation has hindered the application of corresponding equipment on unwinding roll for controlling tension automatically in the sheet entering the calender. Field regulation over a motor or generator speed change of ten to one would be required and this is recognized as a wider range than is available in commercial machines today. The motors which have been applied to shaft or center-driven winders operating in tandem with supercalenders for winding rolls where the ratio of full roll diameter to core diameter is ten to one are of the d-c series design operating in conjunction with specially compounded generators. The regulation of the motor field strength and armature voltage is accomplished inherently in the system by the changing armature current which increases as the roll increases in diameter.

Conclusion

Supercalenders and their winders are outstanding examples of process machines for which motors are called upon to provide unusual flexibility. In addition to transmitting power, the electrical equipment is a production tool vital to the performance of the machine.

Discussion

D. R. Shoults (General Electric Company, Schenectady, N. Y.): We are indebted to Mr. Smith for an interesting résumé of past and present practices in driving supercalenders and their auxiliary equipment.

There have been a number of types of drives used for supercalenders, most of which were reasonably successful when operating at speeds of 500 feet per minute or less, but without question the recent emphasis on high calendering speeds, with a consequent emphasis on ease of acceleration and retardation with readily adjustable operating speeds, has required that the drive used be unusually flexible. The natural trend in such a situation, as Mr. Smith has pointed out, is toward the use of the adjustable-generator-voltage-controlled drives. Such drives lend themselves very well to co-ordinated winder drives or electrical braking systems for unwinding stands.

Another factor which has become of increasing importance as speeds are increased is that of nip pressure between the rolls. In order to obtain equivalent finish on the paper at high operating speeds, the operators have found it necessary to increase the nip pressure considerably above the values common a few years ago. A bottom roll nip pressure of at least three times the pressure resulting from the weight of the rolls themselves is today common practice. Such high pressures give increased roll deformation and naturally influence materially the power requirements of the drive.

Mr. Smith cites a 75-inch-wide supercalender with nine rolls which has a power constant of 0.132 horsepower per inch face per 100 feet per minute. The pressure is 1,700 pounds per inch of width which is assumed to be at the bottom nip. I would like to know whether this calender was equipped with paper or cotton composition rolls. If these rolls are of paper, I believe that that particular requirement is typical, but if they are of cotton, it seems that this value is somewhat low. Mr. Smith subsequently states that *average* observations show that with paper and steel rolls and with *normal* pressure, 0.10 horsepower per inch face at 100 feet per minute is the average requirement. I would like to know what pressures Mr. Smith considered as normal pressures and whether he makes any distinction as to the number of rolls in the stack. The calender stacks have generally 9 to 11 rolls and it is my observation that the power requirement is a direct function of the number of nips in the stack as well as the speed and pressure.

With an up-to-date roller-bearing-equipped calender stack, the four variables are: speed, width, number of nips, and the *average* nip pressure per inch of width. These factors are all essentially linear. Figure 1 of this discussion shows the results of a number of observations correlated as to power requirements as a function of average nip pressure. The average nip pressure is taken, rather than the maximum pressure which occurs at the bottom nip, as being more indicative of the over-all operation of the stack.

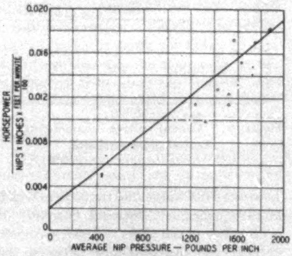

Figure 1. Supercalender power-pressure data; paper rolls

These observations, taken on seven different arrangements of supercalenders, show a reasonably close co-ordination when plotted in this manner. If the supercalender which Mr. Smith first cited is assumed to be equipped with paper and steel rolls as were the rest of these, this point corresponds with the others.

Inasmuch as the usual supercalender stack has at least nine rolls, Mr. Smith's constant of 0.1 horsepower per inch face will give on this curve, a constant of 0.0125 horsepower per nip which would be equivalent to an average nip pressure of from 1,200 to 1,600 pounds per inch. The bottom nip pressure, which is more commonly used in expressing pressures, may be from 10 to 20 per cent higher than this.

If an 11-roll stack is assumed, the maximum pressure will be from 950 pounds to 1,350 pounds per inch nip. On this basis, it does not seem that this power constant is adequate for the modern calenders where pressures certainly do approach 2,000 pounds per inch face; furthermore, it appears that power is a very definite function of the number of rolls in the stack and this should be taken account of in general recommendations.

There is one other point worthy of comment and that is that my experience shows no great variation of power with respect to the weight of paper being used, but that the major variation is in the nip pressure. It is true that higher nip pressures may sometimes be used with the heavier grades of paper, but it is more likely that high nip pressures will be used where it is desired to obtain a highly finished paper at high calendering speeds. The finish obtained seems to be a function, not only of nip pressure, but of speed of operation. The paper as it passes through the stack is subjected to a steam bath and the finish depends upon the amount of moisture absorbed. Naturally, the slower the stack runs the more moisture will be absorbed and the higher the finish. A higher nip pressure can offset a reduction in moisture as far as finish is concerned and accordingly, the tendency is to use higher nip pressures to get an equivalent finish when high speeds are required.

Experience with supercalenders operating at speeds from 1,000 to 2,000 feet per minute is not yet extensive enough to indicate clearly definite relation between optimum nip pressure and operating speed. Some operators feel, as Mr. Smith points out, that the greatest pressures may be used at a relatively low speed while others indicate that the maximum pressure may be required to obtain a sufficient finish on any paper calendered at the maximum speed. A more complete investigation of this factor is desirable and in any case, specific knowledge or assumption in this connection is required to choose properly between the constant-field or adjustable-speed motors. Both types of drive operate successfully within their power limits.

The power data presented herein should be supplemented by further observations before any general recommendations are arrived at and it is hoped that interested operators will in the future present such data.

R. H. Smith: The calender cited with a power constant of 0.132 horsepower per inch face per 100 feet per minute is equipped with composition paper rolls. In the subsequent statement, referring to average observation, pressures in the order of 1,200 to 1,600 pounds per inch at the bottom nip were considered normal and a nine-roll calender was assumed.

Supercalendering operations are being carried on at higher pressures between the rolls and calender builders have been progressive in developing means for applying and controlling these higher pressures. The roll makers are called upon to provide composition paper and cotton rolls capable of withstanding these pressures without increased deformation and deterioration.

Anticipation of the load requirement of a calender does require consideration of operating pressures. Means for measuring pressures have been none too reliable, which factor, coupled with the variable of roll hardness and surface condition, seems to prevent establishing rigid rules for absolute power determination at this time.

Cold-Cathode Gas-Filled Tubes as Circuit Elements

S. B. INGRAM
MEMBER AIEE

SINCE the discovery by Hull[1] that oxide-coated cathodes could be used as commercially practical thermionic emitters in gas-filled tubes, these tubes have found extensive use. For high-voltage d-c power supplies two-element mercury-vapor rectifiers have almost entirely replaced rotating machines and the earlier high-vacuum tube rectifiers. The addition of a grid to the gas-filled thermionic rectifier yields the thyratron which is used in regulated rectifiers, for the inversion of direct current to alternating current, and as a sensitive relay in numerous industrial control circuits. The discovery by Slepian and Ludwig[2] of the ignitor principle in initiating the arc spot on a mercury-pool cathode provided a ready means for controlling current flow in tubes with mercury-pool cathodes and the ignitron has now shown itself to have a large field of application.

The present paper will concern itself with gas-filled circuit elements of a third type, cold-cathode tubes. The principles of operation of these devices are not new[3] but their wide application has awaited the development of tubes which will operate in low-voltage circuits of, say, 150 volts or less.[4] Functionally, these tubes have much in common with thyratrons but their current-carrying capacity must remain limited because energy dissipation at the cathode of a glow discharge restricts the current which may be drawn without overheating the surface. However, as control devices in that large class of circuits where milliamperes rather than amperes are required, cold-cathode tubes are capable of performing many of the circuit functions of thyratrons and possess several major advantages over tubes of the hot-cathode type.

General Characteristics of the Glow Discharge

The properties of cold-cathode tubes in which we are interested follow immediately from the well-known characteristics of the self-sustaining glow discharge.

Consider two electrodes in a gas at a reduced pressure. If a low potential is applied between them the few ions which are always present in the gas become multiplied by secondary ionization resulting from impacts of the original electrons and ions with gas atoms and a small current flows through the tube. The gas conduction at this stage is known as the Townsend discharge and the current which flows is called a Townsend current. It is generally of the order of a microampere or less. As the potential between the electrodes is increased the current increases and at some potential, the breakdown voltage, the gas becomes a good electrical conductor. The voltage across the electrodes now assumes a lower value, which we will call the sustaining voltage, at which it remains practically constant and independent of the current, the magnitude of the current being determined by the external resistance of the circuit. Figure 1 shows schematically the current-voltage characteristic of such a discharge tube.

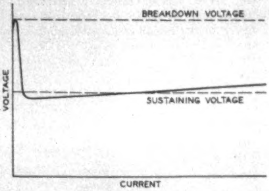

Figure 1. Current-voltage characteristic of typical glow discharge

Cold-Cathode-Tube Structure and Characteristics

Figure 2 shows a photograph of a typical cold-cathode tube. The structure consists of three elements, a cathode, an anode, and a control anode. The cathode is a nickel surface coated with a mixture of barium and strontium produced by reduction from the oxides of these metals by positive-ion bombardment in a glow discharge. The anode is a plain nickel wire shielded from the other elements except for its extreme end by a glass sleeve and separated from them by a spacing of about one-half inch. The control anode is placed close to the cathode. In the particular tube illustrated this element is identical with the cathode and therefore these two elements may be used interchangeably. Such construction, however, is not necessary and in other tubes the control anode may be a wire or small surface near the cathode. The gas filling is a mixture of the rare gases, 99 per cent neon—1 per cent argon at a pressure of about 40 millimeters of mercury. Variations in the constitution of this gas mixture may be used to control the electrical characteristics of the device. In such a three-element cold-cathode tube we have to deal with two conduction paths. That between the cathode and the main anode we will call the main gap and that between the cathode and the control anode the control gap. The nominal characteristics and ratings of this tube are given in table I.

Such a tube has many interesting properties as a circuit element. Basically it may be made to perform three distinct circuit functions, those of a relay, rectifier, and voltage regulator. We shall consider these in turn.

The Cold-Cathode Tube as a Relay

Figure 3 shows a typical cold-cathode-tube relay circuit. The supply potential must be greater than the main gap sustaining voltage but less than the main gap breakdown voltage so that no conduction will normally occur. If now a potential exceeding its breakdown voltage is applied to the control gap the resulting ionization will initiate conduction in the main gap and the anode potential will fall to the main gap sustaining voltage, the current being limited by the external circuit impedance. Conduction will continue until the circuit is opened or the anode voltage maintained below the main gap sustaining voltage for a sufficiently long time for the tube to deionize. The control gap current required to cause breakdown in the main gap we will call the transfer current. In magnitude it does not greatly exceed the Townsend currents flowing just below the breakdown voltage. For the tube described it is in general less than one

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Paper number 39-41, recommended by the AIEE subcommittee on electronics, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 21, 1938; made available for preprinting December 22, 1938.

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[1] For all numbered references, see list at end of paper.

microampere. The cold-cathode tube is thus a very sensitive relay.

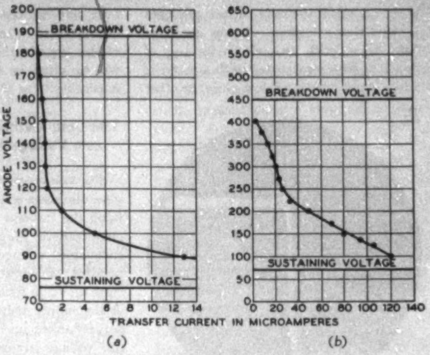
The transfer current is a function of the anode voltage. It must obviously be zero at the main-gap breakdown voltage and approach infinity at the main-gap sustaining voltage. Figures 4*a* and *b* show the transfer-current characteristic of the tube illustrated in figure 2 with two different gas fillings. Neon with a one per cent admixture of argon gives a very low transfer current. One hundred per cent argon gives a much higher transfer current and a higher main-gap breakdown voltage.

In using cold-cathode tubes as relays we must consider not only the amplitude of the signal, which together with the control-gap circuit impedance determines the current available for transfer of the discharge, but also its duration. For the Western Electric 313*C* tube it is found that signals of a duration of 200 microseconds are long enough to give reliable operation even when the amplitude of the signal is small. For signals of greater amplitude a shorter signal duration will suffice.

Figure 4. Transfer current characteristic of cold-cathode relay tube

(*a*)—Gas filling, 99 per cent neon, 1 per cent argon, 40 millimeters

(*b*)—Gas filling, 100 per cent argon, 15 millimeters



Deionization times are of the order of ten milliseconds, somewhat too long for satisfactory operation on 60 cycles. Tubes with shorter deionization times may be made by using gas filling of pure argon but this may be done only at the expense of increasing the transfer current. Thus while such tubes are faster in their operation as relays they are less sensitive.

The stability and reproducibility of tube characteristics are of interest in circuit design. The important characteristics are the control-gap breakdown and sustaining voltages and the main-gap sustaining voltage. The stability of the main-gap breakdown voltage may vary without detrimental effect on the circuits provided it does not fall to the supply voltage. Several years operating experience with tubes of the 313 type have shown that in general the three characteristics mentioned above will remain within plus or minus five volts of their initial values over several thousand hours of operating life if the current ratings are not exceeded. Deterioration of the tubes or shifts in characteristics on standing are negligible. Variations in initial characteristics will depend upon the degree of manufacturing control and, in general, will not exceed plus or minus ten volts.

One limitation on the use of glow discharges as relays should be noted. Oscillation, inherent in the glow itself, and resulting in "noise," makes gas tubes unsuitable to carry voice currents in communications circuits. This restriction, of course, applies to all other gas discharge devices including thyratrons. For control and signalling applications, however, this noise is unimportant.

The Cold-Cathode Tube as a Rectifier

The second circuit function which can be performed by means of cold-cathode tubes is rectification. Rectification requires an asymmetry in the current-voltage characteristic of the rectifying circuit element. When such an asymmetry exists a symmetrical voltage wave applied to the elements results in an asymmetrical current wave in the circuit. The cold-cathode-tube rectifier depends for its operation upon an asymmetrical property of the glow discharge itself. The sustaining voltage is primarily determined by the cathode fall of potential which in turn is largely dependent on the nature of the cathode material. In general low cathode falls are associated with surfaces of low work function. The slope of the current-voltage characteristic depends not only upon the nature of the cathode surface but also upon its

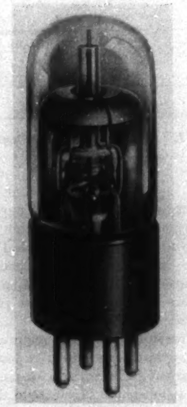


Figure 2. Cold-cathode relay tube

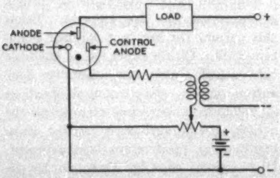


Figure 3. Cold-cathode-tube relay circuit

Table I. Characteristics and Ratings of Western Electric 313C Tube

Control-gap breakdown voltage	70 volts
Control-gap sustaining voltage	60 volts
Main-gap breakdown voltage	175 volts
Main-gap sustaining voltage	75 volts
Transfer current at anode voltage of 130 volts	5 microamperes, maximum
Deionization time	
Main gap	10 milliseconds
Control gap	3 milliseconds
Maximum instantaneous cathode current	30 milliamperes
Maximum average cathode current	10 milliamperes
Maximum time of averaging cathode current	1 second
Maximum instantaneous reverse current in main gap	5 milliamperes

area. A small cathode surface produces a steep characteristic. A glow-discharge tube then, with two electrodes one of which is large and coated with a material whose work function is low while the other is small and uncoated, will have an asymmetrical characteristic.

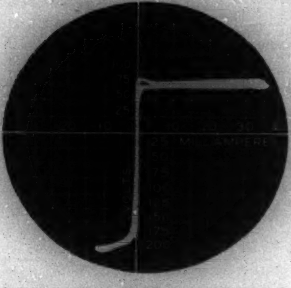


Figure 5. Asymmetrical current-voltage characteristic of cold-cathode rectifier tube

The current-voltage characteristic of a Western Electric 313*C* tube connected as in the circuit of figure 6 is shown in figure 5. The trace was obtained by means of a cathode-ray oscillograph, the applied voltage being 208 volts root-mean-square, the load resistor, R_1, 5,000 ohms, and the control-gap resistor R_2, 100,000 ohms. The rectifying properties of the main gap are apparent from this characteristic. The control gap is connected into the circuit only to provide a low starting voltage in the forward direction.

Rectification can also be obtained using two-element tubes, the low starting voltage being produced by designing the tubes so that the anode-cathode spacing is small and the breakdown voltage therefore low. However, cathode sputtering from the small electrode on to the neighboring active surface causes the voltage characteristic of such tubes to be unstable so that three-element tubes are to be preferred.

As rectifiers cold-cathode tubes may be used either to convert an a-c power supply to direct current or to discriminate between positive and negative polarity in a circuit. In power supplies for radio receiving sets several tubes have attained some importance in the past,[5] although unsatisfactory tube life, radio-frequency noise arising from the discharge, and the inherent inefficiency resulting from the large voltage drop have prevented their wide adoption and in recent years their importance has been declining. As polarity detectors, however, cold-cathode tubes have a wide field of usefulness. One extensive application of this type in the communications field will be cited in the latter part of this paper.

The Cold-Cathode Tube as a Voltage Regulator

The voltage-regulating property of the glow discharge is well known. It is based upon the flatness of the current-voltage characteristic shown in figure 1. The sustaining voltage is practically independent of current. Thus in the circuits shown in figures 7*a* and *b* variations in the supply voltage will be practically entirely taken up in the series resistance, the voltage across the tube remaining constant.

Commercial voltage regulators are available which regulate at 60, 70, 90, 110, 130, and 150 volts. All such tubes may be operated in series to obtain regulation at higher voltages. The regulated voltage will in general vary less than five per cent from no load to full load and variations from tube to tube can usually be held to within ± five per cent of the nominal values. The tube illustrated in figure 2 may be used to regulate at either 60 or 75 volts, the sustaining voltages in the control and main gaps, respectively. The two circuit connections are illustrated in figures 7*a* and *b*. In the circuit of figure 7*b* the relay principle has been used to reduce the starting voltage exactly as it is when the tube is used for rectification.

Comparison of Ignitron, Thyratron, and Cold-Cathode Tube

In the introductory paragraph of this paper the functional similarity of the three types of gas-filled control tubes, thyratrons, ignitrons, and cold-cathode tubes, was stressed. These devices have this fundamental property in common. Each one contains a control element, the grid in the thyratron, the ignitor in the ignitron, and the control anode in the cold-cathode tube. Positive potential may in each case be applied to the anode without initiating gas conduction provided the control element is held more negative than some critical value. If, however, this critical value is exceeded, breakdown occurs and the gas becomes conducting. After conduction begins the control element loses all sensible control over the discharge which may be extinguished only by maintaining the anode below the sustaining voltage long enough for the tube to deionize.

Since they have these basic properties in common it is not surprising that the three types of gas-filled control tubes should be functionally similar in their circuit applications. It is scarcely too strong a statement to say that any *type* of circuit set up with one of these circuit elements can also be set up with either one of the others. In spite of this, however, they are not competitive with each other but rather complementary in their functions. This is because, while they are basically similar in principle, their operating characteristics are so widely different that generally in any particular case no doubt will exist as to which device is the *practical* one to use. Table II summarizes in a brief though necessarily incomplete way the comparative characteristics of the three classes of gas-filled control tubes.

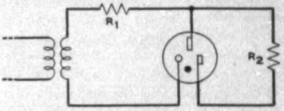


Figure 6. Cold-cathode-tube rectifier circuit

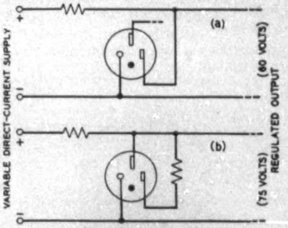


Figure 7. Cold-cathode-tube voltage-regulator circuits

The brevity of the table requires that something further be said in explanation.

In current-carrying capacity the ignitron is an inherently high-current device, the mercury-pool cathode being capable of supplying thousands of amperes of current. On the other hand, there exists a minimum current for stable operation, of approximately five amperes. Below this current the arc spot will not maintain itself. On the other end of the scale cold-cathode tubes are inherently low-current devices. At currents above about 20 milliamperes per square centimeter the active cathode surface is rapidly disintegrated by positive-ion bombardment. High current capacity then can be obtained only by the use of cathode areas

Table II. Comparison of Gas-Filled Control Tubes

Characteristics	Ignitron	Thyratron	Cold-Cathode Tube
Current capacity	5–10,000 amperes	Up to 100 amperes	Up to 100 milliamperes
Deionization time	10^{-4} second	10^{-4} second	10^{-3} second
Ionization time	10^{-6} second	10^{-6} second	10^{-4} second
Cathode heating time	0	Finite	0
Deterioration in standby service	No	Yes	No
Accuracy of characteristics	Variable	±2 volts	±10 volts
Sustaining voltage	15 volts	15 volts	75 volts

Note: Where specific values are given these are only approximate and are cited only to make a quick comparison possible.

of impractical size. Existing tubes are capable of supplying peak currents of approximately 100 milliamperes.

In speed of ionization and deionization the ignitron and the thyratron are much faster than the cold-cathode tube. This is because they are low-pressure devices. Argon-filled cold-cathode tubes are faster than those filled with neon-argon mixtures in which the neon predominates. The transfer currents, however, are also much higher so that in the argon-filled tubes sensitivity must be sacrificed in return for speed of operation.

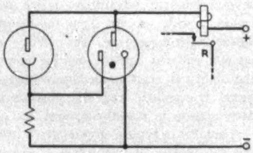


Figure 8. Photoelectric relay

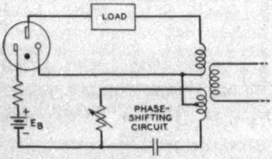


Figure 9. Cold-cathode-tube circuit with phase control

The ignitron and the cold-cathode tube are, except for a brief ionization time, instant starting devices. The thyratron, on the other hand, requires a cathode heating time to bring the cathode to operating temperature. When the thyratron is used as a relay, the cathode must be maintained continuously at electron emitting temperature even though the periods of operation are brief and intermittent. This implies continuous deterioration of the tube even during standby periods. The necessity of supplying cathode power is itself a disadvantage. In service where the operation is intermittent much longer tube life is to be expected with cold-cathode tubes than with thyratrons.

As a voltage-operated device the thyratron is the most sensitive of the three. Its critical characteristic will generally be held within an extreme range of ± two volts. The variations in breakdown voltage of existing cold-cathode tubes will exceed this by a factor of five. Variability in ignitor rod materials causes ignitron characteristics to be most variable in this respect. For this reason an ignitron is generally controlled by an associated thyratron circuit so that the amplitude of the ignitor current may be made to exceed the critical value by a large margin while the thyratron is used as the voltage-sensitive element.

The sustaining voltage of the discharge is low, approximately equal to the ionization potential of the gas in both the ignitron and the thyratron. In the cold-cathode tube it is higher, approximately equal to the cathode fall of potential. This quantity is characteristic of the cathode material and the gas and is in general lowest for the alkalis and alkaline-earth metals associated with the rare gases. In practical applications the convenience of operation of the cold-cathode tube, since it requires neither filament transformer nor filament power, more than compensates for its lower efficiency resulting from high tube voltage drop.

Circuit Applications of Cold-Cathode Tubes

In cold-cathode-tube circuits as in thyratron circuits the problem of extinguishing the discharge, once it is initiated, must be met. The means available are four in number. The first is the use of an alternating anode voltage. After the removal of the control anode voltage conduction will then continue until the applied anode voltage falls to the sustaining voltage. Reignition will not occur on the next positive cycle if the frequency is low enough to allow the tube time to deionize. The second method is the opening of the anode circuit by a switch or relay contact. Again the length of the interruption must exceed the deionization time. The third is the application of a surge of negative voltage to the anode through a capacitor as in the familiar parallel type inverter circuit. The cold-cathode equivalent of this circuit is illustrated in figure 10 and will be described hereinafter. The fourth is the overshooting of the voltage due to the inductance of the circuit and the dynamic characteristic of the tube when a capacitor is discharged through it. This will be illustrated in considering the relaxation oscillator.

The basic relay, rectifier, and voltage-regulator circuits using cold-cathode tubes have been illustrated in figures 3, 6, and 7. Figure 8 shows a simple cold-cathode photocell relay. Increase of light intensity will cause the relay, *R*, to operate. Means will have to be provided for opening the anode circuit to reset the device for a second operation.

If an alternating voltage is applied to the anode of a cold-cathode tube and an alternating voltage of variable phase applied to the control anode, phase control of the output can be obtained. The circuit is shown in figure 9 and is quite analogous in its operation to phase-control circuits using thyratrons. The bias, E_B, on which the alternating voltage is superimposed should be somewhat lower than the control-gap sustaining voltage.

Figure 10 shows a cold-cathode-tube square-wave oscillator which is the ana-

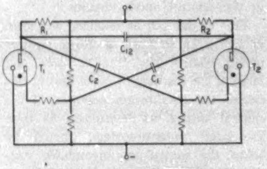


Figure 10. Square-wave oscillator

Note the similarity to the self-excited parallel-type thyratron inverter

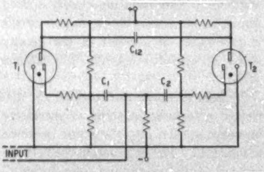


Figure 11. Counting circuit

Note the similarity to the Wynn-Williams "scale of two" circuit

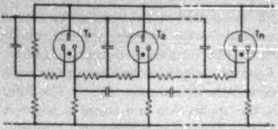


Figure 12. Chain counting circuit

logue of the self-excited parallel-type thyratron inverter. Such a circuit, of course, would not be used for the conversion of d-c power to a-c power since the cold-cathode tube is not a power device. It may be used, however, to generate a block wave to key another circuit. The operation of such a circuit is well known but will be described here since the principles are of general applicability in a variety of other circuits. Assume tube T_2 to be conducting, tube T_1 nonconducting. On the firing of T_1 its anode voltage will drop suddenly from the supply voltage to the sustaining voltage of the tube. This negative surge will be transmitted through the commutating capacitor C_{12}, extinguishing T_2 by driving its anode potential below the sustaining voltage and maintaining it there for a time determined by the time constant R_2C_{12}. A similar surge through C_2 reduces the control anode voltage of T_2 below the control gap breakdown voltage. C_2 charges up through the control gap resistors and T_2 fires when this voltage is again reached. The discharge will thus transfer back and forth between T_1 and T_2 at a rate dependent on the values of the constants in the control anode circuits.

This circuit can be modified as shown in figure 11 so that incoming positive pulses control the transfer back and forth of the discharge. Note that in this circuit the equilibrium potential of the control anodes, as determined by their associated potentiometers, should be below the control-gap breakdown voltage rather than above it as in the preceding circuit. The functioning of this circuit is analogous to that of the familiar "scale of two" counting circuit of Wynn-Williams.[6] For counting closely spaced impulses, of course, the thyratron circuit is preferable on account of the greater speed of operation of the hot-cathode tubes.

A chain of tubes, down which the discharge progresses in steps as successive impulses are fed into the input is shown in figure 12. The drop across the cathode resistor of the conducting tube "primes" the next tube in line so that it, rather than any other, is fired when the next impulse arrives. A potentiometer across tube T_1 gives its control anode an initial priming so that the first incoming pulse fires this tube preferentially. Detailed consideration of the behavior of thyratron circuits of this general class has been given recently by Shumard.[7]

Any glow discharge tube may be used in a relaxation-oscillator circuit. The simplest form of such a circuit is shown in figure 13. Oscillation will generally occur without the presence of the inductance L but its insertion causes the capacitor to discharge to a voltage well below the sustaining voltage rendering certain the extinction of the tube.

A novel use of cold-cathode tubes as relays in a device for the remote control of radio receivers has recently been described.[8] From a circuit standpoint the principal feature of interest in this circuit is the use of a radio-frequency signal on the control gap to fire the tube. As might be expected the peak voltage of the radio-frequency signal required to break the tube down exceeds the d-c control-gap breakdown voltage by a considerable margin.

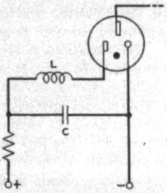


Figure 13. Simple gas-tube relaxation oscillator

What is probably the largest use of cold-cathode tubes to date is in an application in telephone communication where they have found extensive use in a four-party subscriber set for selective ringing.[9] The circuit is illustrated in figure 14. Of the four ringers, two are connected to one side of the line, two to the other, ground being used to complete the circuit. For selective operation of the two ringers on one side of the line a ringing signal is used which consists of an alternating voltage on which is superimposed a direct voltage. The cold-cathode tubes functioning as rectifiers are placed in series with the ringers and are oppositely poled in the two cases. One ringer responds to positive superimposed voltage, the other to negative.

One particularly valuable property of the tube in this application not possessed by rectifiers of other types is that as long as the breakdown voltage is not exceeded, the tube represents virtual open circuit to ground. Thus no transmission loss is experienced and no ground noise is introduced into the circuit when the line is used for voice transmission, the talking battery voltage having in general a nominal value of either 24 or 48 volts.

In the older type of equipment a-c relays were used to disconnect the ringers from the line except when ringing voltage was applied. Selection between the positive and negative polarities was obtained by means of mechanically biased ringers.

Summary

Three types of gas-filled control tubes are now in common use. The properties of cold-cathode tubes, the most recent of these to receive extensive application, have been considered and comparisons drawn with those of the more familiar thyratron and ignitron. It is concluded that in its own field of low-current control devices the cold-cathode tube has several inherent advantages which will ensure a wide use for it in the future. These advantages are, the ability to operate without cathode heating power, the ability to start immediately when a signal is applied, and the absence of deterioration in standby service.

A number of typical circuits illustrating the capabilities of the tubes as circuit elements have been described. Several of these are in commercial applications. One, involving some hundreds of thousands of tubes, has been operating for several years and proves beyond doubt that the cold-cathode tube is a valuable addition to the array of control devices available to the circuit engineer.

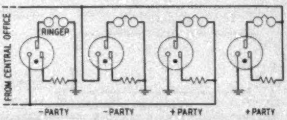


Figure 14. Four-party selective ringing circuit

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Polyphase Broadcasting

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THE universal use of amplitude modulation in radio broadcasting service has led to several formidable problems in the design of broadcast transmitting equipment. Improvements in technique which permitted the complete modulation of a carrier (100 per cent modulation) raised new problems in connection with the conversion efficiency of the final radio-frequency amplifier stage. Continued research has resulted in such developments as the high-level plate-modulated amplifier, the high-efficiency linear amplifier, outphasing modulation, and others.

In all of these high-efficiency systems, the radio-frequency output of the transmitter is amplitude modulated, and at 100 per cent modulation, the radio-frequency power output of the transmitter varies all the way from zero to four times the carrier power during each cycle of the audio-frequency modulating voltage. It is thus apparent that the final power-amplifier tubes must be capable of delivering a peak power equal to four times the carrier power of the transmitter.

The unique feature of the system to be described is the fact that the radio-frequency power output of the transmitter is constant throughout the audio-frequency modulation cycle, and is equal to $1^1/_2$ times the carrier power. This feature indicates the possibility of reducing the high-power tube complement to a very minimum, and it is conceivable that the peak power capacity of the final amplifier tubes might be as low as $1^1/_2$ times the carrier power instead of the twice carrier value of the present paper.

The comparison between the polyphase broadcasting system and the system of present-day broadcasting is somewhat similar to the comparison between a polyphase generator and a single-phase generator in that the instantaneous power output of a polyphase generator supplying a balanced load is constant, while in the case of the single-phase generator, the power varies sinusoidally from zero to twice the average power during the cycle.

It will be shown that the "rotating modulation field" essential to polyphase broadcasting may be obtained by the use of a five-element vertical antenna array. Four identical radiators are arranged at the corners of a square with the fifth antenna in the center. The central antenna is supplied with unmodulated radio-frequency power and is called the carrier antenna. The antennas at opposite ends of one diagonal constitute a directional pair and are supplied with suppressed-carrier modulated radio-frequency power. The two antennas at the ends of the other diagonal also constitute a directional pair and are fed with suppressed-carrier modulated radio-frequency power. The two antennas of each side-band pair are fed in series and carry currents of opposite phase, so that the directional patterns are figures of eight at right angles. Further, the phase of the suppressed-carrier currents in the side-band pairs differs from the phase of the current in the carrier antenna by an angle of 90 degrees. If in addition, the modulating voltage in the suppressed carrier channels is in quadrature, the required conditions, for a rotating modulation field, are fulfilled.

The Radiating System

The fundamental arrangement of the antenna system is shown in figure 1. The antenna at the point O is the carrier antenna. The antennas 1 and 1′ comprise a directional pair, and carry equal and opposite double side-band currents. Similarly, antennas 2 and 2′ comprise a directional pair and are placed physically at right angles to the first pair.

Let the following currents be established in the antenna system:

$i_O = I_O \cos \omega t$		(carrier)
$i_1 = I_1 \sin \omega t \sin \rho t$		(double side band)
$i_1' = -I_1 \sin \omega t \sin \rho t$		(double side band)
$i_2 = I_1 \sin \omega t \cos \rho t$		(double side band)
$i_2' = -I_1 \sin \omega t \cos \rho t$		(double side band)

where

i_O = instantaneous current in antenna O
i_1 = instantaneous current in antenna 1
i_1' = instantaneous current in antenna 1′
i_2 = instantaneous current in antenna 2
i_2' = instantaneous current in antenna 2′
ω = 2π times carrier frequency
ρ = 2π times modulating frequency
I_O = maximum value of current in carrier antenna
I_1 = maximum value of current in side band antennas

The field intensity E at a remote point P, with arbitrary reference angle θ and at a distance r_O meters from the central antenna, becomes:

$$E = \frac{KI_O}{r_O}\cos \omega\left(t - \frac{r_O}{C}\right) + \frac{KI_1}{r_O}\left\{\sin \omega\left(t - \frac{r_O - d\cos\theta}{C}\right) - \sin \omega\left(t - \frac{r_O + d\cos\theta}{C}\right)\right\}\sin \rho t + \frac{KI_1}{r_O}\left\{\sin \omega\left(t - \frac{r_O + d\sin\theta}{C}\right) - \sin \omega\left(t - \frac{r_O - d\sin\theta}{C}\right)\right\}\cos \rho t$$

Where K is an antenna performance constant, all antennas assumed identical, C is the velocity of light in meters per second.

The above expression after some reduction, becomes:

$$E = \frac{KI_O}{r_O}\cos \omega\left(t - \frac{r_O}{C}\right)\left[1 + \frac{2I_1}{I_O} \times \left\{\sin\left(\frac{\omega d\cos\theta}{C}\right)\sin \rho t - \sin\left(\frac{\omega d\sin\theta}{C}\right)\cos \rho t\right\}\right]$$

Now, if the spacing of the antennas be restricted so that $\sin(\omega d/c)$ differs only slightly from $\omega d/c$, we have approximately

$$E = \frac{KI_O}{r_O}\cos \omega\left(t - \frac{r_O}{C}\right) \times \left[1 + \frac{2\omega d I_1}{CI_O}\sin(\rho t - \theta)\right] \quad (1)$$

Inspection of equation 1 shows that the

Paper number 39-46, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 5, 1938; made available for preprinting January 3, 1939.

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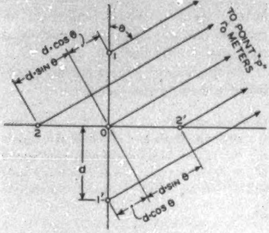


Figure 1. Antenna plan

field intensity at the point P is amplitude modulated, and that the phase of the modulation varies as the azimuth angle θ.

The modulation may be said to be 100 per cent when $(2\,\omega d I_1)/(c\,I_O) = 1$.

Further, the value of I_1, the maximum value of current in the side-band antennas is, for 100 per cent modulation

$$I_1 = \frac{C}{2\omega d} I_O$$

$\frac{\omega d}{C}$ is the antenna spacing in radians S

Hence

$$I_1 = \frac{I_O}{2S}$$

Thus, for a spacing of one-half radian, the maximum side-band antenna current is equal to the maximum carrier antenna current in the 100 per cent modulated condition.

Referring again to figure 1, the following items should be noted:

1. Radiators 1 and 1′, since they carry equal and opposite currents, induce no voltage in antennas O, 2, or 2′.
2. Radiators 2 and 2′, since they also carry equal and opposite currents, induce no voltage in antennas O, 1, or 1′.
3. Radiator O induces equal voltages in antennas 1 and 1′, and 2 and 2′. Inasmuch as these antenna pairs are fed in series, the induced voltages are in opposition, and thus produce no current.

The full significance of the above points is grasped when it is realized that the carrier antenna, side-band pair 1, 1′, and side-band pair, 2, 2′, are completely decoupled, and each may be fed by its own generator (amplifier) without affecting the others.

In the derivation of the expression for the field intensity at the point P, the formula was simplified by making the assumption that the electrical spacing was small, less than 0.5 radian. This consideration is fortunate, for in practice it means that the side-band radiators can conceivably be suspended from the main carrier radiator. For example, with a spacing of one-third radian, the following situation exists at a frequency of 1,000 kilocycles:

One wave length = 985 feet
One-third radian = 52.2 feet

The required spacing is thus 52.2 feet, which is an entirely feasible value, permitting the central radiator to support the side-band radiators.

Another desirable result of the close spacing is that the high-angle radiation distribution of the side-band system closely approaches that of the central antenna, so that the per-cent modulation is substantially equal for all radiation angles.

The spacing, however, must not become too small, for some difficulty would undoubtedly be experienced, first in the balance of the mutual effects of the independent systems, and second, the radiation resistance of the side-band pairs at the current loop diminishes approximately as the square of the spacing. For a spacing of one-third radian, the resistance at the current loop for the side-band pairs may be calculated to be 22.2 per cent of that of the central antenna. It is a rather simple matter properly to excite this side-band system.

The Transmitter

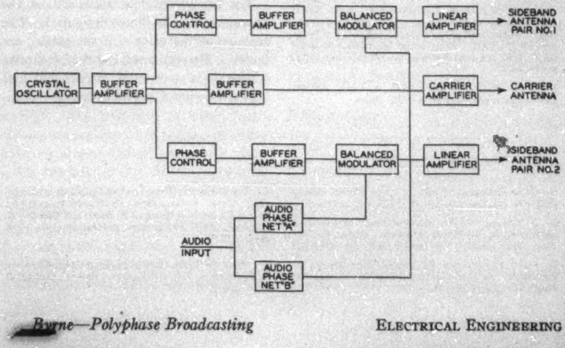
A block diagram of a typical polyphase transmitter is shown in figure 2. It is essentially a three-channel transmitter frequency-controlled from a common crystal oscillator and appropriate buffer amplifiers. The carrier channel is a straightforward continuous-wave telegraph design and sufficient amplifiers cascaded to obtain the desired carrier power level. The output of the carrier amplifier is coupled by conventional means to the carrier antenna. Each of the side-band channels consists of a final class-B radio-frequency amplifier excited by a balanced modulator. The balanced modulator, in turn, receives its excitation from the crystal oscillator through a phase-shifting goniometer, and the necessary buffer amplifiers. The output of each side-band channel is fed to the appropriate pair of side-band radiators. The phase-shifting goniometer is employed in each of the side-band channels to provide easy adjustment of phase so that the phase of the double side-band current in the side-band antennas can be set at its proper value with respect to the radio-frequency current in the carrier antenna.

Polyphase Audio System

All of the foregoing material would be an interesting academic consideration only, if it were impossible to design a satisfactory source of two-phase audio-frequency modulating voltage. Two possible systems were studied.

The first system given consideration was the following. A single-side-band signal is obtained using a 50-kilocycle suppressed-carrier modulator and appropriate filters. The resulting single-side-band signal is impressed on the grids of two demodulators, one demodulator being supplied with the original carrier, and the other with the original carrier shifted 90 degrees in phase. The outputs of the demodulators will be in phase quadrature. This system, while producing perfect quadrature voltages, is seriously limited in its low-frequency response due to the nature of the filters required. The limitation is so severe, that the best single-side-band systems today are acceptable only between frequencies of 100 and 6,000 cycles. This frequency

Figure 2. Block diagram of polyphase transmitter



band is not sufficient for high-fidelity broadcasting, and consequently, this source of two-phase audio was discarded.

The second system, and the one adopted, makes use of non-dissipative phase-delay networks of the lattice type. The audio-frequency voltage is impressed across two networks, each having three sections, and the circuit constants in each section are so chosen that the *difference* in the phase delay of the two networks is substantially 90 degrees throughout the frequency range 30–10,000 cycles.

Figure 3 is the calculated and measured performance of a pair of two-section networks. In this graph, the deviation from the desired value of 90 degrees is shown as a function of frequency. Figure 4 is the calculated performance of a pair of three-section networks.

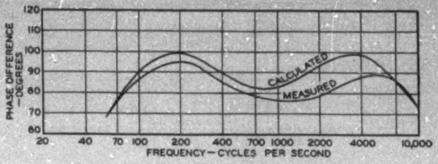


Figure 3. Performance of a pair of two-section phase-shifting networks

Practical Aspects of the System

VACUUM-TUBE COMPLEMENT

It is clear that, for a 50-kw transmitter, the carrier amplifier must be capable of supplying a peak power of 50 kw. Under no condition is its load any greater than 50 kw. Further, at 100 per cent modulation, the power output of the system is constant at $1^1/_2$ times carrier power.

The combined output of the side-band amplifiers is hence equal to one-half the carrier at every instant of the 100 per cent modulation cycle. Each of the side-band pairs is idle twice every cycle and during this time the operating side-band pair must supply all the side-band power. It is therefore apparent that the side-band amplifiers *each* have a peak capability of one-half carrier power. In the case of a 50-kw transmitter, therefore, each side-band linear amplifier must have a peak capacity of 25 kw.

The total tube capacity in the final stage of the transmitter is hence equal to two times the carrier power, in contrast with four times carrier power in present day equipment.

The reduction in the number of vacuum tubes required in the final stage of a high-power transmitter is at once apparent. Comparing a possible arrangement for a 500-kw polyphase transmitter with the arrangement at WLW, it can be shown that at least 11 of the 20 100-kw vacuum tubes now used would be eliminated. The saving in filament power consumption alone, would be of the order of 80 kw.

EFFICIENCY OF FINAL AMPLIFIERS

The efficiency of the carrier amplifier will be nominally 70 per cent so that in the case of a 50-kw transmitter, the plate input to this stage will be 71.4 kw. In the case of the side-band amplifiers, assume that the peak efficiency during a modulation cycle is 70 per cent. This efficiency will be obtained at the maximum output of 25 kw. Assuming that the plate current of the linear amplifier is proportional to the radio-frequency grid voltage, the instantaneous power input p will be

$$p = E_B I_B |\sin pt|$$

where E_B = direct plate voltage in kilovolts, I_B = maximum value of plate current in amperes. The d-c input power P is the average of the above expression.

$$P = \frac{2E_B I_B}{\pi}$$

Also it was assumed that, for 25-kw peak output, the efficiency was 70 per cent. Therefore

$$E_B I_B \times 0.7 = 25$$

The average power input to each side-band amplifier is thus

$$P = \frac{2 \times 25}{\pi \times 0.7} = 22.7 \text{ kw}$$

The average output of each side-band amplifier is 12.5 kw and the efficiency is 12.5/22.7 = 55 per cent.

At 100 per cent modulation, the total power input to the last stages is

Carrier amplifier	71.4 kw
Side-band amplifier number 1	22.7 kw
Side-band amplifier number 2	22.7 kw
Total	116.8 kw

The output, 100 per cent modulated, is 75 kw and the efficiency of conversion is 75/116.8 = 64 per cent. This efficiency figure is comparable to other high-efficiency systems.

EXCITER CIRCUIT REQUIREMENTS

In the case of the high-efficiency linear amplifier, the exciter circuit must be of sufficient capacity to drive the final amplifier to a peak output of four times carrier power. For example, in the case of a 50-kw transmitter, the exciter must be capable of substantially linear performance for a final amplifier output range, 0 to 200 kw. In the polyphase system, however, each side-band amplifier exciter is required to drive its amplifier linearly only in the range 0 to 25 kw. This fact results in a reduction in exciter capacity, and in fact, radiation-cooled tubes may readily be used for excitation of the final amplifiers for 50-kw transmitters. The exciter for the carrier amplifier may likewise consist of radiation-cooled tubes. Inasmuch as the total capability of the final amplifiers is only one-half as great as in conventional systems, the exciter capability is likewise half as large as is ordinarily required. As a matter of fact, the exciter capacity is somewhat less than one-half that required for a linear amplifier, for it is unnecessary to provide a stabilizing load for the carrier amplifier exciter.

Fidelity of Transmission

The effect of the phase of the audio signals being different from 90 degrees results in nonuniform percentage of modulation in different directions. Equation 1 was derived on the assumption that the audio-frequency modulating voltage was in phase quadrature. If the audio-frequency voltages are not in phase quadrature, the expression for the field is modified according to the following expression:

$$E = \frac{KI_O}{r_O}\cos\omega\left(t - \frac{r_O}{C}\right)\left[1 + \frac{2SI_1}{I_O} \times \left\{\sin pt\cos\theta - \cos(pt - \phi)\sin\theta\right\}\right]$$

where ϕ is the deviation from the desired 90 degrees.

Inspection of this relation shows that the greatest effect due to phase deviation will occur for θ = 45 degrees, 135 degrees, 225 degrees, and 315 degrees.

The following table shows the effect of small deviations upon the per-cent modulation at an angle θ = 45 degrees.

Phase Deviation	Decibel Change in Amplitude
+10	−0.82
+ 5	−0.38
0	0
− 5	+0.36
−10	+0.68

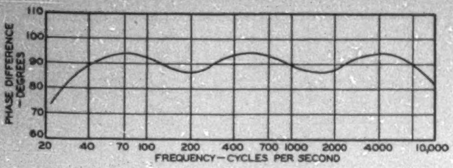


Figure 4. Performance of a pair of three-section phase-shifting networks

The change in modulation level for small deviations from the desired value of 90 degrees is thus seen to be small.

If the phase relation between the audio signals is different for signal components of different frequencies, the result in any particular direction is the same as that produced by varying frequency response in the conventional transmitter. As noted above, if the phase does not vary from 90 degrees by more than ±10 degrees the effect is a maximum variation in frequency response of less than one decibel. Thus a deviation from the desired 90-degree phase relation of the modulating voltages produces frequency distortion.

The effect of an improper phase adjustment of the side-band amplifiers with respect to the carrier is to produce amplitude distortion. To make this clear refer to figure 5. In this figure the line *OC* is the carrier vector. The side-band vectors are oppositely rotating and their sum lies along the line $S-$, $S+$. At any particular instant in the modulation cycle the sum of the carrier and the two side-band vectors might be represented by the vector *OP*. As the modulation progresses through the cycle, the point *P* moves back and forth along the line $S-$, $S-$ in simple harmonic motion. A linear detector would merely provide a current proportional to the magnitude of the vector *OP* as a function of time. Consequently if the side-band phase is incorrect by as much as 30 degrees serious amplitude distortion would occur at high modulation percentages, for the length of the vector *OP* obviously does not vary sinusoidally. On the other hand, if the side-band phase is no more than three degrees from the desired value, a glance at figure 5 is sufficient to indicate that the amount of distortion occurring would be rather small. In practice, a remote monitoring point would be selected, and the modulated radio-frequency pattern would be transmitted back to the control point at a low intermediate frequency—say 50 kilocycles. This pattern would then be viewed on a cathode-ray oscilloscope and proper adjustment of the phase of the side-band amplifiers with respect to the carrier would permit full 100 per cent modulation to be obtained.

Experimental Data

A systematic program of experimental work is in progress to substantiate the fundamental operations of the system and to make a detailed study of broadcast-signal fidelity.

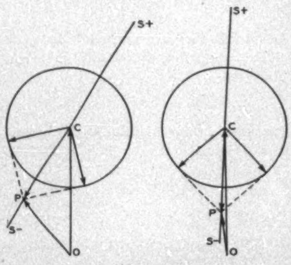


Figure 5. The effect of side-band phase adjustment

The first preliminary work was carried out with a transmitter having about 100 watts carrier power and operating on about 80 meters. Preliminary measurements showed complete accord with the above theory of operation. A check was made using two-phase 60-cycle audio frequency for modulation of the transmitter and 60 cycles from the same source was used to modulate a second conventional transmitter on a different frequency. By the use of two similar receivers the demodulated 60-cycle output from the two receivers was compared, and the phase between these two outputs varied from 0 to 360 degrees as the receiving point was moved around the antenna system.

The audio-frequency phase-difference network of figure 2 was then inserted and the polyphase transmitter operated on voice modulation. The fidelity of operation was equivalent to that of conventional transmitters. No particular attempt was made in these first experiments to measure audio-frequency harmonic distortion or to make a detailed study of fidelity. No effort was made to provide an accurately spaced antenna system or to provide low-distortion suppressed-carrier modulators in this first model.

In order to make a detailed experimental study of the broadcast signal fidelity, a second project is under way. This project consists in construction of a 1,000-watt broadcast-frequency transmitter. Experimental data will be obtained using an array of half-wave vertical radiators.

Conclusions

A new system of broadcasting amplitude-modulated radio-frequency signals has been described. It is felt that this system has great possibilities and the essential features are high efficiency of power conversion and minimum tube capacity requirements for the final amplifiers. The inherent disadvantage of such a system lies in the fact that an essential requirement is a nondirectional radiation pattern. It is felt that the complication of circuits and adjustments over that of conventional transmitters will further limit the application of this system to high powered equipment (50 kw and larger) where power consumption and tube replacement are formidable items of expense.

The technical possibility of superpower transmitting equipment of the order of 1,000 to 2,000 kw, is brought into the realm of reality using tubes now available.

A 12-Channel Carrier Telephone System for Open-Wire Lines

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Synopsis: A new carrier telephone system is described, together with its application in the long-distance telephone plant. By its use, an open-wire pair which already furnishes one voice circuit and three carrier circuits may have 12 more telephone circuits added. Thus in all 16 telephone circuits are obtained on a single pair. Several such systems may be operated on a pole line.

Various problems incident to the extension of the frequency range, from about 30 kilocycles, the highest frequency previously used, to above 140 kilocycles, are discussed. Among the more important of these are the control of crosstalk between several systems on a pole line, arrangements for taking care of intermediate and terminal cables, and automatic means for compensating for the effects of weather variations on the transmission over this wide frequency range.

BARE WIRES supported on insulators, stretched between poles, make up the pioneer electrical communication circuit, the open-wire line. Although great advances have been made in the application of cable structures, the open-wire lines still hold their own in some sections of the country. This is because, to offset their physical vulnerability, they have several unique virtues. They are flexible and permit adding one pair of wires at a time. They are also comparatively economical where conditions favor their use. Furthermore they are low-attenuation circuits and for this reason were the first to be used for high-frequency carrier systems.

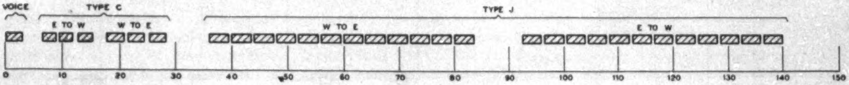

Figure 1. Frequency allocations

The first carrier systems, beginning in 1918, added three or four channels to the existing voice circuit on a pair. To keep pace with this development, improvements in transposition systems were devised so that many such carrier systems might be operated on the same pole line. Such carrier systems, typified by the three-channel type *C*[1,2] system, have seen continuous growth in use in the long-distance plant. Now a 12-channel system, the type *J*, is being made available to add to the type *C* system, thus giving 16 telephone circuits on an open-wire pair in addition to the two telegraph circuits. Since there are already about 60,000 miles of pole line equipped with type *C* systems, the new type *J* system was developed to go in the frequency range above the type *C* system rather than to supersede it with more channels (figure 1).

The new system has been designed to meet high standards of transmission and reliability for distances up to several thousand miles. The frequency band transmitted by the individual derived circuits is exceptionally wide, from about 100 to 3,600 cycles for a single system and has been previously discussed[3] in relation to the channel spacing in this and other new broad-band developments.

An important feature of the work on the type *J* system has naturally been that of making the line circuits suitable for carrying the higher frequencies. The tendency of circuits to crosstalk into one another increases rapidly with frequency. Advances in transposition design and structural improvements have now made it possible to extend the frequency range from 30,000 cycles to 140,000 cycles, which is about the upper frequency of the type *J* system. The problem of incidental cables in open-wire lines has also been serious, since the losses increase with frequency, and what is usually more important, there may be substantial reflection effects at junctions of the open-wire line and cable. These are serious, not only from the standpoint of the transmission loss which they entail, but from their effect on crosstalk. The increase in attenuation at the higher frequencies has also brought other problems into the picture. For example, repeaters are needed at more frequent intervals than with the lower-frequency systems. Attenuation variation with frequency due to weather changes is greater than at the lower frequencies.

Figure 2 shows schematically the complete type *J* system, with its different major circuit elements, resulting at the terminals in the division of the single line circuit effectively into 16 talking circuits. In no recent development is the function of the wave filter in providing essential units in a frequency dividing plan more forcefully illustrated than in the application of this new system, in combination with the type *C* and other facilities which exist. There are about 60 different designs of filters and networks in the terminals and repeaters. Their functions are varied, as, for example, separating the individual channel bands, separating the opposite directional groups of channels, separating the type *J* frequency range as shown in figure 1 from the type *C* and other ranges, separating the different carrier frequencies of a carrier supply system in which the carriers are all derived from a common 4,000-cycle source, etc.

The new system, as in the case of the type *C*, uses single-sideband transmission with carrier elimination. Copper-oxide units are employed as translator elements of various kinds—modulators, demodulators, and harmonic producers. Methods of mounting the equipment, and methods and apparatus for testing follow lines already worked out for the type *K* cable carrier system, which was described a year ago in two AIEE papers.[4,5]

Channel Terminals

A terminal of the type *J* system changes 12 independent voice channels into a compact block of 12 carrier channels

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Paper number 39-53, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 28, 1938.

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1. For all numbered references, see list at end of paper.

properly allocated in frequency for transmission over the open-wire line. Inversely, such a block received from the open-wire line is separated and transformed into 12 independent voice channels. The first step in transmitting the 12 voice channels is to modulate them on 12 carrier frequencies 4 kilocycles apart from 64 to 108 kilocycles and to select the lower sidebands by means of quartz-crystal channel band filters. The last step in the conversion from a received 12-channel block to the 12 independent voice channels consists in the division of the block by 12 quartz-crystal channel filters and the demodulation of these messages to produce voice frequency transmissions. These two frequency changes and separations are performed by the same equipment that is used in the type *K* cable carrier system terminals.

Figure 3 shows the circuit of a modulator and a demodulator for the opposite directions of a single conversation with indicated connections for the 11 others which make up this fundamental 12-channel block. The modulator consists of a bridge assembly of copper-oxide varistors and is supplied with about 0.5 milliwatt of carrier power from the carrier supply system which is described later. Of the two resulting sidebands, the lower is selected by the crystal band filter following the modulator. The line sides of 12 modulator band filters are joined in parallel and a compensating network is connected to preserve the band characteristics of the upper and lower channels.

On the receiving side, after separation by one of the 12 parallel filters, the side band is applied to a demodulator supplied with the proper carrier frequency to restore the voice-frequency message. Because of the low level at which demodulation takes place, the demodulator is followed by a single-stage amplifier to produce the level desired in the voice-frequency circuit. The gain of this amplifier is adjustable over a moderate range.

The combination of a single modulator and a single demodulator and associated equipment shown in figure 3 is called a "modem" and two of these are mounted on a single equipment panel. Nine of these panels, sufficient for $1^1/_2$ type *J* systems, or 18 conversations, mount in a single relay rack bay of standard height.

Carrier Supply

The carrier frequencies 64–108 kilocycles are all derived as harmonics of a 4-kilocycle frequency produced by a tuning-fork-controlled oscillator. This frequency is applied to an easily saturated coil to produce a sharply peaked wave which is rich in odd harmonics. Even harmonics of four kilocycles are obtained by rectification in a copper-oxide unit of part of the odd harmonic output. Odd and even harmonics appear in separate circuits from which each frequency desired is separated by a quartz-crystal filter. Frequencies as high as the 121st harmonic, that is, 484 kilocycles, are obtained in this way from the carrier supply system. Because of the importance of the carrier supply two sources are provided, with automatic equipment to transfer rapidly from the regular to the emergency source.

Group Modulation

As shown in figure 1, the type *J* system uses a band of 36 to 84 kilocycles for the west to east direction of transmission and 92 to 140 kilocycles for the east to west direction. The output of the fundamen-

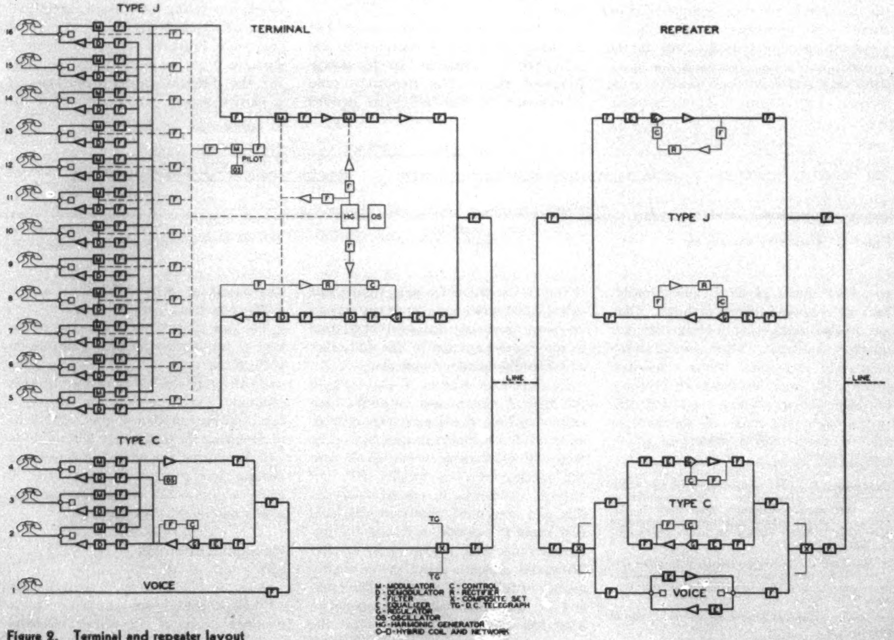


Figure 2. Terminal and repeater layout

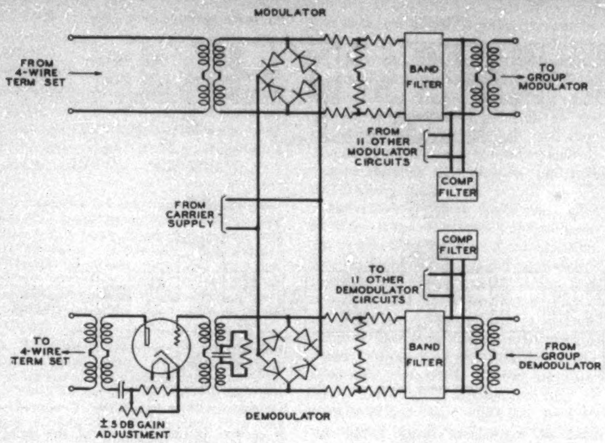

MODULATOR
FROM 4-WIRE TERM SET
BAND FILTER
TO GROUP MODULATOR
FROM 11 OTHER MODULATOR CIRCUITS
COMP FILTER
FROM CARRIER SUPPLY
COMP FILTER
TO 11 OTHER DEMODULATOR CIRCUITS
TO 4-WIRE TERM SET
BAND FILTER
FROM GROUP DEMODULATOR
± 5 DB GAIN ADJUSTMENT
DEMODULATOR

Figure 3. Channel modulator and demodulator

tal 12-channel unit consists of 12 lower sidebands from carriers of 64–108 kilocycles. This must, therefore, be translated to the two type *J* directional groups for line transmission. Since the frequencies in the fundamental unit overlap those in both directions of line transmission, this transfer must be made in two steps. Figure 4 shows these frequency translations. The first group modulation is the same for both directions of transmission. By modulating the fundamental unit with a carrier of 340 kilocycles there is obtained a block of lower sidebands extending from 400 to 448 kilocycles. A second modulation with a 484-kilocycle carrier then gives, for transmission from west to east, a 12-channel block of upper sidebands extending from 36 to 84 kilocycles. For the east to west transmission the second modulation uses a 308-kilocycle carrier, producing a 12-channel block of lower sidebands between 92 and 140 kilocycles.

Frequencies as high as 308, 340, and 484 kilocycles are chosen for group modulation in order that undesired products shall be well separated from desired products to permit their elimination by simple filter structures.

The same group modulation processes that have been described above for adapting the 12-channel group for line transmission are used in the opposite sequence for receiving the block from the line and preparing it for separation by the channel band filters of the receiving terminal; thus, for instance, at an east terminal the block of upper sidebands, extending from 36 to 84 kilocycles as received from the line, is first modulated with 484 kilocycles producing lower sidebands between 400 and 448 kilocycles. These are next modulated with 340 kilocycles which produces a block of 12 lower sidebands extending from 60 to 108 kilocycles, which is the group that the fundamental 12-channel terminal unit is designed to handle.

Figure 5 shows the essential features of the group modulating and group demodulating circuits. As in the type *K* system, group modulation is performed at a very low level of the message material and with a high level, about 25 milliwatts, of the group carrier supply, in order to minimize interchannel crosstalk. The group modulators are of the doubly balanced bridge type which aids in suppressing some of the unwanted modulation products. Following the first group modulator and also following the first group demodulator are coil and capacitor type 400–448 kilocycle band filters which reject the unwanted products and pass the band of frequencies containing the 12 channels. Between this filter and the second group modulator on the transmitting side of the terminal, an intermediate amplifier is used in order to keep the level of the group transmission above danger of noise. Following the second group modulator and also following the second group demodulator are low-pass filters which cut off frequencies above about 160 kilocycles, to suppress unwanted modulation products. From the output of the receiving low-pass filter the 12-channel group, 60–108 kilocycles, passes through a two-stage "auxiliary" amplifier to bring it to the desired level.

The carrier frequencies for group modulation and for group demodulation are derived from the same four-kilocycle tuning-fork-controlled oscillator that supplies carriers for the 12-channel unit. From the circuit in which appear the odd harmonics of 4 kilocycles, the 77th, 85th, and 121st harmonics, that is, 308, 340, and 484 kilocycles, are selected by carrier

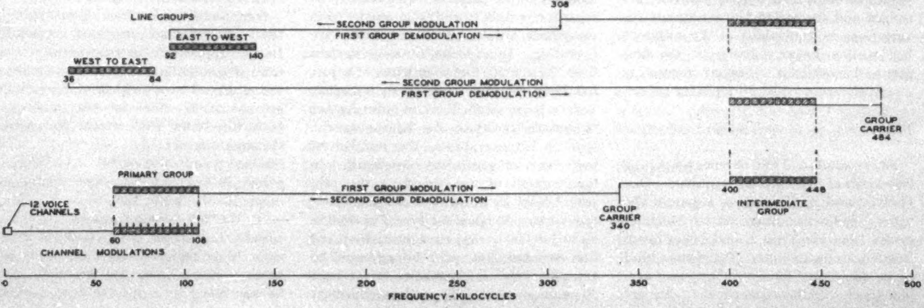

GROUP CARRIER 308
LINE GROUPS
← SECOND GROUP MODULATION
FIRST GROUP DEMODULATION →
EAST TO WEST
92
140
WEST TO EAST
36
84
← SECOND GROUP MODULATION
FIRST GROUP DEMODULATION →
GROUP CARRIER 484
PRIMARY GROUP
FIRST GROUP MODULATION →
← SECOND GROUP DEMODULATION
12 VOICE CHANNELS
GROUP CARRIER 340
400
448
INTERMEDIATE GROUP
60
108
CHANNEL MODULATIONS
0
50
100
150
200
250
300
350
400
450
500
FREQUENCY - KILOCYCLES

Figure 4. Frequency translations

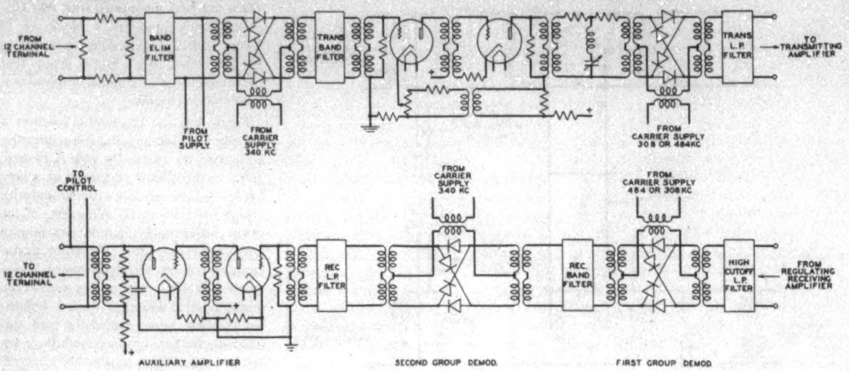


Figure 5. Group modulator and demodulator

supply filters and separately amplified by two-stage amplifiers to produce the powers required for group modulation. Outputs from these amplifiers are fed to individual-frequency busses capable of supplying the group modulators and demodulators for ten systems. An emergency carrier supply for these frequencies is also provided with arrangements for switching rapidly from the regular to the emergency circuits.

Terminal Amplifiers

As indicated on figure 5, the transmitted 12-channel group, now transferred to the proper frequency range for line transmission, goes from the low-pass filter at the output of the second group modulator to a transmitting terminal amplifier which is similar in most essentials to the amplifiers of the line repeaters. The 12-channel group arriving from the line passes through a regulating amplifier arranged and controlled to compensate for variations in equivalent of the adjacent line section before passing to the first group demodulator. Similar regulating amplifiers are used at all repeater points.

Filters

At terminals and also at repeater points, two kinds of filter sets are required. One kind is used in the line to separate the type *J* frequency range 36 to 140 kilocycles from the type *C* and other lower frequencies on the line. The second kind is the directional filters of the type *J* system itself. These separate a 12-channel band of frequencies lying below 84 kilocycles used for west to east transmission from the 12-channel group lying above 92 kilocycles which is transmitted from east to west. These directional filter sets are carefully designed to equalize any nonuniformity of loss in both the directional and the line filters. As this equalization involves a considerable loss over a large part of the filter band it is provided entirely in the receiving directional filters where the transmission is at a low level and the loss can readily be made up by amplification. In this way nearly the full energy output of the transmitting or repeater amplifier is available for line transmission.

Line Crosstalk Problems

As noted previously, type *J* systems will, in general, be applied on pairs on which type *C* systems are already operating. Such pairs have already been arranged to transmit frequencies up to 30,000 cycles, and transposed in such a manner as to perform satisfactorily as regards crosstalk to and from nearby pairs on which similar carrier systems are operating. In addition, on most modern lines the spacing between wires of a pair has been reduced from 12 to 8 inches; and, on many of the lines, in order further to reduce crosstalk by increasing the spacing between pairs, the number of pairs on a crossarm has been limited to four instead of five, omitting the pole pair. Now, by applying a new transposition system designed for type *J* operation up to 140,000 cycles, an eight-inch-spaced four-crossarm line may be arranged to transmit type *J* frequencies on at least 10 pairs out of 16. Type *C* systems may, of course, be used on all of the pairs. Finally by using the most advanced transposition design methods, and increasing the crossarm spacing, in addition to the features noted above, a new line may be constructed to permit the operation of 16 channels on all pairs.

To make the pairs of wires good for type *J* systems, more than a fourfold increase in frequency range, was difficult. The natural tendency of the circuits to crosstalk is increased even more than the frequency ratio, so that in addition to applying a new transposition design it is necessary that the transposition poles be more accurately located, and that the sags of the two wires of each pair be kept more nearly alike. On lines which already have eight-inch-spaced wires, no major structural changes are necessary. However, on lines which have only 12-inch-spaced wires and where it is desired to make available a number of pairs for type *J* transmission, structural changes, such as respacing the wires of the pairs concerned to 6 inches, are necessary in order to reduce the coupling.

One factor of extreme importance is that of reflected near-end crosstalk. In the application of transposition systems it is usually not possible to reduce the near-end crosstalk to a magnitude approximating the far-end crosstalk. It is the latter with which the carrier systems are chiefly concerned, since similar types of systems on different pairs all transmit the same frequency range in the same direction. If, however, the lines concerned do not have smooth impedance characteristics, that is, a high degree of freedom from reflection effects, near-end crosstalk may be converted by reflection into far-end crosstalk of sufficient magnitude to be controlling over the true far-end crosstalk.

This means that lines to be used for several type *J* systems must be made unusually smooth electrically—impedance variations kept within a few per cent. The achievement of such smoothness consists chiefly in:

1. Reducing the electromagnetic and electrostatic couplings to other pairs so that there are no large energy interactions, with corresponding impedance irregularities. Generally speaking, when the pairs concerned have been transposed for reduced far-end crosstalk up to the maximum frequency transmitted, this condition is also satisfied.
2. Minimizing the effect of intermediate and terminal cables. This latter problem has caused considerable concern and is responsible for the development of several new techniques in the design and treatment of such cables, where they appear in a long line otherwise consisting chiefly of open wire.

Cable Treatment

As a means of overcoming the reflection and attenuation effects of short pieces of terminal or intermediate cable, loading naturally suggests itself, as applied in type *C* systems, where the cable pairs involved are commonly equipped with carrier loading coils, spaced at about 700-foot intervals. This compares with the 3,000-foot or 6,000-foot spacings which are standard for voice-frequency loading. However, loading pairs in existing cables satisfactorily up to 140,000 cycles would mean coils at approximately 200-foot intervals. Because of physical limitations, existing manhole spacings, etc., this is highly impractical. A reasonable solution has, however, been found in the creation of a new form of low-capacitance high-frequency cable—a disk-insulated unit which has constructional features in common with the coaxial cables and a capacitance of only 0.025 microfarad per mile as compared with about 0.062 microfarad for conventional cable pairs. This permits more practical loading-coil spacings. These disk-insulated units are made up as spiral-fours, that is, two pairs (0.051 inch diameter wire) which form the diagonals of a square. When these cables are loaded with small coils at intervals of approximately 600 feet, they present impedance characteristics substantially equivalent to that of an open-wire pair over the desired frequency range. Accordingly, they form a desirable, although somewhat expensive, solution of the problem of intermediate or entrance cables. As shown in figure 6, the spiral-four units are bound together in complements of seven or less under a lead cable sheath similar to standard toll cables. It should be noted that the low-capacity disk-insulated loaded cables not only provide a satisfactory solution of the impedance matching problem, but they also give a cable circuit of low attenuation—approximately 1.2 decibels per mile at 140 kilocycles.

Nevertheless, where spare pairs exist in cables, it has often been found economical to use them for type *J* transmission. It is possible to use them only nonloaded, in which case the attenuation is very high—four to six decibels per mile, depending on the gauge, at 140 kilocycles, and impedance matching transformers are, of course, required at the junction of the open-wire and cable. There are cases where this higher attenuation may be permitted and these pairs are used by separating the type *J* range from the lower frequency range, which is transmitted through pairs equipped with the older type *C* carrier loading. The separation is accomplished by filters which are usually housed in small filter huts at the junction of the open-wire line and cable.

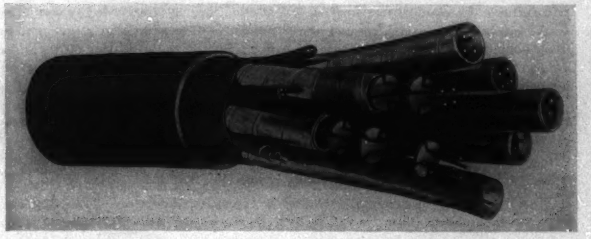


Figure 6. Disk-insulated cable

Sheath diameter 2.3 inches

In other cases it has been found economical to use the frequency-separation method with filters and to install new nonloaded cables of lower attenuation to lead in the type *J* frequency band alone. Paper-insulated ten-gauge pairs or the disk-insulated spiral-four cable of the type described above may be used for this purpose. In either case transformers are used to match the cable impedance to that of the open-wire line over the type *J* frequency range.

The reflection requirements are so severe and the effects of even short lengths of cable at the high frequencies so serious, that even short lead-in cables, where the open-wire line actually extends to the repeater or terminal building—cables which are only 100 or 200 feet long—must receive special treatment. This has also been accomplished by the use of the disk-insulated spiral-four cables, loaded.

Interaction Crosstalk

Because of the higher attenuation there will be many repeater points on a long line at which the type *J* system will be amplified but at which the other systems and wires on the line will pass through the station without amplification. In this case, even though the type *J* pairs are properly transposed to keep down crosstalk between themselves, there still remains the crosstalk between them and the other pairs on the line, not only pair-to-pair crosstalk but crosstalk from the type *J* pair to various circuit paths consisting of irregular wire combinations.

Two difficulties arise in this case: The first is that the crosstalk from the output of one *J* system into an irregular path may be retransferred into the input of a repeater on another type *J* system. The second is that the crosstalk from the irregular path may be returned to the input of the same repeater and either influence the over-all transmission characteristic or, if sufficiently severe, actually cause the repeater to sing. This general situation has made it necessary to introduce in the circuits at such points "crosstalk suppression" filters in the non-*J* pairs and longitudinal choke coils in all pairs.

Staggering

In addition to the various steps which are taken in order to reduce crosstalk by improving the line conditions, the type *J* system may include a feature which has been used in the type *C* system—the staggering of the channel bands used on

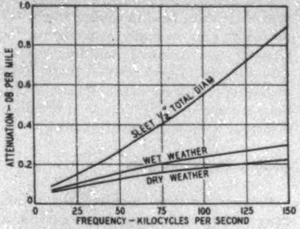


Figure 7. Attenuation-frequency characteristics of open-wire lines

neighboring pairs. The advantage of staggering results from the facts that (*a*) the sensitivity of the ear and the power of the voice vary over the audible range, (*b*) the efficiencies of transmitter and receiver also tend to vary over the frequency range, (*c*) part of a channel band may lie opposite "dead" frequency range on an adjacent pair, and (*d*) by controlling the arrangement of the sidebands the crosstalk may be made unintelligible even if not inaudible. The staggering feature is readily provided in the type *J* system by a suitable choice of carrier frequency for the second group modulator and first group demodulator. With the staggered systems the highest frequency used would be about 143 kilocycles.

Attenuation Problem

In what has preceded in the discussion of line problems, the emphasis has been confined chiefly to the question of the smoothness of a line from an impedance standpoint in order to keep down reflection effects and, correspondingly, to improve the operation from a system-to-system crosstalk standpoint. There is also the problem of the higher attenuation incident to the use of higher frequencies. Between 30,000 cycles and 140,000 cycles, the normal wet-weather attenuation for a 165-mil open-wire pair, for example, rises from about 0.13 to 0.28 decibel per mile—an increase of approximately 2:1. Repeaters on the type *J* system, if applied on the basis of approximately the same output level and minimum level requirements, must be spaced at about one-half the interval of the type *C* systems. Normal spacings for type *J* systems would therefore be expected to range from 75 to perhaps 100 miles where no large amount of intermediate cable existed.

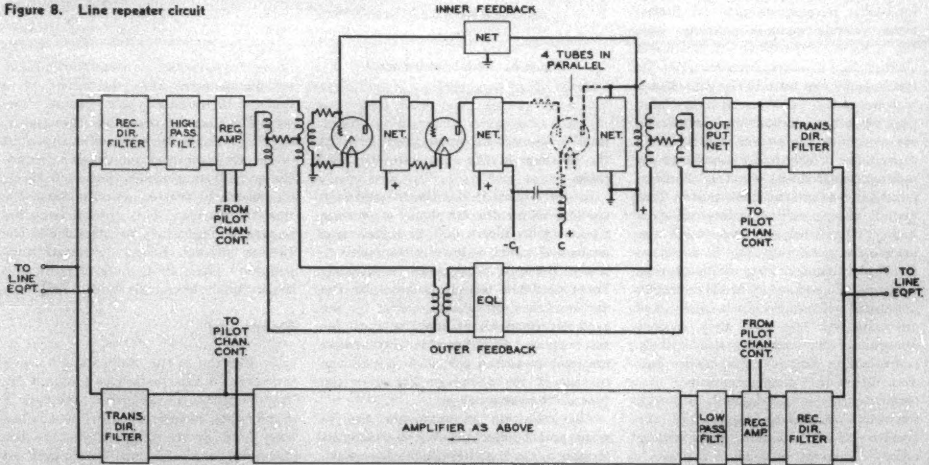
However, another problem, not present to a similar degree at the lower frequencies, tends in many cases to have a controlling effect on this spacing, that is, sleet or ice on the wires. With ice, frost, or snow on the wires, the wet-weather attenuation may be exceeded by very large amounts. The additional attenuation is due primarily to the coating on the wires themselves rather than the coating on the insulators. It arises from the potential gradient through the ice deposit in combination with the high dielectric loss characteristic of the ice or snow coating. Figure 7 gives examples of the attenuation frequency characteristics of open-wire lines, including certain measurements with ice coating. The exact increase in attenuation due to snow and ice naturally depends on the thickness and other characteristics of the coating. Even very thin coatings of ice on the wires tend to raise the attenuation at 140 kilocycles from the normal wet-weather figure of about 0.28 decibel to about 1 decibel a mile, that is, an increase of three or four to one Extremes up to five decibels per mile have been measured for short lengths of line with ice nearly two inches in diameter. Such heavy ice obviously approaches the mechanical breakdown conditions for the line.

Where ice and sleet occur the repeater spacings may be reduced to about 50 miles or less. The repeaters now being provided for the type *J* systems have gains of approximately 45 decibels. Repeaters are under development which are expected to raise the maximum available gain to something like 75 decibels. The normal dry or wet weather operation of such repeaters would be limited to gains of perhaps 10 to 25 decibels depending upon the amounts of cable included. The problem of obtaining automatic gain control over the extra wide range required by the high sleet attenuations is a difficult one.

Repeaters

At each repeater point line filters and directional filters are required on both sides of the amplifying equipment to separate type *J* currents from those of

Figure 8. Line repeater circuit



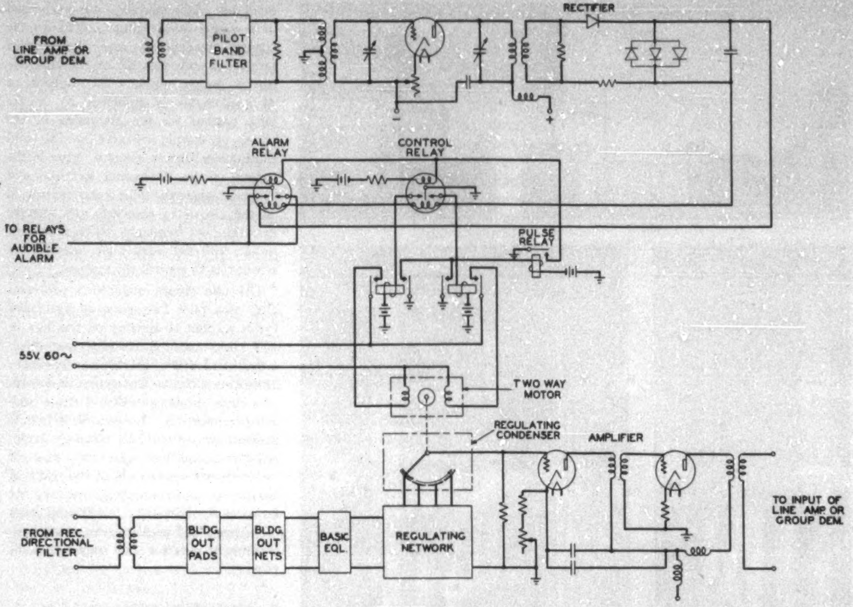


Figure 9. Regulating amplifier and pilot control

lower frequency services on the line and to separate oppositely directed groups for separate amplification in one-way line amplifiers. These filters have been described in connection with the terminals where they perform similar functions. Two regulating amplifiers, one for each direction of transmission, properly controlled to compensate for variations in the attenuation of the preceding line section, are also needed at each repeater point. These are described later under "Regulation."

Figure 8 shows the circuit of one of the line repeaters and indicates the location of the directional filters, and certain supplementary filters for suppressing frequencies outside the transmitted range; also the regulating amplifier circuit, and the pick-off of the pilot channel which controls the gain.

The line amplifier has three stages of pentodes. The first two stages use single tubes of high voltage amplification and low power capacity while the third stage has four power pentodes in parallel to increase the output capacity. Because of considerable heat developed by these power tubes, special precautions are necessary to dissipate the heat and to protect capacitors and other elements mounted near them.

Negative feedback to improve the operation of the amplifier is supplied over two paths. The inner feedback, from the plates of the output tubes over a properly designed network to the grid of the input tube, reduces the gain at frequencies outside the transmitted band and so prevents singing at those frequencies. It has little effect at frequencies within the type *J* range. The outer feedback path includes the input and output transformers, which are made as hybrid coils. In each of these one pair of the conjugate windings is connected to the incoming or outgoing circuit of the amplifier while the other pair is used for the feedback connection. By feeding back through the transformers in this way, they benefit by feedback in much the same way as the tubes, and the over-all characteristic of the amplifier is practically independent of the transformer characteristics. This feedback reduces the amplifier gain by over 40 decibels and correspondingly reduces modulation effects within the amplifier, and gives exceptionally stable transmission with respect to tube and voltage changes. It is also designed to improve and stabilize the input and output impedances.

Equalization

Equalization is necessary in each direction of transmission at a repeater point and in the receiving direction at a terminal, to compensate for frequency distortion produced by the preceding section of line. Fortunately, the attenuation-frequency curves for the usual open-wire circuits, that is, 104-, 128-, and 165-mil wire, have nearly the same shapes for section lengths giving the same attenuation at the maximum frequencies for the two directions of transmission, so that these various circuits can be equalized alike.

As is well known, the transmission frequency characteristic of an amplifier with large feedback is almost the inverse of that of the feedback circuit itself, so that the insertion in the feedback circuit of a network having the same characteristics as a line section will provide equalized transmission over the amplifier and section combined. In the outer feedback circuit of the line repeater is included an equalizer which has a characteristic sloping with respect to frequency in the same way as the variation

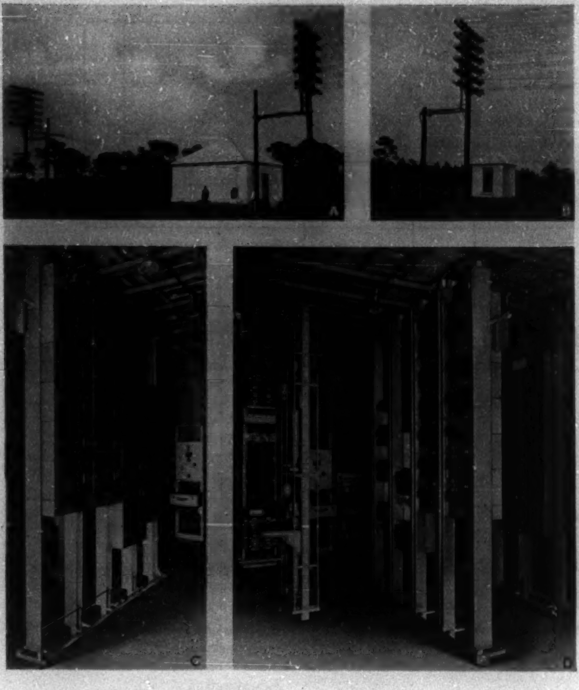


Figure 10. Typical installations

A—Auxiliary repeater station

B—Cable hut

C and *D*—Terminal installations

in loss under wet weather conditions of the longest open-wire section likely to be used. Thus, there is provided in the repeater a basic equalization for this longest wet-weather line. At a receiving terminal a basic equalizer is provided which performs this same compensation, but in this case the slope of the curve must necessarily be opposite to that of the line attenuation and of the equalizer in the feedback path of the line repeater.

Line sections, however, vary in length and in the amount of entrance cable included. In order that they may be properly corrected by this basic equalization, they must be built out to equal this longest wet-weather section. For this purpose there are provided flat loss pads and building-out networks whose losses have the same frequency shapes as the losses of short lengths of open-wire circuit. These pads and networks can be inserted or omitted by simple changes in strapping. They suffice to build out the shortest section which is expected to be used.

Pilot Currents

For a satisfactory system, arrangements must be provided to correct automatically for the effects on line attenuation due to changes in weather, by adjusting the amplification at each repeater point and in the receiving terminal circuit. To permit measuring these effects a pilot current of fixed frequency, near the middle of the transmitted band, and of constant amplitude, is supplied from each terminal. This is applied to the transmitting side of the terminal circuit between the 12-channel terminal and the first group modulator, where the message band lies between 60 and 108 kilocycles. The pilot frequency is 84.1 kilocycles which is obtained by modulation of 88 kilocycles, from one of the output taps of the channel supply of that frequency, with 3.9 kilocycles derived from a tuning fork oscillator. This modulation is performed in a copper-oxide bridge similar to the channel modulators and the desired product is selected by an 84-kilocycle carrier supply filter. The output of 84.1 kilocycles is sufficient to supply pilot current for ten terminals in the office. A sharply selective crystal band elimination filter is inserted between the output of the 12-channel terminal and the point where the pilot source is bridged on the circuit to eliminate any current near the pilot frequency which would interfere with the small pilot current that is sent out to control the system.

The two group modulation processes alter this pilot frequency of 84.1 kilocycles so that it appears on the line as 59.9 kilocycles in the west to east directional band, and as 116.1 kilocycles in the east to west band. Correction in accordance with the magnitudes of these mid-group currents in the two directions is satisfactory over all 12 channels under ordinary conditions. In the case of ice or snow the channels at the edges of the directional frequency groups may not be properly adjusted. Additional pilot frequencies will probably be needed ultimately to care for such unusual conditions.

Regulating Amplifier

Figure 9 shows the circuit of the regulating amplifier and above this, the circuit of the pilot-channel receiving equipment which controls it. Current enters the regulating amplifier circuit from the left, coming from the receiving directional filter through a shielded transformer and the pads and building-out networks used for equalization. At the terminals the circuit includes also the basic equalizer. Last in the circuit leading from the line to the regulating amplifier is the regulating network which consists of a series of three equal networks having a total loss of 20 decibels at 140 kilocycles in the east to west direction and 15 decibels at 84 kilocycles in the west to east direction. The network loss increases with frequency in the same way as the difference between dry and wet weather attenuation of the line. The two terminals of the regulating network and the two junction points between the three networks are brought to four sets of stator plates on an adjustable capacitor. The rotor of this capacitor, which has about the same area as one set of stator plates, is connected to the grid in the first stage of the regulating amplifier. Rotation of the capacitor therefore applies, to the grid of the first tube, a voltage which decreases continuously as the capacitor rotates from left to right.

The regulating amplifier has two stages of pentode tubes, a high input impedance necessary for the proper operation of the capacitor potentiometer, and feedback to stabilize the gain and to prevent intermodulation of the channels. Its output goes to the line amplifier at repeater stations, and to the first group demodulator at the terminals. At a west terminal there is interposed a high cut-off filter to eliminate frequencies above the upper band which may have been picked up on the open-wire line.

Pilot Control

The setting of the capacitor which controls the regulating network is determined in accordance with the amount of pilot current flowing in the circuit in the direction concerned. At repeater stations the pilot current is picked off at the output of the line amplifier, being separated from the message transmissions by a quartz filter which has about a 30-cycle pass band. For control of transmission from west to east at the repeater stations, this filter selects 59.9 kilocycles and for control of the oppositely directed transmission, 116.1 kilocycles. At the terminals the pilot-channel selecting filter is connected across the output of the auxiliary amplifier following the second group demodulator where the pilot frequency is 84.1 kilocycles. The pilot current from the pick-off filter is amplified in a single-stage amplifier which has feedback for constancy of operation and input and output circuits tuned to the pilot frequency. After amplification the pilot current is rectified by a temperature-compensated copper-oxide rectifier.

The resulting direct current passes through the operating windings of the control and alarm relays. These Weston Sensitrol relays are, in fact, microammeters with high and low contacts made by the pointers. The mechanical bias of the moving system is adjusted so that with the normal pilot current the pointer will remain free in the middle between the two contacts. A change of about 0.5 decibel in this current will cause the pointer of the control relay to make contact with the terminal at the corresponding end of its swing. As the limiting contacts are magnetized and the pointer is of magnetic material, good contact is insured. When contact is made on one side a 60-cycle circuit is closed through the motor which controls the regulating capacitor in such a direction as to cause the loss in the regulating network to be increased. Closure of the other contact similarly causes the loss in the regulating network to be decreased. Closure of either contact also closes a circuit containing a slow operate "pulse" relay to release the Sensitrol relays after an interval of about four seconds During this time the gain of the regulating amplifier will have been changed about 0.1 decibel. If now the pilot current level is within 0.5 decibel of normal the operation is complete. If not, it is repeated and the device keeps periodically testing the circuit so long as it is away from satisfactory compensation. There are also alarm circuits for attracting attention in cases of wide variations in equivalent. In severe ice conditions where a single regulating repeater has not sufficient gain to make up for the great loss in the line, the next succeeding repeater will do its utmost to make up the deficiency.

Conclusion

In what has preceded, developments have been described which are making it possible to provide a very substantial increase in circuits on open-wire pole lines without additional wire stringing. Illustrations of typical office installations of type *J* carrier equipment, unattended repeater stations, and filter huts are shown in figure 10.

Three stages in the development of the open-wire line over the past 20 years, giving successive increases in circuit capacity, are shown in figure 11. Prior to the application of carrier systems, a four-crossarm pole line would yield 30 voice circuits. Now, on a new line 256 circuits are potentially obtainable. Thus it is probable that the open-wire line will continue as an important factor in furnishing facilities in moderate numbers, particularly in the less densely populated sections of the country and where climatic conditions are not unfavorable. Installations of type *J* systems have already been made in various parts of the United States.

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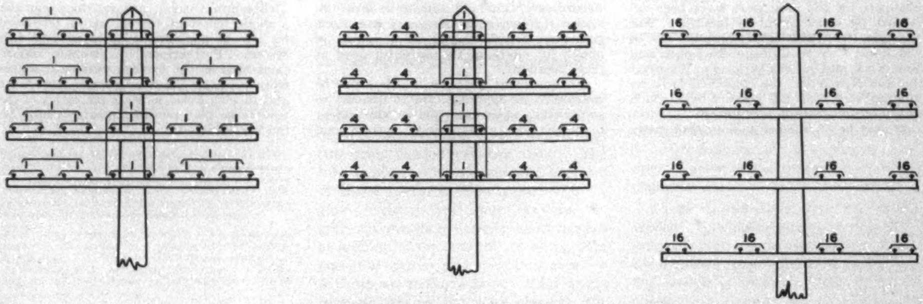

Facilities—30 voice circuits

Line construction with eight-inch-spaced nonphantomed nonpole pairs. Type C systems on all eight-inch-spaced pairs

Facilities—22 voice circuits, 48 carrier circuits, total 70

New line construction; all eight-inch-spaced pairs; no pole pairs; crossarms spaced 36 inches instead of 24 inches; no phantoms

Facilities—16 voice circuits, 240 carrier circuits, total 256

Figure 11. Growth in line carrying capacity

Beyer. *Bell System Technical Journal*, January 1939.

3. TRANSMITTED FREQUENCY RANGE FOR CIRCUITS IN BROAD BAND SYSTEMS, **H. A. Affel.** *Bell System Technical Journal*, October 1937.

4. A CARRIER TELEPHONE SYSTEM FOR TOLL CABLES, **C. W. Green** and **E. I. Green.** *Bell System Technical Journal*, January 1938, and ELECTRICAL ENGINEERING, May 1938.

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Discussion

James J. Pilliod (American Telephone and Telegraph Company, New York, N. Y.): The 12-channel open-wire carrier telephone systems came into commercial use in the plant of the long lines department of the American Telephone and Telegraph Company during the latter part of 1938 and in January of this year. Six such systems have been placed in operation, one in Texas between Dallas and Houston, two on the recently completed transcontinental route between Oklahoma City and Whitewater, one between Oklahoma City and Albuquerque, and two between Charlotte and West Palm Beach. These systems provide 72 channels, making a total of nearly 55,000 miles of telephone circuit.

The longest of these systems is the Oklahoma City–Whitewater (Los Angeles) whose route totals approximately 1,180 miles and in which there are 16 intermediate repeater points. The spacings of these repeaters were adjusted in accordance with knowledge of abnormal weather conditions. On the eastern end, between Oklahoma City and the Texas–New Mexico state line, in which sleet is to be expected, the average spacing is in the order of 50 miles, while west of this point where less severe conditions, from a transmission standpoint, are encountered, the spacing averages more in the order of 75 miles. On the southern systems spacings ranging from 75 to nearly 100 miles obtain.

Experience with these systems thus far has been very satisfactory. Excellent telephone circuits have been obtained and the channels, for the most part, have been extended by other types of facilities. For example, four-wire cable circuits out of Oklahoma City to Chicago, St. Louis, and New York, and out of Charlotte to Washington and New York, the combined facilities forming very long toll circuits over which excellent transmission is obtained.

It may be of interest to note that many of the channels between Charlotte and West Palm Beach are extended north to New York by means of the cable carrier systems between New York and Charlotte of the kind described in a paper by C. W. Green and E. I. Green, presented at the winter convention in January 1938, to form New York–West Palm Beach and New York–Miami circuits.

The use of this type of facility will doubtless be an increasing factor in our plans for circuit growth.

R. M. Goetchius (nonmember; American Telephone and Telegraph Company, New York, N. Y.): In addition to the systems mentioned by Mr. Pilliod, two additional type *J* systems were placed in service in 1938, one between Dallas and San Antonio, a distance of 280 miles, and one between Dallas and Longview, a distance of 130 miles. These two systems provided 5,000 miles of additional telephone circuit. In the application to existing telephone plant of all these type *J* carrier telephone systems, there are many engineering problems. For example, in laying out the intermediate repeater stations, consideration must be given to the probability of ice formations, static noise, which is a maximum in the summer, and possible interference from near-by radio stations. In many cases portable testing apparatus is used to make field measurements of the order of magnitude of these effects in the particular location.

After the number of auxiliary repeater stations between existing offices has been determined on these general considerations, it is necessary to make a detailed survey to determine suitable sites for the repeater office. Important elements in these considerations are the availability of commercial power and the accessibility to main roads. In most parts of the country commercial power is readily available within a few miles of the theoretically best location for a repeater station. However, in the case of several offices on the route between Oklahoma City and Whitewater, Calif., no source of power was available at the repeater site or even within a reasonable distance. Accordingly, windmill generators were installed together with emergency gas-engine generators which served as a source of power for charging the batteries used at these locations.

An important problem which must be considered in applying type *J* systems to an existing open-wire line is the extent to which the transposition system and configuration of the line needs to be modified to make it satisfactory for type *J* use. The solution in each case depends to a large extent on how well the line is transposed and on the number of type *J* systems for which it appears reasonable to arrange the line. On the present-day eight-inch-spaced well transposed lines, it is possible to apply a number of type *J* systems with relatively small changes in the present arrangements. On the older lines employing 12-inch spacing where only a limited number of crossarms are now available for type *C* carrier, the application of type *J* systems may require extensive retransposing and even respacing to six inches of the pairs which are used for type *J* operation. The solution for this problem which was worked out in Texas for the Dallas–Houston and Dallas–San Antonio systems was quite different from either of the general treatments mentioned previously. On both of these routes, which consisted of five full crossarms of wire, it was decided to provide additional facilities during 1937 before the type *J* system was available. In order to prepare the additional wire for future type *J* application and to eliminate the possible hazard due to wires from the upper crossarm falling on the type *J* wire, an additional crossarm was placed on both these lines 24 inches above the present top crossarm. This was accomplished by adding a simple extension fixture consisting of a four-inch steel H-beam arranged to be fastened to the pole by the through bolts supporting the two upper crossarms. On this additional crossarm there were placed four six-inch-spaced 128-mil conductors suitably transposed for type *J* operation.

While experience with commercial operation of the type *J* systems has been quite limited, the service results to date have been very good. The circuits themselves are very quiet and transmit a wider band of frequencies than previous types of open-wire carrier systems. The regulation features of the system automatically compensate for all normal weather changes and therefore minimize the amount of periodic maintenance work required to maintain satisfactory operation. As an example of this, during the first three months of operation of the Dallas–San Antonio system, there was not a single report of trouble to the testboard on any of the circuits operated over this system. Furthermore, if trouble conditions arise in the system, automatic alarm features notify the maintenance attendants and, in some cases, indicate the nature of the trouble so that remedial measures may be quickly initiated.

An Electronic Control Circuit for Resistance Welders

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FLEXIBLE electronic control circuits for the precise timing of resistance welders have been utilized thus far primarily on large welders. This paper describes a new circuit using recently developed types of cold-cathode electronic tubes which makes the application of such control economically practicable to small as well as large welders. The advantages of precise timing of welds by electronic means are well recognized, particularly for exact duplication of welds and for fabrication of materials difficult to weld or subject to physical or chemical changes if overheated. Utilization of the method on small laboratory or industrial welders may render expedient processes that heretofore were considered unavailable except for large-scale production.

Elements of Operation

In the circuit shown by the heavy lines near the top of figure 1, two band-ignitor mercury-arc tubes[1] are connected in a parallel-inverse relationship in series with the welder-transformer primary winding to conduct alternate half-cycles of current. These tubes consist of a mercury-pool cathode and a metal anode in an evacuated bulb with a metal band located outside the tube near the edge of the surface of the mercury. Conduction begins in such tubes upon application of a high-voltage from a spark coil to the external starting band if the anode is more than about 50 volts positive with respect to the cathode.* The function of the control unit, which constitutes the remainder of the circuit, is to initiate conduction in the band-ignitor tubes whenever the control switch *S* is moved from point 2 to point 1. Conduction is to begin precisely at a chosen phase angle in the supply-voltage alternation during an arbitrarily predetermined number of half cycles. The band-ignitor tubes then act as electrically controlled synchronously operating switches in series with the welder. Two are necessary to conduct the current in opposite directions because of their unilateral-conduction or rectifying characteristic. The threefold requirements of the control unit are that it (1) deliver a controllable number of high-voltage pulses to the starting bands of the band-ignitor tubes, (2) synchronize these pulses with the supply-voltage alternation at a controllable phase angle, and (3) provide operation which is independent of both the time at which the control switch *S* is operated and the interval it is held closed.

Operation in Detail

The Strobotron tube T_2 is the nucleus of the control circuit. Its function is to complete a discharge circuit for capacitor C_4 through the primary winding of the spark coil TR_2 and thereby to impress a high-voltage surge upon the starting bands of the band-ignitor tubes. Since the Strobotron tube is a relatively recent development, its characteristics as utilized in this application will be summarized briefly.

The electrodes of the Strobotron tube[2] consist of a cathode composed of a caesium compound, a metal anode, and two grids located between them. The tube is filled with neon gas at a pressure of about 1.5 millimeters of mercury. A glow discharge will start between the grids if the voltage between them reaches an amount of the order of 100 volts, with one grid positive and the other negative with respect to the cathode. If the anode is made 200 to 400 volts positive, the discharge will immediately transfer to the anode and cathode, and it will change from a glow into an arc with a low voltage drop if the source of supply for the anode-cathode circuit is capable of furnishing several amperes even momentarily. Less current to the grids is required for starting if the inner grid (the one nearer the cathode) is made negative and the outer grid (the one nearer the anode) is made positive than if the grids have the reversed polarities.

It is necessary that the high-voltage pulse to the starting bands occur twice during each cycle of the supply voltage in order that both band-ignitor tubes fire and conduct the alternate half cycles. To this end, the output of a peaking[3] transformer TR_1 is rectified by the full-wave rectifier tube T_1, and the resulting unidirectional peaked voltage wave, having a frequency of 120 cycles per second, is applied to a voltage divider consisting of R_4 and R_5 in series. The major portion of the peaked voltage, about 50 volts in amplitude, is supplied to the inner grid of the Strobotron tube through resistor R_6. The polarity of the voltage is such as to make the inner grid momentarily negative when the peaks occur. Capacitors C_2 and C_3 are included to furnish a surge of grid current and make the firing of the Strobotron tube more positive.[2] In addition they serve to prevent extraneous surges from starting the tube. The primary winding of the peaking transformer TR_1 is supplied through resistor R_3 and capacitor C_1, which form a resistance-capacitance phase-shift circuit for adjustment of the time in the cycle at which the peak of voltage from the secondary winding occurs. This voltage supply to the inner grid of the Strobotron tube is not sufficient alone to cause the tube to fire, but it insures that firing will occur at a frequency of 120 cycles per second when the remainder of the control circuit makes the outer grid positive by an amount greater than about 50 volts. Thus the second of the control-unit requirements listed above is accomplished.

The transformer TR_3 supplies power to heat the cathodes of the various high-vacuum tubes, and in conjunction with the full-wave rectifier tube T_6 it furnishes a direct voltage across capacitor C_7 to operate some of the tubes and to charge capacitor C_4.

Tubes T_4 and T_5 form a "trigger-controlled" time-delay circuit to supply the outer Strobotron grid with a direct voltage for a chosen time interval after the control switch is operated. The adjustable contact on resistor R_{15} is so set that normally grid number 1 of tube T_4 is negative beyond cutoff; hence, the current through R_{11} is negligible and the voltage of point *E* is practically that of *D;* namely, about 375 volts positive.

Paper number 39-40, recommended by the AIEE committee on electric welding and subcommittee on electronics, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted September 19, 1938; made available for preprinting December 19, 1938.

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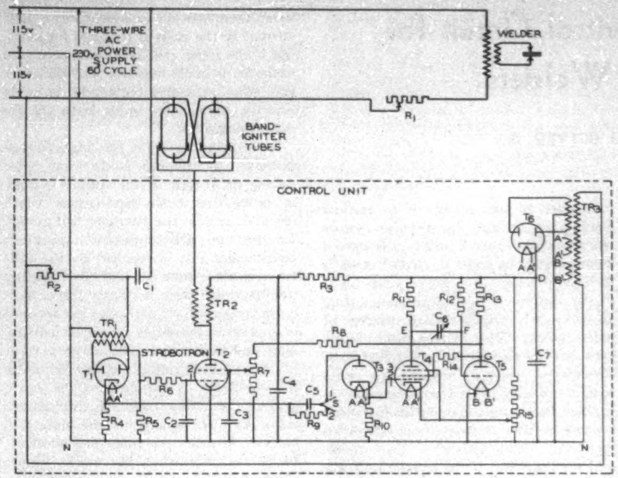
The authors are indebted to H. E. Edgerton for many suggestions in the design of the circuit.

1. For all numbered references, see list at end of paper.

* Voltage polarities of tube electrodes are taken with respect to the cathode as a reference in this article unless otherwise stated.



The grid of tube T_5 is held at a positive voltage through resistor R_{12}; therefore this tube conducts a relatively large plate current, the voltage drop in the plate resistor R_{13} is large, and point G is normally only a few volts positive. The current to the grid of the tube T_5 causes a large voltage drop in R_{12}, making the voltage of point F also only a few volts positive, with the result that capacitor C_6 is charged to about 350 volts, terminal E being positive with respect to terminal F.

When a positive voltage impulse is impressed on grid number 1 of tube T_4, in a manner to be described later, the plate current through resistor R_{11} suddenly rises, the voltage of E falls, and, because the charge on the capacitor C_6 cannot change instantaneously, the grid of tube T_5, point F, is suddenly made highly negative as is indicated in the idealized wave forms of figure 2. The plate current of tube T_5 therefore decreases suddenly. As it decreases, the voltage of point G increases, thereby increasing the screen-grid voltage of tube T_4, which increases the plate current in T_4 and accelerates the changes. The result is that grid number 2 of the Strobotron tube is suddenly made positive, and it remains fixed at this positive voltage until the transient described below is completed.

When point F is driven negative, a charging current flows from D through R_{12}, C_6, and T_4 to N. The voltage of the grid of T_5 therefore approaches that of D in a manner that can be represented approximately by an expression involving an exponential with a negative exponent. The grid voltage becomes equal to that of the cathode of tube T_5 in a time of about 0.69 $R_{12}C_6$ seconds, where R is in ohms and C is in farads. After the elapse of this time, tube T_5 begins to conduct again, and the voltage of the screen grid of tube T_4 is thereby decreased. This causes a decrease of the plate current and an increase in the positive voltage of point E, which in turn increases the voltage of point F and accelerates the return of the circuit to its initial condition. The outer grid number 2 of the Strobotron tube T_2 is thus suddenly brought back to a low positive voltage after a time dependent upon the size of capacitor C_6.

Figure 1. Electronic control circuit for a resistance welder

The control unit is enclosed by dotted lines

Circuit constants:

R_1 — 10 ohms
R_2 — 5,000 ohms
R_3 — 3,000 ohms
R_4 — 100,000 ohms
R_5 —1,000,000 ohms
R_6 — 25,000 ohms
R_7 —1,000,000 ohms
R_8 —1,000,000 ohms
R_9 — 200 ohms
R_{10}— 100,000 ohms
R_{11}— 100,000 ohms
R_{12}—1,000,000 ohms
R_{13}— 100,000 ohms
R_{14}— 50,000 ohms
R_{15}— 25,000 ohms
T_1 —Type 6H6
T_2 —Strobotron
T_3 —Type 1V
T_4 —Type 77
T_5 —Type 01A
T_6 —Type 6X5
TR_1—Peaking transformer
TR_2—Automobile-type spark coil
TR_3—Plate and filament supply transformer (Thordarson number T7020)
C_1 —1.0 microfarad
C_2 —0.001 microfarad
C_3 —0.005 microfarad
C_4 —1.0 microfarad
C_5 —0.005 microfarad
C_6 —Decade capacitor
C_7 —24 microfarad

The operation of the time-delay circuit described here results in the application of a positive voltage to grid number 2 of the Strobotron tube for a controlled time interval whenever an impulse is delivered to grid number 1 of tube T_4. During this interval the Strobotron will fire at 120-cycles-per-second frequency in accordance with the voltage supplied to its grid number 1, and pulses synchronized with the supply-voltage alternations will be delivered to the starting bands of the band-ignitor tubes.

The first control-unit requirement listed here, namely, that the unit deliver a controllable number of high-voltage pulses to the starting bands of the band-ignitor tubes, is only partially fulfilled by the circuit thus far described. If a control switch were incorporated in the circuit merely to connect grid number 1 of tube T_4 to a source of positive voltage upon manual closure, the number of half cycles of welding current conducted during the controlled time interval would depend upon the time in the cycle at which the control switch was closed, and the welding current would continue as long as the switch was held closed.

These difficulties are avoided by interposing tube T_3 with its associated circuit to deliver only one short impulse to grid number 1 of tube T_4 regardless of the length of time the control switch S is held closed; and to cause this impulse to occur at a particular point in the supply-voltage cycle regardless of when the control switch is closed. Control switch S (actually a relay for remote control) is normally closed on position 2, and operation transfers it to position 1. A small portion (about ten volts) of the next succeeding rectified peak of voltage from transformer TR_1 then appears across resistor R_4 and charges capacitor C_5 through tube T_3 and resistor R_{10}. A small voltage pulse caused by the charging current through R_{10} is delivered to tube T_4 and the controlled weld is thereby initiated. Capacitor C_5 cannot discharge while the control switch is held closed on position 1 because of the rectifier tube T_3. Hence, only one pulse is transmitted to tube T_4 each time the switch is operated, and a number of half cycles of welding current predetermined by the setting of capacitor C_6 occurs.

Controls and Adjustments

The external adjustments on the panel of the control unit, illustrated in figure 3, are the half-cycle adjustment, C_6, which is a decade capacitor, and the conduction-angle control R_2 for adjustment of the starting point of conduction within the half cycles. The decade capacitor provides for any number of half cycles from one to ten, and the range may be extended to much larger numbers of half cycles by the addition of an external supplementary capacitor in parallel with C_6. The conduction-angle control permits delay of the point of ignition of the band-ignitor tubes in each half-cycle of supply voltage beyond the natural lagging phase angle of the welding-load current. This delay makes possible the elimination of a high transient surge of magnetizing current,[4] and a reduction in crest current.[5] It insures exact duplication of successive welds and gives smooth, continuous control of the welding heat. In all, four methods of controlling the welding heat are provided, namely, by adjustment of (1) the number of half cycles, (2) the firing point within the half cycles, (3) the resistor R_1 in series with the welder-transformer primary winding (practicable only with small welders), and (4) the taps on the welder-transformer windings.

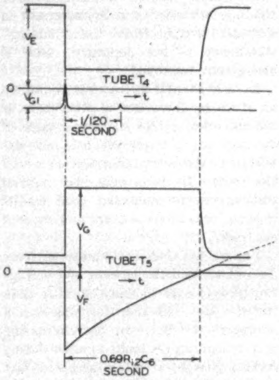


Figure 2. Approximate wave forms of the voltages in the time-delay circuit

Voltages are for the lettered points in the circuit, and are measured from the cathode of the corresponding tube

Two internal adjustments are included in the control unit to provide for differences in replacement tubes. These are the voltage adjustment R_7 for the grid number 2 of the Strobotron tube, and the grid-bias adjustment R_{15} for the tube T_4. The adjustable contact on R_{15} is set by advancing it upward on the diagram slightly beyond the point at which the time-delay circuit becomes stable and ceases to oscillate continuously with the control switch *S* in its normal position number 2. The setting of the adjustable contact on resistor R_7 is made by advancing it upward on the diagram slightly beyond the point at which the Strobotron first fires when the control switch is moved to position number 1. Once made, these adjustments should not have to be changed.

Performance

A desirable feature of the resistance-welder control circuit is that the main tubes in it are of the cold-cathode type and do not require heating power. As a result, the Strobotron and the band-ignitor tubes are ready for service instantly without the time-delay requirement for cathode heating necessary with some control tubes. The only time delay in starting the unit is that inherent in the cathodes of the other tubes, which are of the radio-receiver type and require a time comparable to that of the ordinary radio receiver for heating. When the main switch is closed to excite the whole circuit, the cathode of the triode T_5 in the time-delay circuit must reach its thermionic emitting temperature before that of the rectifier tube T_6; otherwise grid number 2 of the Strobotron tube will receive a positive voltage, and welding-current cycles will continue steadily for a few seconds. For this reason a quick-heating filament-type tube, the 01*A*, is included in the circuit.

The calibration of the decade capacitor C_6 is practically independent of manufacturing variations in tubes T_4 and T_5 because the resistors R_{11}, R_{12}, R_{13}, and R_{14} are large compared with the effective resistances of the tubes while they are conducting. Thus it is not necessary to recalibrate the capacitor when tubes are changed.

The life of the Strobotron and the band-ignitor tubes depends primarily on the number of welds made, and is independent of the standby intervals between welds during which the unit is left connected to the power supply. These cold-cathode tubes are of simple and rugged construction, and laboratory tests indicate their life to be adequate to render them practical for spot-weld service.

The control unit may be assembled in a small cabinet no larger than a table-model radiobroadcast receiver, for all the tubes and circuits in it are of low power ratings. The unit is sufficiently powerful to control the band-ignitor tubes for a welder of any size. It is also suitable for the control of ignitron-type tubes if the spark coil TR_2 is replaced by an appropriate transformer. The model illustrated in figure 3 has been used thus far to operate a small welder of one-kilovolt-ampere rating with current crests of 300

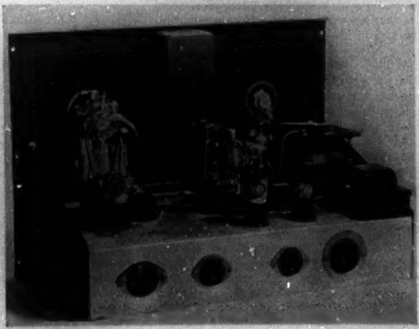


Figure 3. External and internal views of the resistance-welder control-unit assembly

amperes, but there appears to be no inherent limitation to its use with larger welders if band-ignitor tubes of sufficiently high ratings are substituted in the circuit.

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1. **T. S. Gray** and **W. B. Nottingham,** *Review of Scientific Instruments,* volume 8, February 1937, pages 65–8.

2. **A. B. White, W. B. Nottingham, H. E. Edgerton,** and **K. J. Germeshausen,** *Electronics,* volume 10, March 1937, pages 18–21.

3. **O. Kiltie,** ELECTRICAL ENGINEERING, volume 51, November 1932, page 802.

4. **R. N. Stoddard,** ELECTRICAL ENGINEERING, volume 53, October 1934, page 1366.

5. **J. W. Dawson,** ELECTRICAL ENGINEERING, volume 55, December 1936, page 1374.

Discussion

L. G. Levoy (General Electric Co., Schenectady, N. Y.): The authors have presented an interesting description of a control circuit for spot-welding applications. I would like to ask what actual crest voltages are required on the ignitor bands of the power tubes? High voltages, even though of the nature of an impulse of short duration, are frequently a barrier to widespread application, particularly when the same result can be accomplished without the use of high voltage.

The circuit proposed in figure 1 is synchronous starting with respect to phase angle, but is random starting with respect to polarity of the first half cycle of any spot. In a simple spot welder there are no cumulative residual current transients, since the electrodes are lifted from the work after the termination of each spot. There is, however, some residual flux left in the core of the welding transformer. If the spot length and polarity of starting are properly controlled, the effect of this residual flux can be minimized. In a welding transformer liberally designed for the voltage applied, the effects of residual flux are small, but in many cases welding transformers normally run at high flux densities, in which case the effect of residual magnetism may give rise to primary transients and also may result in greater magnetic energy storage at the termination of the spot. This often gives rise to undesirable arcing at the electrodes when they are quickly removed from the work after the cessation of primary current, tending to shorten the electrode life and also cause pitting of the surface of the work. For these reasons, where precise work is to be done it would be desirable to incorporate the feature of unipolar starting in the control circuit. To get the maximum benefit of this, the spot length should be adjustable in full cycle increments only. These features are available on existing electronic controls. Where odd half-cycle spot lengths are required, antipolar starting is provided for in available electronic controls. For interrupted spot or seam welding, proper control of the starting polarity and spot length is a requisite to consistent welding.

T. S. Gray: The actual crest voltage required on the ignitor bands of the power tubes, about which Mr. Levoy asks, depends on the thickness of the glass through which the voltage acts. In glass tubes made with ordinary borosilicate glass approximately $1^1/_2$ millimeters thick, the crest voltage required is about ten kilovolts. Thinner glass between the metal ignitor and the mercury pool makes possible a reduction of this crest voltage by a factor of three to five (see K. J. Germeshausen, *Physical Review,* volume 55, Jan. 15, 1939, page 228). There is no serious problem of high-voltage wiring or protection associated with the use of the band-ignitor tubes, because the problem is similar to that in an automobile and the spark coil may be located nearby in the same metal enclosure with them.

The suggestion by Mr. Levoy that for a minimum magnetizing transient the timing should be adjusted in multiples of a full cycle is important, and this adjustment would ordinarily be made except when a single half cycle is desired. It is interesting that provision for unipolar or antipolar starting becomes important with large welders and with interrupted spot or seam welding. In our work with small spot welders we have not observed arcing of the electrodes or pitting of the surface of the work as a result of the lack of such control.

The A-C Arc Progresses

CLAUDE J. HOLSLAG
MEMBER AIEE

ELECTRIC WELDING could be pictured very accurately to an electrical engineer as an attempt at maintenance and control of a short circuit. In resistance welding, whether spot, butt, roller seam, flash, or projection, it is an actual short circuit with definite resistance and low voltage drop. The arc is the maintenance of a short circuit in a very unstable medium, generally air, with a comparatively high voltage and nature conspiring to extinguish it. The "holding" of a metal arc is especially difficult as one electrode is continuously melting away into the other electrode called the "work."

In 1930 the AIEE published an article by the writer on time recovery and control of the welding current and voltage, as shown by an oscillograph, with both direct and alternating current. Direct current holds an arc very readily and for the single carbon arc it is so far unchallenged. Alternating current, however, is used for the double carbon arc and for hydrogen-flame double or single tungsten shielded arc welding. It was considered impossible to hold an a-c metallic arc continuously previous to the writer's efforts. The a-c arc, however, once established, is much easier to control and nonferrous alloy welding, even with copper-base alloys, is being accomplished by the use of capacitance and the high-frequency pilot circuit. The introduction of desirable gases, instead of air with its oxygen and nitrogen, to help hold the arc by means of covering on the rods or by surrounding the arc as in "atomic" or "shielded arc" welding, is a very great improvement in the arc stream from the "holding" standpoint. The deposited metal is also greatly improved.

In 1920 the AIEE JOURNAL contained an article by the writer on phenomena of a-c arc welding, also showing a table of electrode arc voltages which is now up-to-date by the general adoption of covered electrodes. If there were ever natural companions for combined good results it is the firm of the a-c arc and covered electrodes.

The a-c arc does not wander or have "arc blow" and the current and voltage can be held ever so much steadier than the d-c arc. Whether for this reason or due to the fact that the alternating current agitates the molten puddle during crystal growth, it is an accepted fact that alternating current and covered electrodes make the highest quality weld deposit.

Class I pressure vessels are welded with alternating current. The $11,000,000 Boulder Dam job was welded with alternating current.

Paper number **39-67**, recommended by the AIEE committee on electric welding, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted April 30, 1938; made available for preprinting December 31, 1938.

CLAUDE J. HOLSLAG is president of the Electric Arc Cutting and Welding Company, Newark, N. J.

Copper alloys—Monel, bronzes, etc.—still give trouble with the metallic a-c arc although the double carbon arc is better for brazing with alternating current than with direct current. Capacitor circuits have brought copper, bronze, and brass alloy welding into the scope of the a-c metallic arc. Previously the d-c carbon arc was the solution for copper-alloy welding and brazing not done by gas welding. The "atomic" or hydrogen-bathed arc has opened a new field for alternating current in that special jobs requiring no contact with the air are extending the field of the art. Copper welding, Monel, stainless, and "tricky" alloys are handled with ease by the hydrogen-flame arc.

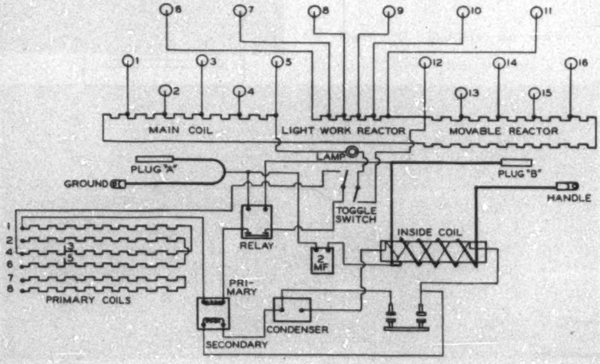
The superimposed high-frequency circuit is a very desirable adjunct for special welding purposes. With an air-core transformer and capacitor oscillating circuit (figure 1) high frequency, as used in vaudeville shows, is here made to jump the gap and break down the surfaces for the following of the arc current. This is especially desirable for thin work, stainless, mill-scale plates, and tack welding. It is also useful in starting and maintaining the hydrogen "atomic" arc and in maintaining any arc under specially disadvantageous circumstances such as oil, wind blowing, grease, water, etc. This high frequency can either be superimposed on a-c or d-c arc circuits but is generally used with low-current a-c circuits.

The writer has preached the advantages of covered rods and a-c welding for 26 years. The opposition to covered electrodes and alternating current was tremendous. Even though the AIEE published the writer's 1920 article, the major companies fought acceptance of the truth of the qualities shown very strongly. In fact, acceptance of alternating current only recently has become universal in the United States although in Europe this progress had preceded ours. However, in this country for several years all important work such as class I pressure vessels and oil-fired vessels, penstocks, and those companies who have a testing laboratory use a-c welding for its quality product. In addition to its quality, alternating current is faster and, hence, cheaper in labor, which is 80 per cent of the cost of arc welding. The first cost and the electrical cost of operation is less with alternating current and there is no maintenance. There is no electrolytic action or positive or negative corrosive forces. The deposit is neutral, of finer grain structure, free from porosity, and generally better on all iron and steel work.

Why has a-c arc welding progress been so retarded? The chief sales disadvantage against alternating current has been the open-circuit voltage, which lowers the power factor and is liable to cause nervous shocks especially in wet places.

The writer has lately developed a unique method of lowering the voltage by an odd circuit as shown in figure 2. In its simplest form it consists of a leakage transformer with two windings. One winding is across the operating voltage, say 80 volts, and the other winding is in series with the operating voltage, open-circuited until contact is made. On open circuit the second winding subtracts from the first one leaving the voltage low, say 40 volts. During welding the second winding becomes a reactance, lowering the voltage to the arc voltage of 40. There is no doubt but that this action occurs through phase shifting, exactly as described in the 1920 article. In that case, however, the voltages were added. In this case the second winding is used to subtract on open circuit by plain transformer action and under load to subtract by voltage reactance drop and phase shift. Capacitance can be used in this circuit to raise the bucking voltage which disappears under load. Professor Comfort A. Adams and the writer have had a good many months of argumentation as to the interaction of these two circuits and at various current and turn values the action is widely different. The writer admits he cannot explain everything that goes on in this circuit. Perhaps some professor and student will take this up as a thesis. Whether explained or not, it operates as described, and is especially applicable for heavy welding. Figure 3 shows a modified form of this circuit.

Figure 1. Superimposed high-frequency circuit



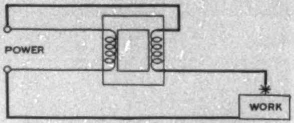


Figure 2. Basic buck-back circuit

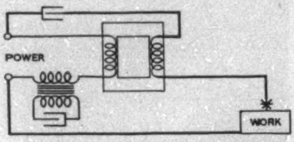


Figure 3. Compensated buck-back circuit

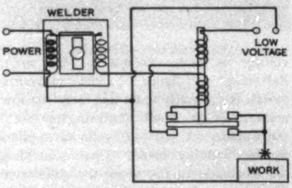


Figure 4. Original low-voltage control circuit

The writer has also developed a method of holding the open-circuit voltage low while enjoying the steadiness of applied higher voltage (although this higher

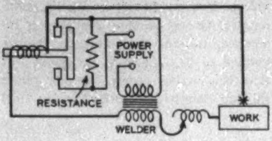

POWER SUPPLY
RESISTANCE
WELDER
WORK

Figure 5. Primary voltage cutdown control

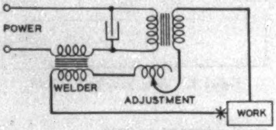

POWER
WELDER
ADJUSTMENT
WORK

Figure 6. Primary counterbalanced

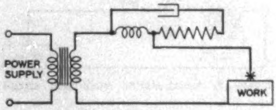

POWER SUPPLY
WORK

Figure 7. Hunter series circuit

voltage is never present at the arc). Referring to figure 4, the magnetic switch is normally open and only the low voltage of a small (bell-ringing size) transformer at, say, 40 volts is applied to the welding leads. Upon touching the electrode to the work the difference in reactance between the work electrode circuit and the transformer parallel circuit causes the magnetic switch to close the arc circuit, opening the "bringing in" circuit and the switch is held in by a series winding until the arc exceeds, say, 40 volts when it drops out. This system is "positive safe" and thousands are in use, some with the doubtful improvement of a time delay in dropping out. The device is used on large welding transformers in the primary circuit thus cutting off even the core-loss expense and for small arc welders the 40 volts can be obtained from the welder itself by a tap or separate winding.

Another method of accomplishing low voltage, figure 5, also developed by the writer, is to put a resistance or reactance or capacitance in the primary of the arc welding transformer and by a simple series relay cut out this voltage-lowering impediment during welding.

Still another method, figure 6, is to counterbalance a reactance or capacitance in the primary circuit by the welding current but this is limited to a very short range of welding current with insufficient range of adjustment.

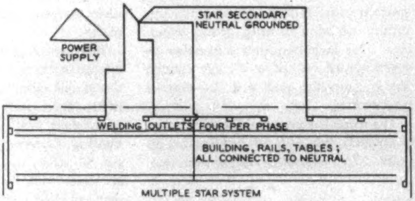
The power factor of a-c arc welding can be overcome with capacitors, which capacitors also give spontaneity of arc, this action being similar to that of condenser coils in an automobile ignition circuit or as in an oil-boiler ignition transformer. Capacitors can also be used to lower the open-circuit voltage by neutralizing the inductive action, as in the Hunter circuit (T. M. Hunter, president of the American Transformer Company), figure 7. Of course, anywhere a capacitor is in the circuit it has a correcting effect and if enough capacity is used the power factor can be leading. F. C. Owens of Fayetteville, N. C., also helps power factor and the welding arc by use of capacitors in multiple, and wherever a capacitor is placed in or across all or part of an arcing circuit, it helps the arc and, of course, the power factor. Balancing of capacitance in the primary of the 40-40 circuit is also feasible but capacitors are only as reliable as storage batteries.

Another disadvantage of a-c welding, from an operating standpoint, is the fact that it operates single phase and very often not on a line built for single phase and the voltage droops and the welding withers accordingly.

With the multiple star system, as schematic wiring diagram and photographs show, there is an adequate tank or reservoir in the shape of the mother transformer absolutely to preclude any drooping. With the a-c multiple system the efficiency is three times better than with a multiple d-c system and twice as good as that of any single-motor-generator system. The independent control of both amperage and voltage at each station also removes one of the disadvantages of d-c multiple system where only one voltage is available for the entire system. D-c multiple installations would be used more if their efficiency were not so poor.

A typical welding-shop floor plan is shown in figure 8, with mother transformer *A* star connected, neutral grounded to building steel, rails, work tables, etc., and one cable to each general location to outlet transformers along the wall, overhead, or generally out of the way. The operator has but one welding lead. D-c motor generator sets have two welding leads, three power

Figure 8. Multiple star system


POWER SUPPLY
STAR SECONDARY NEUTRAL GROUNDED
WELDING OUTLETS FOUR PER PHASE
BUILDING, RAILS, TABLES; ALL CONNECTED TO NEUTRAL
MULTIPLE STAR SYSTEM

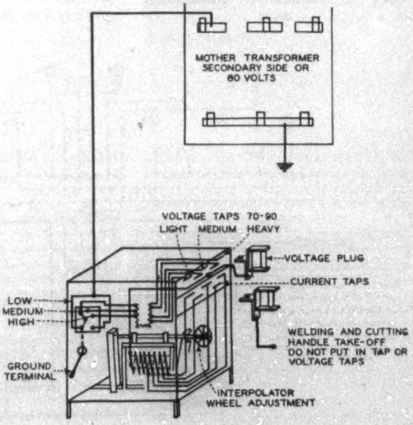

MOTHER TRANSFORMER SECONDARY SIDE OR 80 VOLTS
VOLTAGE TAPS 70-90
LIGHT MEDIUM HEAVY
VOLTAGE PLUG
CURRENT TAPS
LOW
MEDIUM
HIGH
WELDING AND CUTTING HANDLE TAKE-OFF DO NOT PUT IN TAP OR VOLTAGE TAPS
GROUND TERMINAL
INTERPOLATOR WHEEL ADJUSTMENT

Figure 9. Detail, mother transformer and station

leads, and a motor generator for each arc.

Still another great advantage of the multiple star system is the space in the shop is not interfered with welding apparatus, nothing but outlets on the wall being necessary for this system.

The 220-, 440-, 550-volt power lead wires to the alleged portable single-operator machines are done away with and a very great hazard of fire and safety is obviated.

The second greatest detriment to single-operator transformer operation is interference which is a by-product of electrical line drop. The mother transformer reservoir system precludes this and I^2R is a negligible component where reactance control is used. In single-operator installations, part of each unit is tied up if the full capacity is not used.

D-c multiple systems are popular where one single voltage and no polarity reversing will do but the resistance method of control of d-c installations makes them very costly to operate and the large motor and its losses makes fractional operation very uneconomical. This is obviated in the multiple star system (figure 9). In single-operator installations there can be no diversity factor of the equipment. In the multiple arrangement the entire output can be had or any fraction of it with the maximum diversity factor and efficiency and no stand-by losses.

The a-c arc is one-fourth of the cost of a multiple d-c system and one-half that of any d-c motor generator set.

Where the work is heavy enough to stand the reservoir capacity, chipping and caulking are not necessary, and up to $1^1/_2$-inch-thick plates, this system has accomplished this desirable step forward with the testing and consent of the interested authorities, both the insurance, The American Society of Mechanical Engineers and the National Inspection Bureau. This is a saving of 50 per cent alone and alternating current is faster than direct current generally.

This multiple ring system (figure 10) is a planned installation. The country's welding progress has arrived at a point where this Topsy-like method of growing by adding single-operator machines should be superceded by an orderly designed welding power plant. In this connection, this system fits in beautifully with electric power, steam, or Diesel installations, or combinations of either. This multiple star system, which provides great chunks of power where needed without affecting the light and medium work outlets, is also ideal for the submerged-arc heavy welding or any requirement where combined amperage is necessary.

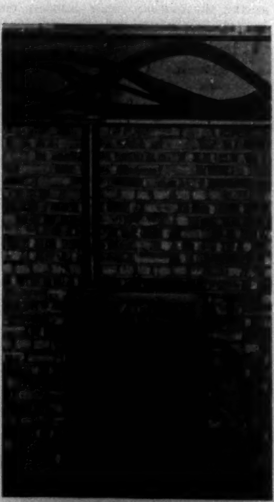


Figure 10. Multiple star system

Hamler boiler, Chicago

Figure 11. Public Service preheating welding

The latest development of induction hysteresis stress-relieving operates perfectly with this system. An illustration of two pipe joints preheated, welded, and normalized with the Smith-Dolan a-c preheating development is shown in figure 11. There were 8,900 such welds at the Essex plant of the Public Service Electric and Gas Company accomplished perfectly with their 60-cycle power. The test pressure on these joints was 2,250 pounds; the operating temperature was 950 degrees Fahrenheit. There were no leaks or failures of any kind. The metal was that tricky alloy to weld, carbon molybdenum steel.

The d-c arc is better for overhead and some vertical welding. The general use of covered electrodes has reduced this advantage of direct current materially and most industrial work is now "positioned."

Alternating current has become the steam shovel of welding, leaving direct current for the special work tentatively.

Discussion

A. U. Welch (General Electric Company, Pittsfield, Mass.): As pointed out in this paper, a-c arc welding has made great strides recently, largely due to the fine performance obtained from modern covered electrodes. The remarkable improvement in arc stability obtained from these newer covered electrodes has almost eliminated the necessity for complicated welding circuits and equipment, and excellent results are being obtained with simple adjustable reactance control of arc current.

The circuit shown in figure 2 is merely a regulating autotransformer with its series winding connected bucking. It gives exactly the same performance as any other reactance-controlled transformer with the same open-circuit voltage and current. The circuits shown in figures 3, 6, and 7 use capacitance in series with inductive reactance to improve arc stability or to permit lowering open-circuit voltage somewhat with the same arc stability. The circuit operates in such manner that if an arc should fail to restrike after passing through normal current zero, the voltage across the reactor, or the equivalent reactance of the transformer, collapses immediately, leaving a charge on the capacitor. The voltage available to restrike the arc is then the sum of the capacitor and transformer open-circuit voltage, which will be higher than the normal open-circuit voltage. The capacitor soon discharges through its transformer, autotransformer, or discharge coil, and the circuit returns to normal open-circuit voltage. Since this is a series resonant circuit, extreme care must be taken in applying it to arc welding in order to avoid dangerous overvoltages on the capacitors, reactors, and transformers. Operation would not be so precarious if no adjustment of current output were required.

In order to obtain improved arc stability, it is necessary that the capacitive reactance partly compensate for the inductive reactance, or vice versa. In order to control current, either the inductive or capacitive reactance must be varied, and if the range of current is at all wide, the two reactances are likely to become so nearly equal as to form a completely resonant circuit with very high voltages on both capacitor and reactor. Furthermore, any additional reactance inserted in the circuit, which may be inherent reactance in the primary and secondary leads, will greatly disturb the operation of the circuit, change the current output to a very marked extent, and in some cases throw the circuit into resonance. Even the normal starting transient currents of the reactor and capacitor are apt to change the inductance sufficiently to cause resonance troubles.

A connection of capacitors in shunt across the primary leads of an arc welding transformer improves power factor, but has no effect on the welding arc except possibly to help hold the primary voltage up to its rated value.

The "multiple star system," which is merely a system of distributing power at low voltage instead of primary voltage, in general offers no advantage over distributing at usual primary voltages, such as 220, 440, or 550 volts. The price of adjustable reactors for the multiple star system is essentially the same as the price of arc welding transformers designed to run off primary voltage, and the elaborate system of heavy cables required for distributing at low voltage is much more expensive than the usual primary wiring in conduit. This is particularly true if isolating switches are used at each welding station, as is the usual practice, to prevent one faulty unit from shutting down the entire system. The cost of switches for the low-voltage high-current system is very much more than for the primary circuit switches.

The adjustable-reactance welding transformer which is the accepted standard equipment for a-c arc welding has the advantages over the circuits presented in this paper of simplicity, low cost, low maintenance, adaptability, and freedom from overvoltages and other dangerous phenomena.

R. F. Wyer (General Electric Company, Schenectady, N. Y.): As always, Mr. Holslag's paper stimulates a great deal of thought, as he doubtless intended it to do. One idea that seems to me needs further clarification is the use of a three-phase mother transformer and individual welding stations to counteract voltage droop in power circuits that lack stiffness. If a power line into a welding shop is too light to handle the additional load imposed by a-c welding, then the only means for enabling it to carry this load without additional voltage droop is the installation of capacitors, synchronous motors, or synchronous condensers to give a leading-power-factor load sufficient to bring the power factor of the whole plant load up to the point where the total kilovolt-ampere input will not exceed the kilovolt-ampere input previously existing in the plant. That is to say, sufficient leading reactive kilovolt-amperes must be furnished in the form of capacitors or synchronous machines to cancel the lagging reactive kilovolt-amperes of the welding load, and in addition some of the lagging reactive kilovolt-amperes of the other plant load. Only by this means could the total kilovolt-ampere load of the plant, and thus the voltage regulation of the power line be held constant in spite of the addition of the welding load.

The reference to the mother transformer as a tank or reservoir to preclude voltage droop is not clear to me. It might be thought that a three-phase transformer would distribute a single-phase load over all three phases of a three-phase power circuit. This, however, is not true, since a single-phase load on the secondary of a three-phase transformer will result in single-phase power input to the transformer, and the unbalancing effect on the power supply will be just the same as if only a single-phase transformer were used. It is true that if all three phases of the mother transformer are loaded on the secondary side, then the welding load will be distributed on the power line. However the same thing can be accomplished by simply distributing ordinary welding transformers on the three phases of the power circuit, and the load distribution will be accomplished.

It is difficult to see how there could be any economy in distributing welding power at low voltage, such as 80 volts, around a welding shop. This practice is quite the opposite of the trend in power distribution, where the tendency is to go to higher voltages in order to save copper.

Returning to the voltage droop consideration, it would seem that extraordinary precautions would have to be taken to avoid even increasing the voltage droop through the use of low-voltage power distribution to the various welding stations on account of the reactive drop in voltage which is inherent in all a-c distribution circuits. This is directly proportional to current, and since the current carried by the distribution circuit must necessarily be increased when using low-voltage distribution, I would expect that the trouble due to voltage droop would be accentuated rather than counteracted.

To use specific figures, suppose three welders are each using 250 amperes at various locations in a shop. With the ordinary single-operator transformer welder setup, individual 440- or 220-volt leads, one pair from each phase, would be run to the individual transformers. Assuming 440-volt distribution, the current in these leads would be about 46 amperes. The actual primary current would depend on the open-circuit voltage on the secondary of the welding transformer, but 46 amperes is figured on the basis of an 80-volt open-circuit voltage, by multiplying the welding current by the ratio of transformation of 80/440.

With an 80-volt distribution system, and assuming that 80 volts open-circuit voltage is desired by the operator, then the current in the distribution circuit between the mother transformer and the individual welding stations will be 250 amperes, because there is no ratio of transformation in the individual welding station. Of course, if the open circuit voltage at the welding station were reduced to 60 volts, then the current in the 80-volt distribution circuit would be somewhat less; it would be 6/8 × 250, or about 190 amperes.

Remembering that the loss due to I^2R in the distribution lines is proportional to the square of the current in those lines, with a ratio of 46 to 200 amperes, the ratio of the weights of copper which will be required to give the same loss in the two systems will be 30 to 1.

In view of the foregoing considerations, it appears to me that a low-voltage distribution system such as is proposed by Mr. Holslag would result in an increase in interference between operators due to voltage drop in the supply lines to the individual welding stations, instead of a decrease as suggested in the paper.

K. L. Hansen (Harnischfeger Corporation, Milwaukee, Wis.): The paper is replete with assertions which no doubt will be accepted for what they are worth. For example, it is stated that the efficiency of the a-c multiple system is twice as high as that of any single-motor-generator system. As the efficiency of some motor generator sets is above 50 per cent, the efficiency of the a-c multiple system should be over 100 per cent, which is, to say the least, doubtful.

There is, however, one statement in the paper which will for a certainty be accepted without reservation, namely, that the author has preached the advantages of a-c arc welding for 26 years. In view of Mr. Holslag's statement that the acceptance of the a-c arc has now become universal, it would seem that the endless repetition in the paper of arguments which have become not only familiar, but threadbare, through 26 years of preaching, should be superfluous.

Perhaps the acceptance of the a-c arc has not been so universal as this statement asserts. Indeed, what follows immediately tacitly implies that such is not the case. The question is asked, "Why has a-c arc-welding progress been so retarded?" Mr. Holslag answers that the chief reason is the high open-circuit voltage, which lowers the power factor and is liable to cause nervous shocks, especially in wet places.

In the following two paragraphs Mr. Holslag gives a hint of what might have been an interesting topic for an engineering paper, namely, a method he has recently developed for overcoming the drawback to a-c welding just mentioned. The description is, however, entirely too inadequate for anyone to form an intelligent conception of its operation, let alone judge its merits. Had the paper omitted the great deal of needless repetition of old arguments and concentrated on an engineering discussion of this new development, it might have been a good one.

Mr. Holslag states that in Europe progress in a-c welding has preceded ours. C. H. Jennings, research engineer of the Westinghouse company, has recently returned from Europe where he spent considerable time studying various phases of welding. He has written at least one article and given a number of addresses on various phases of European welding practice. In Germany he found that the d-c arc preponderates, undoubtedly because of the great use of bare electrodes still prevalent there. The situation in England is interesting.

According to Mr. Jennings, the use of coated electrodes became prevalent in England at the very inception of the metallic arc-welding process. Furthermore, the strict requirements regarding inrush currents of induction motors made starting equipment expensive and militated against the use of motor generator sets. Hence, the conditions were ideal for a-c welding, and that is about all there was at the beginning. Some years ago, however, enough d-c welding had developed here to make the ratio about 20 per cent direct current to 80 per cent alternating current. The continued increasing use of direct current has made that ratio at the present time 40 per cent direct current to 60 per cent alternating current, and Mr. Jennings estimates that continuance of the present trend will shortly make it a 50-50 ratio. What will happen after that is problematical. If Great Britain possessed an ardent advocate of d-c welding, who had been preaching its advantages for a quarter of a century, he would now undoubtedly be prepared to publish a paper on "The D-C Arc Progresses."

Recently, when Mr. Jennings addressed the Milwaukee section of the American Welding Society on this subject, he was asked if he could account for this rapid increase of d-c welding in England. His answer was that the inability of the a-c arc to weld successfully nonferrous metals, such as aluminum, copper, copper alloys, nickel, nickel alloys, nickel clad steel, etc., and the superiority of the d-c arc in welding of some alloy steels and in vertical and overhead welding, are unquestionably factors in this changing ratio of d-c to a-c welding in England. Even Mr. Holslag concedes that d-c welding has these advantages, and for that reason gives it a lease of life, although only a tentative one.

There is one other statement in the paper that I in general agree with, although not 100 per cent. In his concluding paragraph Mr. Holslag says that alternating current has become the steam shovel of welding. It is well known, however, that the steam shovel has become an entirely antiquated piece of machinery, having completely given way to the internal combustion engine, that is, the gasoline or Diesel engine, and to some extent to the electric-driven shovel. Mr. Holslag did well in choosing an analogy, although I would not go so far as he does and compare a-c welding with the steam shovel, which is a completely outmoded piece of equipment. That is entirely unfair to a-c welding. There is, however, one strong similarity. If a steam shovel today were to be taken in trade for an up-to-date machine, its trade-in value would approach the vanishing point. It has been our experience that the same holds true when we are confronted with the situation of taking a welding transformer in trade for a motor generator set.

W. Richter (A. O. Smith Corporation, Milwaukee, Wis.): This discussion is confined to an analysis of the so-called "basic buck-back circuit" as shown in figure 2 of the paper.

For the purpose of studying the behavior of the circuit, replace the arc by a variable load resistance and the transformer by two coils, having self- and mutual inductance, as shown in figure 1 of this discussion.

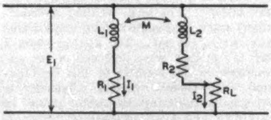


Figure 1

Let

$z_1 = R_1 + jwL_1$ = primary impedance
$z_2 = R_2 + jwL_2$ = secondary impedance
$x = wM$ = mutual impedance
R_L = load resistance
E_1 = primary voltage
E_2 = secondary voltage
I_1 = primary current
I_2 = secondary or load current

We have then:

$$E_1 = I_1 \times z_1 + I_2 jx$$
$$E_1 = I_1 \times jx + I_2 \times (z_2 + R_L)$$

Eliminating I_1, results in

$$I_2 = E_1 \times \frac{z_1 - jx}{z_1(z_2 + R_L) + x^2} = E_1 \times \frac{(z_1 - jx)/z_1}{z_2 + \dfrac{x^2}{z_1} + R_L}$$

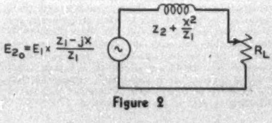


Figure 2

The secondary voltage is found by multiplying I_2 with R_L. This results in

$$E_2 = I_2 \times R_L = \frac{E_1 \times \dfrac{(z_1 - jx)}{Z_1}}{1 + \left(z_2 + \dfrac{x^2}{z_1}\right)/R_L}$$

For $R_L = \infty$ we obtain the open-circuit secondary voltage

$$E_{2_0} = E_1 \times \frac{z_1 - jx}{z_1}$$

Putting this into the equation for I_2, we obtain

$$I_2 = \frac{E_{2_0}}{z_2 + \dfrac{x^2}{z_1} + R_L}$$

This shows that the secondary current is the same as if we employed a generator or transformer with a terminal voltage E_{2_0} and a fixed impedance $z_2 + (x^2/z_1)$ in series with the load as shown in figure 2 of this discussion.

Thomas M. Hunter (American Transformer Company, Newark, N. J.): Mr Holslag's paper is of considerable interest to the welding industry because it describes a number of circuits for improvement in a-c welding. Up until the present time direct current has been largely used for arc welding and alternating current has been used to a lesser extent. Economically alternating current has many advantages over direct current. The reasons for this are as follows:

1. The load factor on the average welding machine is very low and the difference between the running-light losses of a generator and transformer is very high, consequently the operating efficiency of the transformer is very much greater than the generator.
2. The maintenance cost of transformers is negligible as compared with generators.
3. Transformers can be built so that they are much easier to install and transport from time to time than is the case of generators.

The reasons why a-c welding has not been more generally used are as follows:

1. The transformers, as built up to the present time, have open-circuit voltages from 80 to 105 and this has been considered dangerous by many users of welding equipment.
2. The power factor of these transformers is very low, ranging from 20 per cent to 50 per cent lagging.
3. These transformers draw a very large single-phase current and due to the low power factor this affects adversely the line voltage which causes disturbance on other apparatus.
4. In many of the transformer designs the means of controlling current has been awkward and very unsatisfactory from the operating standpoint.
5. Due to the very high reactance of transformers the length of secondary leads has affected the current output to such an extent it was impossible to obtain desired currents at great distances from the transformer.

In Mr. Holslag's paper he shows a number of circuits which tend to overcome these objections. Figure 2 will lower the open-circuit voltage and afford satisfactory welding operation but it does not improve the power factor. In figures 3, 6, and 7 he shows the use of capacitors which do aid in improving power factor as well as lower the open-circuit voltage; in figure 7 a circuit is shown where reactance and capacity is used in series with the secondary circuit of the transformer. This gives very desirable characteristics for arc welding. The capacitors are not in the circuit at no load and are introduced into the circuit in such a manner that there is no tendency for instability, dangerous voltages, etc., and the apparatus is quite simple. Therefore, it is possible to obtain operating characteristics as shown in the data given hereinafter. We are giving data on both a reactance transformer and a resonant-circuit transformer.

A. U. Welch, in his discussion, explains the action of this circuit and I believe his explanation is about what takes place. The welding current builds up voltages across the capacitance and the reactance higher than the impressed voltage and the arc is stabilized due to the fact that the open-circuit voltage and reactance voltage come into play just at the correct moment to maintain the arc. This of course means that a lower voltage can be used at open circuit and still maintain arc stability at least equal to that obtained with reactance transformers. There have been a great many devices offered for lowering this open-circuit voltage and the ones used in the past have been of a mechanical nature such as contactors. This circuit gives a result which is entirely automatic and requires no moving parts.

The control is designed so that a range of current from 20 per cent to 100 per cent can be obtained with stability throughout. The circuits are so interlocked that it is impossible to get complete resonance which would build up excessive voltages. The control is very simple requiring only a relatively few seconds to change from maximum to minimum or vice versa. It is made either for installation in the transformer or external, in which case it can be installed at a distant point. In many installations it is desirable to have the welding transformer installed on the balcony or some other place so as to conserve floor space and the control unit, being relatively small, can be installed near the operator, the interconnecting wires being of low capacity requirements. In this respect this control duplicates the practical results of a generator control. It is a very simple matter to make the control device motor operated.

Table I

Test	Reactance Transformer	Capacitor Transformer
Drop in line voltage when thrown on line (per cent)	15	0
Power factor (per cent)	51 lagging	88 leading
Time required to obtain complete range in current	2 minutes	10 seconds
Change in current when secondary leads were increased from 60 foot length to 350 feet (per cent drop)	35	20
Current drawn on primary (amperes)	120	65
Open-circuit voltage on the secondary (volts)	83/105	64
Electrical efficiency (per cent)	88	87

Comparative tests of the capacitor control transformer versus the reactance control, 500-ampere capacity in both cases, give the data in table I of this discussion. In making these tests, a circuit of 150-kva capacity, 440 volts, 60 cycles, was used. All data given were taken at 500 amperes output from the secondary.

Summing up, I would like to refer to a statement made by Mr. Welch which I quote as follows:

> The adjustable-reactance welding transformer which is the accepted standard equipment for a-c arc welding has the advantages over the circuits presented in this paper of simplicity, low cost, low maintenance, adaptability, and freedom from overvoltages and other dangerous phenomena.

In my opinion no device is accepted as standard when better equipment is available. The comparative data given here fully justify the consideration of the resonant circuit for arc welding particularly when these advantages can be had with no sacrifice in operating characteristics.

Claude J. Holslag: The leading electric-welding companies in this country have fought the acceptance of the truth that the combination of a-c transformer welding and covered electrodes was and is the greatest advance in metal construction in its entire history. The large welding manufacturers made a concerted effort to stifle this advance to the art. The kindest thing I can say is they were innocent of the advantages to this country of this combination. The General Electric Company is apparently following the same policy in regard to the low-voltage developments and multiple star system. I notice Mr. Hansen is still of the d-c opinion that prevailed a decade ago. I would like to call his attention to the fact that even the Lincoln company has been attempting to sell a revolving a-c welder for the last five years and is now announcing a static transformer welder and, hence, friend Hansen is the last of the d-c "die hards."

The statement is made in the discussion of my paper that our multiple star system is not different from a grouping of single operator sets. In regard to this, I would like to refer to a three-page article in the April 1938 *Welding Engineer* which was written without my knowledge by Messrs. McGuire and Wood, president and general manager, respectively, of the Hamler Boiler and Tank Company, Chicago, where a 7,200-ampere installation was made by our company. We are listing a summary as made by another large company.

MULTIPLE-STAR-SYSTEM COMPARISON TABLE WITH OTHER ARC-WELDING EQUIPMENT

Versus A-C Single Arc Sets

1. Initial cost less
2. No voltage-drop interference
3.* Better load factor
4. Planned system; no overloaded circuits
5. Balance of phase load

Versus D-C Single Arc Sets

1. Initial cost less
2. Consumes less power—no idle loss—more efficient
3.* Advantage of load factor
4. Planned system with no overloaded circuits
5. Uses common ground

Versus D-C Multiple Arc System

1. Initial cost less
2. Consumes less power—no idle loss—more efficient
3. No interference of operators
4. Open circuit not fixed, but is arranged for adjustment

* Under better load factor, I would like to add the explanation that with single operator sets it is certainly obvious that if a fraction of their capacity is used for welding the rest of their capacity is not available but with the multiple star system half of the unused capacity of, say, two single operator sets could be added to create a third welding station. It is our actual experience that the capacity of this system is doubled because of this fact.

In regard to the reference that a man came back from Germany, who saw bare wire and d-c welding, this is due to one reason, namely; they dislike England and England's advance in welding with covered wire. However, for the last ten years, covered wires have been permeating Germany from all directions. Our Holland and our Scandinavian agents advise they have been having great success with alternating current and covered electrodes for the last ten years all over Europe, especially in Germany. Any person who could not see the advantages of covered wire now would be obtuse, indeed.

So as not to smother this triumph with words I will just point out that the exception noted in Mr. Hunter's discussion is the reactance means of controlling the current, which this company has developed, by which any amount of current can be varied from 10 per cent to 100 per cent with no greater power required under load than open circuit. Comparing this to, say, reactance controls which require two arms of a strong man to open and a very heavy motor for remote control and with which scheme it is practically impossible to move under load, I am quoting Doctor Comfort A. Adams that he cannot understand why the electrical industry has missed such a simple electromagnetic mechanical current-varying solution as we have developed.

Although Mr. Richter presents a very simple and interesting analysis of my basic buck-back transformer, the method which he employs is wholly unsatisfactory for power transformers and was abandoned many years ago as far as that application was concerned. The reason for this abandonment was that this method involved a determination of a relatively small quantity by the difference between two relatively large quantities, both of which were hypothetical and widely variable.

In other words, L_1, L_2, and M as used in Mr. Richter's discussion are all widely variable.

If Mr. Richter's conclusion is correct, to the effect that this arrangement works the same as would a simple leaky welding transformer with an open-circuit voltage equal to E_{20}, it is obvious that this arrangement has no advantage. That this conclusion is not correct is obvious from the following fact: A welding transformer with an open-circuit voltage of 45 or 50 will not maintain a stable arc, whereas my buck-back transformer with an open-circuit voltage of 45 or 50 does maintain a stable arc.

The explanation of this fact is not obvious, but may be due to some short-time transient effect, the analysis of which is too difficult for me to tackle.

If the problem were as simple as Mr. Richter seems to think, it would have been solved long ago.

Effect of Restriking on Recovery Voltage

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MEMBER AIEE

IT IS well known that when any portion of an electrical circuit is either opened or closed a transient condition will usually exist for some period, depending on the damping, before the circuit assumes its new steady state. In power circuits, such switching operations are the application and removal of faults or loads and the connecting or separating of various parts of a system. The transient conditions resulting from these switching operations give rise in some cases to overvoltages, which can be calculated by straightforward and more or less well-known methods, if the transient circuit parameters are known. However, in some instances voltages much higher than those predicted by such calculations have been obtained, and various explanations[1–6] have been offered for these occurrences.

In the present paper, it is suggested that some of these cases of excess voltage arise from the opening of a circuit which is already in a transient condition, the transient condition being caused by immediately preceding switching operations. For example, under certain circuit conditions they may arise from successive momentary clearings and re-establishments of a circuit at such short intervals as to allow comparatively little decay of the transients between these events. Under such conditions it is shown to be theoretically possible to build up extremely high voltages, although tests have indicated that the actual voltages realized are always less than the theoretical maximum.

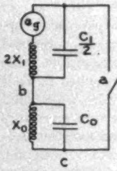

Figure 1

General Analysis

This phenomenon may occur with circuits which may in the simplest case be reduced to that of figure 1. Here capacitance may exist also across the switch, but the only capacitance essential to the process is C_0, so all other capacitances will be neglected at this point. Generation may be either at the point shown or in the x_0 branch, without influencing the analysis except for the voltage *b-c* (that is, across x_0).

Let $2x_1$ be much less than x_0.

Now suppose that there is an arc at *a* which completes the circuit at this point. At each current zero there will be an attempt at extinction, and voltage will rise across the arc path either to complete the recovery transient and interrupt the circuit, or to cause a new breakdown of the arc path and re-establishment of the arc. If breakdown occurs, three components of current will flow; a resumption of the normal-frequency current, a d-c component, and an oscillation involving C_0, $2x_1$, and x_0. The frequency of this oscillation will usually be considerably greater than normal frequency. If it is very high the d-c component becomes very small and in the limit may be neglected without affecting the nature of the process to be described. The two remaining components initially have the same polarity, but in the second half-cycle of the natural-frequency component the polarities are opposite. Then if the restrike occurs at a sufficiently high point on the recovery transient and if $2x_1$ is considerably smaller than x_0, the instantaneous value of current in the natural-frequency component will exceed that of the normal-frequency component and the net current will tend to pass through zero. This gives an opportunity for a new attempt at interruption, which may result in a repetition of the process.

The manner in which this process may build up voltage is as follows, if generation is as shown in figure 1 and interruption occurs at the peak of a voltage wave.

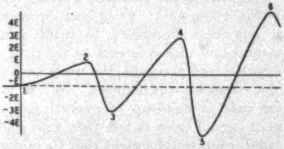

Figure 2. Build-up of voltage at point b of figure 1 due to successive clearings and restrikes within a normal half cycle

Odd-numbered points are clearings, even-numbered points restrikes. After clearing, voltage tends to oscillate about 0, after restrike it tends to oscillate about $-E$

It is evident from inspection that the steady-state voltage *b-c* is zero when the circuit is open at *a*, and substantially equal and opposite to the generated voltage when current is flowing at *a*. Therefore, if E is the generated voltage, which may be taken as equal to normal peak voltage, at the time of interruption the voltage *b-c* tends to change from $-E$ to 0; in doing so it will oscillate to $+E$. The first half cycle of this oscillation is given by the section between 1 and 2 of the curve of figure 2.

At this time the voltage at *a* is $2E$, and the arc may restrike. If it does, the voltage *b-c* tends to change from $+E$, to $-E$, and will therefore oscillate to $-3E$, as indicated by the section from 2 to 3 of the curve of figure 2.

At about the time when $-3E$ is reached, the net current may pass through zero and a second interruption may take place. The voltage *b-c* then starts to change from $-3E$ to zero, and may therefore oscillate to $+3E$, as from 3 to 4 of figure 2.

Paper number 39-50, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 26, 1938; made available for preprinting December 20, 1938.

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[1] For all numbered references, see list at end of paper.

The arc may then restrike again, and thus the process may repeat with a possible voltage increase of considerable magnitude at each repetition.

The voltage at a will, of course, go through changes somewhat similar to those of the voltage b-c.

This analysis has idealized the situation somewhat both as regards system performance by neglecting such items as the change in generated voltage, some of the effects of the presence of x_1, and decrement of the oscillations, and as regards performance of the arc by assuming restrike always to occur at the peak of the recovery transient.

A more exact treatment is given in appendix I, and from this are plotted the curves of figure 3, which apply to the voltage across the switch at a rather than to the voltage b-c. Even here, however, it has been necessary to make some assumption with reference to the time of restrike, and naturally the worst reasonable condition was assumed. As a result, the rate of build-up of overvoltages in most actual cases is less than that indicated, even to the extent that there may not be progressive build-up. Instead, successive voltages may be of a more or less random nature. Figure 4 shows an oscillogram illustrating this phenomenon.

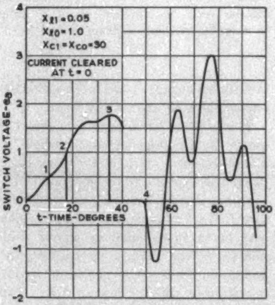


Figure 3. Switch voltage on interruption of circuits of figures 1 or 8

1—Restrike at e_{arc} = 0.5

2—Restrike at e_{arc} = 1.0

3—Restrike at e_{arc} = 1.77

4—Second clearing

The circuit of figure 1, both with the capacitance $C_1/2$ connected as shown and with it connected directly across the breaker, may appear in the single-line diagram of various circuits quite common in operation. Any fault supplied from a rather extensive system through a small and more or less concentrated single reactance may act in this manner. This reactance may be a transformer or reactor, or even a short length of transmission line or cable. A few cases are on record in which overvoltages have been experienced which are believed to be attributable to this phenomenon but they do not appear to be nearly so widespread as the commonness of the circuit might lead one to expect.

Figure 4. Interruption of circuit of figure 1

Curve A—Voltage

Curve B—Current

Curve C—Breaker travel

Curve D—Trip-coil current

Certain other cases have individual features which are discussed hereinafter under the following headings:

1. Ground faults on systems grounded through neutral reactance.
2. Interruption of line charging current.
3. Arcing ground faults on systems with neutral isolated or grounded through reactance.

1. GROUND FAULTS ON SYSTEMS GROUNDED THROUGH NEUTRAL REACTANCE

It is obvious from inspection that the interruption of a single-line-to-ground fault on a system grounded through a neutral reactor fits the circuit of figure 1. Here x_0 represents the zero-phase-sequence reactance, which will lie for the most part in the neutral reactor; C_0 represents the zero-phase-sequence capacitance, or one-third of the capacitance of the entire system to ground. x_1 and C_1 represent positive-phase-sequence reactance and capacitance, respectively. Generation is as shown. In case of fault interruption there is a tendency for the restrike voltage to increase at successive current zeros because of the continually increasing separation of the contacts of the interrupting device. When this restrike voltage becomes sufficiently great, the resulting current oscillation may become so large as to pass through zero in its first cycle. The process of successive build-up of voltage may then take place as explained in the general analysis and illustrated in figure 2 for the neutral voltage and figure 3 for the voltage at the interrupting device.

The methods of appendix I have been used to investigate the conditions under which this type of build-up may occur and it has been found that they may be expressed in terms of the reactance x_0 of figure 1. A lower limit is fixed by the fact that extra current zeros do not occur when x_0 lies below the values indicated by the curves of figure 5, although it should be pointed out that even if x_0 is below these limiting values a voltage approaching three times normal peak may appear at the neutral, as indicated at point 3 of figure 2.

Two points are of interest on these curves. When C_1/C_0 is equal to or greater than unity, corresponding approximately to the usual cable or overhead transmission system, a conservative value of x_0 is ten times x_1; but when C_1/C_0 is equal to zero, which may be approached in the case of a number of generators which are substantially independent electrically except for a common neutral bus, x_0 should not be greater than four or five times x_1.

Once this lower limit is well passed, no great change takes place as a result of increase of x_0 until it approaches equality with the capacitive reactance C_0, that is, until the condition of the ground-fault neutralizer or Petersen coil is approached. The ground-fault current in a system grounded through a ground-fault neutralizer is largely in phase with the voltage, so that the recovery voltage is very small and consequently restriking is unlikely. Even with poor tuning, a cycle or more is required for voltage to recover to normal, so that there is little chance of a restrike at a voltage high enough to cause serious build-up by successive clearings and restrikes.

Beyond the ground-fault-neutralizer value, the natural frequency becomes lower than the operating frequency and the phenomenon becomes substantially that of the interruption of transmission-line charging current.

If it is desired to use a neutral impedance within the danger zone, successive restriking may still be prevented by the use of a parallel neutral resistor of proper value or by the use of a neutral resistor instead of a neutral reactor. The parallel neutral resistance must be low enough to prevent, by its damping action, the restrike current oscillation from passing through zero. Of course, in addition to this main accomplishment, it also reduces the initial recovery voltage, particularly for single-line-to-ground faults.

Calculations have shown that the required parallel resistance is always somewhat greater than that required for critical damping of the ground or zero-sequence circuit, the difference depending on the circuit natural frequencies and on the ratio of zero-sequence to positive-sequence reactance. It is therefore suggested that as a practical and conservative means of selecting the proper resistance, this critical damping value R_c be used. The value of this resistance is given approximately by

$$R_c = \frac{1}{2}\sqrt{x_{ln}x_{cn}}$$

where

R_c = shunt resistance required for critical damping

x_{ln} = inductive reactance of the ground circuit
= one-third of the zero-sequence inductive reactance for a three-phase system

x_{cn} = capacitive reactance of the ground circuit
= one-third of the zero-sequence capacitive reactance for a three-phase system

It may be seen from this equation that the power rating of the required resistor for a given neutral reactor is approximately proportional to the square root of the system capacity to ground.

The discussion above applies directly to single-line-to-ground faults on reactance-grounded systems. However, even in case of two- or three-phase faults to ground there is always a last phase to clear, and this may behave like a single-line-to-ground fault. Thus, the discussion applies qualitatively to all faults involving ground.

It is also evident that the neutral reactance under discussion may be that of a grounding transformer, rather than a reactor, without essentially changing the circuit conditions although in this case there may not be any conductor at neutral potential, so that the neutral may be eliminated as a possible breakdown point.

2. Interruption of Line Charging Current

Upon interruption of the charging current of a transmission line, cable, or capacitor at a current zero the rate of rise of recovery voltage is very slow. If, because of this fact, the circuit breaker interrupts at small contact separation and in addition has a relatively slow build-up of dielectric strength, the arc may restrike. When such restriking takes place, a current oscillation of large magnitude compared to the steady-state charging current occurs. This current may again be interrupted on its first zero, with a resulting recovery voltage which may be higher than normal. Continued restriking at successively higher voltages may take place in this case, just as in the case of the previous section, as the contact separation increases, until the final interruption is attained.

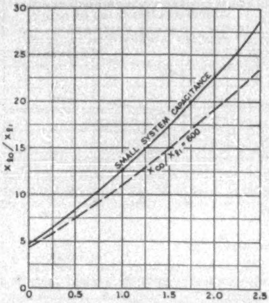


Figure 5. Critical ratio of zero-sequence reactance x_{l0} to positive-sequence reactance x_{l1}, as a function of ratio of positive-sequence capacitance C_1 to zero-sequence capacitance C_0—successive restriking may occur if x_{l0}/x_{l1} exceeds the critical value

Such operation has been discussed in detail in references 8 and 9. An additional illustration is given in appendix II and figure 6, in order to show that the build-up of voltage may occur even with interruption of lumped capacitance current.

To prevent successive restriking it is desirable to build up the breaker dielectric strength as rapidly as possible after the first interruption and thus successfully prevent the first restrike.

Another method of attack is to introduce damping into the circuit by resistance in or across the breaker or line.

3. Arcing Ground Faults on Systems With Neutral Isolated or Grounded Through Reactance

A single-line-to-ground fault on an isolated-neutral system causes only a very small capacitance current to flow. Usually this current flows through an arc, which is interrupted at every current zero and requires a certain restrike voltage for reignition. The results of such action have been described for a particular case in references 1 and 7. A more or less constant restrike voltage may be expected, which limits the voltage on the faulted phase. The transient applied to the system at every such restrike and every subsequent clearing may lead to voltages in the unfaulted phases which in the steady state exceed the maximum transient voltages associated with simple application or interruption of a solid fault.

Curve *A* of figure 7 shows the maximum sustained voltages at the point of fault on the unfaulted phases of a three-phase system having a steady-state arcing ground on one phase. These voltages were obtained by tests on a miniature system.

For positive value of x_0/x_1 the curve of maximum obtainable voltage is affected considerably by the amount of system capacitance, the curve shown being the envelope of the many possible curves obtainable with particular values of capacitance.

It was found in test that if the restrike voltage was continually changing so that the phenomena were not periodic, the voltages on the unfaulted phase could be considerably increased. Such changes might be caused, in an arc to ground on a

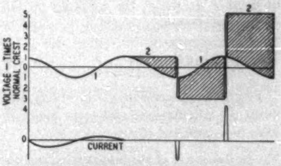


Figure 6. Interruption of capacitance current-circuit of figure 9

Curve 1—Voltage at inductance side of switch

Curve 2—Voltage at capacitance side of switch

Height of cross-hatched area gives voltage across switch power system, by wind or by changes in insulation characteristics.

For ready comparison, figure 7 shows also the steady-state fundamental voltage on the unfaulted phases during a solid line-to-ground fault (curve *E*) and the maximum transient voltage caused by the application (curve *D*) or removal (curves *C* and *B*) of such a fault. Resistance and arc drop are neglected in all curves except that for a sustained arcing ground (curve *A*). In general, fault resistance decreases the voltages on the unfaulted phases.

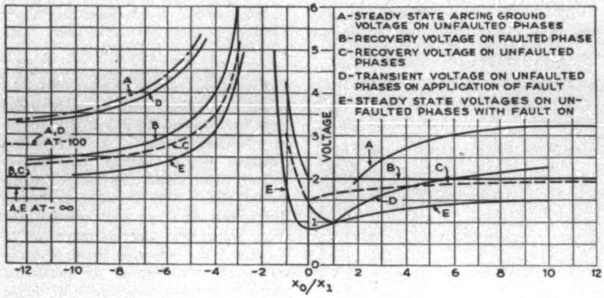


Figure 7. Single-line-to-ground fault on three-phase system—maximum voltages to ground at fault point in per unit of crest leg voltage before fault

jx_0 = zero-sequence impedance

jx_1 = positive-sequence impedance

Conclusions

From the discussion given above and from the calculations and tests made, it may be concluded that:

1. In the course of the interrupting process on certain types of circuit, the current immediately following restriking of the arc after an early attempt to clear may involve high-frequency components of sufficient magnitude to cause the current to pass through zero very shortly after the restrike. Clearing at this time gives rise to voltages considerably in excess of those normally associated with the recovery transient.

2. Systems grounded through neutral reactors may be subject to these overvoltages.

3. If the possibility of these overvoltages is to be avoided, neutral-grounding reactors must satisfy one of the following conditions:

(*A*). Their reactance must be so low as to keep the zero-sequence reactance below

(*a*). Ten times positive-sequence reactance if the zero-sequence capacitance is no larger than the positive-sequence capacitance, or

(*b*). Five times positive-sequence reactance if the zero-sequence capacitance is much larger than the positive-sequence capacitance.

(*B*). Their reactance must tune the line capacitance to ground (ground fault neutralizer).

(*C*). They must be shunted by resistance to provide substantially critical damping.

4. The interruption of line charging current may give rise to high voltages unless restriking is prevented either by quick clearing by means of rapid build-up of dielectric strength in the circuit breaker, or by circuit damping.

Appendix I

In this appendix there is presented an approximate analysis of the behavior of a three-phase power system with neutral reactance grounding, during the clearing of a single-line-to-ground fault and with restriking of the arc after the first clearing. The system may be approximately represented by the circuit of figure 8, which in turn may be represented for the purposes of analyzing a single-line-to-ground fault by the circuit of figure 1.

It is assumed that:

1. Arc drop is zero.
2. All resistances are zero.
3. Arc interruption occurs only at current zeros determined by the circuit constants.
4. The arc restrikes at various values of switch voltage.
5. Positive- and negative-sequence impedances are equal.

Under these conditions the fault current before interruption is given by the equation

$$i_{a1} = \frac{\sin t}{Z_{0s} + 2Z_{1s}} = I_a \sin t \qquad (1)$$

where

x_{c1} = positive-sequence capacitive reactance

x_{l1} = positive-sequence inductive reactance

Z_{0s} = steady-state zero-sequence impedance

$$Z_{0s} = \frac{x_{c0}x_{l0}}{x_{c0} - x_{l0}}$$

The normal voltage at the fault point is 1.0

Z_{1s} = steady-state positive-sequence impedance

$$Z_{1s} = \frac{x_{c1}x_{l1}}{x_{c1} - x_{l1}}$$

t = time measured from the instant the current is interrupted

x_{c0} = zero-sequence capacitive reactance

x_{l0} = zero-sequence inductive reactance

After the current is interrupted at $t = 0$, the recovery voltage on the faulted phase is

$$e_{a1} = I_aZ_{0s}(\cos t - \cos \omega_0 t) + 2I_aZ_{1s}(\cos t - \cos \omega_1 t) \qquad (2)$$

or

$$e_{a1} = \cos t - I_aZ_{0s}\cos \omega_0 t - 2I_aZ_{1s}\cos \omega_1 t$$

where

I_a is defined by equation 1

ω_0 = zero-sequence natural frequency

$\omega_0 = \sqrt{x_{c0}/x_{l0}}$

ω_1 = positive-sequence natural frequency

$\omega_1 = \sqrt{x_{c1}/x_{l1}}$

The zero-sequence voltage is

$$e_{01} = I_aZ_{0s}\cos \omega_0 t \qquad (3)$$

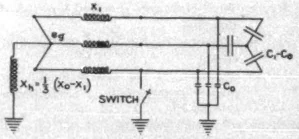


Figure 8. Typical three-phase system with single-line-to-ground fault

If restrike occurs at time $t = t_1$, the restrike current is

$$i_{a2} = -\frac{\sin t_1}{x_{l0} + 2x_{l1}} + \frac{I_a}{x_{l0} + 2x_{l1}} \times \left(\frac{Z_{0s}}{\omega_0}\sin \omega_0 t_1 + \frac{2Z_{1s}}{\omega_1}\sin \omega_1 t_1\right) + I_a \sin(t + t_1) + \frac{(\omega'^2 - \omega_0^2)(\omega_1^2 - \omega'^2)}{(x_{c0} + 2x_{c1})(\omega'^2 - 1)\omega'^2} \times [\sin t_1 \cos \omega' t + \omega' \cos t_1 \sin \omega' t] + \frac{I_a}{(x_{c0} + 2x_{c1})\omega'^2}[2Z_{1s}(\omega'^2 - \omega_0^2)(\omega_1 \sin \omega_1 t_1 \times \cos \omega' t + \omega' \cos \omega_1 t_1 \sin \omega' t) - Z_{0s}(\omega_1^2 - \omega'^2)(\omega_0 \sin \omega_0 t_1 \cos \omega' t + \omega' \cos \omega_0 t_1 \sin \omega' t)] \qquad (4)$$

or

$$i_{a2} = I_0 + I_a \sin(t + t_1) + I_n \cos(\omega' t - \theta_n)$$

where

t is now measured from the point at which restrike occurred

t (equation 4) $= t$ (equation 2) $- t_1$

ω' = natural frequency with fault on

$$\omega' = \sqrt{\frac{(x_{l0} + 2x_{l1})x_{c0}x_{c1}}{(x_{c0} + 2x_{c1})x_{l0}x_{l1}}}$$

I_0, I_n, and θ_n are defined by equation 4

While this restrike current is flowing, the zero-sequence voltage is

$$e_{02} = Z_{0s}I_a \cos (t + t_1) - \frac{x_{c0}(\omega_1^2 - \omega'^2)}{(x_{c0} + 2x_{c1})(\omega'^2 - 1)\omega'} [- \sin t_1 \sin \omega' t + \omega' \cos t_1 \cos \omega' t] + \frac{x_{c0}I_a}{(x_{c0} + 2x_{c1})\omega'} \times [- 2Z_{1s}(- \omega_1 \sin \omega_1 t_1 \sin \omega' t + \omega' \cos \omega_1 t_1 \cos \omega' t) + \frac{Z_{0s}(\omega_1^2 - \omega'^2)}{(\omega'^2 - \omega_0^2)} \times (- \omega_0 \sin \omega_0 t_1 \sin \omega' t + \omega' \cos \omega_0 t_1 \cos \omega' t)] \quad (5)$$

or

$$e_{02} = Z_{0s}I_a \cos (t + t_1) + \frac{x_{c0}\omega' I_n}{\omega'^2 - \omega_0^2} \sin (\omega' t - \theta_n)$$

Note that $\left(\frac{jx_{c0}\omega'}{\omega_0^2 - \omega'^2}\right)$ is simply the impedance of the zero sequence circuit at a frequency ω'.

If the current given by equation 4 passes through zero at $t = t_2$, where t is measured from time t_1, the circuit may then be recleared. After this second clearing the recovery voltage on the faulted phase is

$$e_{a2} = \cos (t + t_1 + t_2) + \omega_0 I_0 x_{l0} \sin \omega_0 t + \omega_1 2I_0 x_{l1} \sin \omega_1 t - I_a Z_{0s}[\cos (t_1 + t_2) \times \cos \omega_0 t + \omega_0 \sin (t_1 + t_2) \sin \omega_0 t] - 2I_a Z_{1s}[\cos (t_1 + t_2) \cos \omega_1 t - \omega_1 \sin (t_1 + t_2) \sin \omega_1 t] - \frac{I_n x_{c0}}{\omega'^2 - \omega_0^2} \times [\omega_0 \cos (\omega' t_2 - \theta_n) \sin \omega_0 t + \omega' \sin (\omega' t_2 - \theta_n) \cos \omega_0 t] + \frac{2I_n x_{c1}}{\omega_1^2 - \omega'^2} [\omega_1 \cos (\omega' t_2 - \theta_n) \sin \omega_1 t + \omega' \sin (\omega' t_2 - \theta_n) \cos \omega_1 t] \quad (6)$$

where

t (equation 6) $= t$ (equation 4) $- t_2$
$= t$ (equation 2) $- t_1 - t_2$

$$\frac{x_{c0}}{\omega'^2 - \omega_0} = \frac{2x_{c1}}{\omega_1^2 - \omega'^2} = \frac{x_{c0} + 2x_{c1}}{\omega_1^2 - \omega_0^2}$$

The zero-sequence voltage is the negative of the sum of all the terms of equation 6 having a frequency ω_0.

As an example of the application of these equations, a circuit with $x_{l0}/x_{l1} = 20$, $x_{c0}/x_{l0} = 30$, and $x_{c1} = x_{c0}$ has been considered. Figure 3 shows the recovery voltage on the faulted phase. If restrike occurs at half or normal voltage (points 1 and 2), the resulting oscillations of the restrike current do not pass through zero and the current continues for another half cycle; if restrike occurs at maximum recovery voltage (point 3), the restrike current oscillates to zero at point 4 and is there interrupted. The recovery voltage then rises to about three times normal, if no further restriking occurs. At the same time the zero-sequence voltage (which is nearly equal to the neutral voltage to ground) rises to about two times leg voltage.

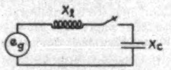
By means of these equations and tests on miniature circuits with $x_{c1} = x_{c0}$, it has been determined that if $x_{l0}/x_{l1} < 10$, the restrike current cannot oscillate to zero regardless of the point of restrike. Therefore a second (or more) clearing is rendered impossible.

If $x_{c1} > x_{c0}$ the critical value of x_{l0}/x_{l1} is decreased, while if $x_{c1} < x_{c0}$, the critical x_{l0}/x_{l1} is increased, as shown by figure 5.

Appendix II

As an extremely simplified representation of the interruption of charging current one may consider the circuit of figure 9. Here the line or other capacitance is represented by a lumped capacitance x_c supplied through the source inductance x_l. If this circuit is interrupted at a current zero the recovery voltage in per unit of normal voltage at the capacitor is

Figure 9



$$e_{a_1} = \frac{x_c - x_l}{x_c} \cos t - 1 \quad (7)$$

If restriking takes place when the recovery voltage is maximum at $t = \pi$, the restrike current is

$$i_{a2} = \frac{\sin t - 2\omega \sin \omega t}{x_c} \quad (8)$$

where t is now measured from the instant of restrike, and

$$\omega = \sqrt{x_c/x_l}$$

It is evident that in general the natural-frequency component of i_{a2} is much greater than the fundamental component. Thus a large current oscillation of high frequency takes place and may be interrupted at its first current zero. If this occurs, the restrike current and subsequent recovery voltage, neglecting minor oscillations, will appear as in figure 6. By continuing the process begun by equations 7 and 8 we arrive at the successively higher voltages of figure 6.

It is not to be inferred that voltages of exactly this character exist. Instead, there is here also a randomness in the restrike voltage at successive intervals which largely controls the individual voltage peaks. In addition, the capacitance may be distributed along a transmission line as in references 8 and 9, and the voltage may also be affected by coupling between phases of a polyphase system. However, the tendency and the possibility of a building up of high voltages is clearly shown, so that it seems highly desirable to prevent such restriking.

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Discussion

R. A. Hentz (Philadelphia Electric Company, Philadelphia, Pa.): From the time impedances in the neutrals of generators were deemed desirable, resistances were used almost exclusively, if not entirely so, and to the best of my knowledge with complete satisfaction as far as electrical characteristics are concerned. Later grounding reactors instead of resistors were used in a number of cases, as they offered the advantages of smaller space, less cost, and of being less subject to deterioration. With the advent of transmission substations where the busses were supplied from delta-connected transformer banks, neutral grounding was obtained in many cases by means of zigzag grounding transformers.

The analysis made by the authors brings out (conclusion number 2) that systems grounded through neutral reactors may be subject to overvoltages considerably in excess of those normally associated with the recovery transient. This analysis is of considerable importance to those systems which use neutral reactors or where grounding transformers are employed which resulted in a somewhat similar electrical condition.

The importance of the subject is further emphasized by the fact that not only can difficulties result from neutral grounding through reactance, but that breakdowns have occurred both to machines and cables on systems for which this form of grounding gives a probable explanation.

Reactance grounding can be eliminated relatively easily where "wye"-connected machines or other equipment are employed by installing neutral resistors in place of the reactances. This in my opinion is to be preferred to paralleling the existing reactors with a resistor. The situation is not so simple where the grounding transformers are employed. Such situations, therefore, present a challenge to designing engineers for the development of some equipment which will either replace or supplement these grounding transformers to the end that these dangerous overvoltages may be reduced to safe limits.

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper represents a valuable addition to the literature on voltage recovery transients. It will be noted that it contains actual test data substantiating at least the order of magnitude of the overvoltage due to an arcing ground predicted by Messrs. R. D. Evans, A. C. Monteith, and R. L. Witzke (AIEE TRANSACTIONS, volume 58, 1939, pages 386–97). The phenomena, however, consisted of more restrikes under less severe conditions than those postulated by Messrs. Evans, Monteith, and Witzke, so it is not certain that the apparent confirmation is more than an accident.

J. A. Adams (Philadelphia Electric Company, Philadelphia, Pa.): It is of interest to compare the limits of zero-sequence reactance given in conclusion 3 with values which have existed during faults on actual systems where the phenomena discussed have probably been the cause of secondary failures.

In two frequency-converter substations on the Philadelphia Electric Company system the neutrals of the 25-cycle generators were grounded through four-ohm reactors. While operating one of these substations with two generators connected to the 13.2-kv bus, one generator with the neutral grounded through the reactor and the other with the neutral ungrounded, a single-phase-to-ground fault developed on one of the cables fed from the bus. After the fault was cleared it was found that the neutral lead on the generator with its neutral ungrounded had broken down to ground. In this case the ratio of x_0/x_1 was 25.7, which is well above either of the lower limits given. For another fault on this system with only one generator operating the ratio of x_0/x_1 was 12.9 and a second breakdown did not occur. The ratio of C_1/C_0 for these two cases is estimated to be approximately unity.

At the other substation practically simultaneous faults developed to ground on one phase of the leads from one generator operating with its neutral grounded through the reactor and in the winding in the same phase of the second generator operating with its neutral ungrounded. In this case the ratio x_0/x_1 was 14.6 and the ratio of C_1/C_0 was approximately unity. At another time, however, a cable failure to ground with the same system set up did not cause a second breakdown, probably indicating that this ratio of x_0/x_1 is near the critical value.

As a result of an analysis of these cases of trouble, the neutral-grounding reactors are being replaced with four-ohm resistors.

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Considering the paper by Messrs. Concordia and Skeats, we feel that it is essential to consider the arc voltage and the increase in voltage due to extinction of the arc prior to a normal current zero as this effect will give rise to higher voltages than indicated in their paper. This is of particular interest when considering grounding means for generators as the extinction voltage may be comparable to the normal line-to-neutral voltage and proportionately increase the transient voltages.

(See also discussion, page 412.)

C. Concordia and **W. F. Skeats:** The authors are particularly grateful to those discussers who have contributed their experience with overvoltages apparently coming within the scope of this paper. Circuits falling within the general classification susceptible to this type of overvoltage appear to be more widespread than is indicated by any trouble experience, and a study of trouble experience seems to afford the most practical means of setting down more precisely the conditions under which overvoltages may be expected. In this connection it should be noted that fundamentally the phenomenon is not limited to systems using neutral-grounding reactors but may occur on any system where, looking back from the point of fault, one sees first a small reactance, then an appreciable capacitance, and finally a comparatively large reactance.

High arc voltage preceding current zero tends to increase the voltage reached on the normal recovery transient, which indirectly increases the likelihood, upon restrike, of a high-frequency current oscillation of sufficient amplitude to cause the total current to pass through zero early in the cycle and so start the process of building up voltage. High arc voltage after a restrike also helps directly to bring about such an early current zero. However, once the build-up process has been started, the effect of arc voltage may be to retard the rate of build-up and limit the final voltage obtained rather than the reverse. Thus the authors cannot agree with the contention of Messrs. Evans, Monteith, and Witzke that arc voltage of reasonable value will greatly increase the voltages reached by the build-up process discussed in the paper, although of course, extremely high arc voltages may result in dangerously high recovery voltages even without this build-up process. In presenting their paper the authors felt that the overvoltages arising from successive restriking with certain critical circuit constants should be separated from those arising from high arc voltage.

The elimination of trouble in the case of zigzag grounding transformers may be accomplished either by using a grounding transformer whose reactance satisfies the conditions of conclusion 3*A* or 3*B*, or by using sufficient series resistance between the transformer neutral and ground, although this last remedy might in some cases reduce the ground currents too much for proper relaying.

Overvoltages During Power-System Faults

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OVERVOLTAGES may be produced by lightning, switching surges, faults, both solid and arcing, and the overspeeding of machines due to loss of load. The effect of circuit and machine characteristics on the duration and magnitude of these overvoltages has received considerable attention.[1–10] Furthermore, field tests have been made in order to determine the effectiveness of different methods of grounding, and the accuracy of the methods used to calculate the magnitude of overvoltage during system faults.[11–13]

It seems to be the proper time to consider and review the overvoltages produced by the occurrence of system faults, in order that full advantage of present-day knowledge may be taken by those concerned with system design and operation.

There are essentially three components of voltage due to the occurrence of a system fault:

1. Fundamental-frequency voltages.
2. Natural-frequency voltages usually of short duration which are superimposed upon the fundamental-frequency voltages.
3. Harmonic voltages resulting from unbalanced currents flowing in rotating machines in which the reactances in the direct and quadrature axes are unequal.

In general, protective devices, particularly lightning arresters, must be capable of withstanding the transient overvoltages to which they may be subjected for periods of short duration, and then must be able to seal off the power follow current associated with the sustained voltages. Unnecessary lightning-arrester failures due to excessive sustained overvoltages during fault may be prevented by properly grounding and operating the system.

It is the fundamental-frequency overvoltages which largely determine the lightning-arrester rating and the corresponding insulation protective levels. These in turn are a factor in determining the allowable apparatus insulation levels. From a technical standpoint, the fundamental-frequency voltages can be determined with fair accuracy.

Although this phase of the subject is of considerable importance, it has been discussed in the literature only in somewhat scattered and incomplete form. It is the purpose of this paper to review and analyze the factors which determine the maximum voltages which may be obtained on a system following the occurrence of faults, so that a more rational selection of system protective equipment can be made and the best method of system grounding determined.

Results of calculations, and of tests on a miniature system are presented. Included in these results are the transient voltages as well as the fundamental-frequency voltages that occur on a system during a fault. The information on transient overvoltages is of value in indicating the maximum magnitude of voltage that may be obtained on a system during a solid fault, thus showing the magnitude of transient voltage which is inherent in the circuit and which cannot be reduced by the elimination of excess voltages due to switching or arcing, or by building lightning-proof lines. Furthermore, as it seems reasonable to assume that arcing at the fault may subject the circuit during the arcing period to voltages of the same order of magnitude as those obtained immediately subsequent to the initial application of the fault, the curves presented can be used as a guide to indicate the maximum voltage which may be expected due to arcing across an insulator.

The paper analyzes the effect of those factors which determine the magnitude of the overvoltages during faults, which are the most common causes of high sustained voltages. A study of these voltages is usually a necessary first step, after which refinements and the more unusual cases can be considered. The effect of different methods of grounding and the influence of fault and arc resistance are included.

Conclusions

From the results discussed in this paper, the following conclusions can be drawn:

1. Line-to-ground faults for most systems can be used as a basis for determining the maximum fundamental-frequency and transient voltages during faults.

2. If a system is solidly grounded or grounded through reactance, so that the resultant zero-sequence impedance viewed from the fault is inductive rather than capacitive, the fundamental-frequency voltages-to-ground on the unfaulted phases at the fault will not be greater than 1.73 times normal line-to-neutral voltage.

3. If a system is grounded through resistance, so that the resultant zero-sequence impedance viewed from the fault is inductive rather than capacitive, the fundamental-frequency voltages-to-ground of the unfaulted phases at the fault will always be less than twice normal line-to-neutral voltage.

4. In order that the overvoltages during faults shall not exceed that which is considered safe for the operation of grounded-neutral lightning arresters, it is advisable that careful attention be paid to the method of grounding and the number of grounding points in the system. Consideration should also be given to the possibility of temporary disconnection of these grounding points and the resulting overvoltages which may occur.

5. Isolated-neutral systems may be subjected to particularly high overvoltages if the system is large in extent. The overvoltages at or near the region of resonance are appreciably reduced by fault and line resistance. This may be an important factor, particularly in low-voltage systems. On the other hand, resistance in the fault or ground return for a grounded-neutral system may slightly increase the overvoltages obtained on one of the open phases.

6. The transient overvoltages obtainable on a system which ordinarily might be considered to be solidly grounded may approach 2.73 times normal. Sustained voltages are generally higher for systems grounded through resistance than for systems grounded through corresponding values of reactance. The natural-frequency transient voltages obtained on a system grounded through resistance are practically negligible except when the resistance is high relative to the positive-sequence reactance.

7. The transient overvoltages during faults are not expected to be of sufficient magnitude to cause breakdown of the major insulation provided it is in good condition, except for the case of an isolated-neutral system having an appreciable amount of line or cable capacity to ground. The overvoltages during faults in a grounded-neutral system are not as great in magnitude as those which may be expected from lightning or switching surges.

8. Ground-fault-neutralizer (Petersen coil) systems are subjected to transient and fundamental-frequency overvoltages which are, in general, higher than those of a solidly grounded system but lower than the volt-

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Paper number 39-52, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 12, 1938; made available for preprinting December 28, 1938.

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1. For all numbered references, see list at end of paper.

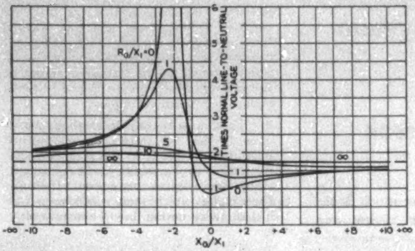


Figure 1. Fundamental-frequency voltages. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

$$Z_1 = Z_2 = 0 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$

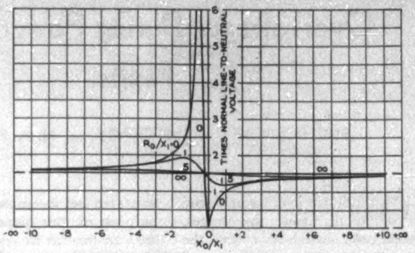


Figure 2. Fundamental-frequency voltages Double-line-to-ground fault. Voltage-to-ground of unfaulted phase at the fault

$$Z_1 = Z_2 = 0 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$

ages which can occur on an isolated-neutral system.

Basis of Study

The calculations, and tests on a miniature system, made to determine the magnitude of overvoltage following the occurrence of system faults, were based on the following assumptions and considerations:

1. The fault is a solid fault; that is, there is no arcing. Arcing tends to subject the circuit to a more or less continuous transient condition. It has been shown that very high voltage may be obtained in a circuit with no losses under assumed conditions of arc interruption and restriking.[7,14] Evidence indicates that these mechanisms of arcing do not ordinarily occur at the point of fault,[13,14] although such phenomena may be associated with switch operation.[15] It is believed that the maximum voltage which may be expected due to arcing can be taken to be approximately that corresponding to the transient overvoltage obtained immediately following the occurrence of a solid fault. Under this assumption the transient voltages presented in the paper are an indication of the maximum voltage which may be obtained due to arcing at the fault.[15]

2. Only fundamental and natural-frequency components of voltage are considered. Harmonics produced by the saliency effect of rotating apparatus are neglected. This assumes in effect that $x_q''/x_d'' = 1$. Exceptionally high voltages may occur due to saliency effects, particularly for the case of water-wheel generators not equipped with amortisseur windings and disconnected from their load. These overvoltages tend to be increased if the generator is left connected with an unbalanced fault to an appreciable amount of transmission line or cable so as to form a capacitive load. The conditions for this important exception are usually evident and are discussed fully in two recent papers.[4,6]

It is important to realize, however, that a certain amount of distortion due to this effect may occur for many system faults and that this will tend to increase the sustained voltages above those based only upon a consideration of the fundamental-frequency components.

3. Sudden disconnection of load and overspeeding of the system and connected generators is not considered. Loss of load on a generator may result in overvoltages due both to the sudden disconnection of the load and also to the overspeeding.[4] The results presented in this paper can, of course, be modified in order to indicate the increase in voltage which might be obtained under these conditions. This effect is most pronounced in portions of the system which overspeed due to loss of load on nearby hydro stations.

4. Negative-sequence impedances are assumed equal to positive-sequence impedances. The effect of rotating loads and machines, except in the case of synchronous machines not equipped with amortisseur windings, is to make the negative-sequence impedance less than the positive and to increase the losses with fault duration. These effects, in general, tend to reduce the magnitude of overvoltage calculated with equal positive- and negative-sequence impedances.

5. The magnitude of the positive-sequence impedance is assumed to be constant for the period in which the overvoltage is being determined. As the effective impedance of a rotating machine changes with time, it is necessary to consider the impedance under conditions which may result in the greatest overvoltage. On an isolated system, for example, the greatest overvoltage may be obtained based on impedances corresponding to the synchronous condition rather than the subtransient or transient condition. Because these impedances vary with time after the fault occurs, it is desirable that results be presented in the form of curves so that the range of voltage can be easily determined.

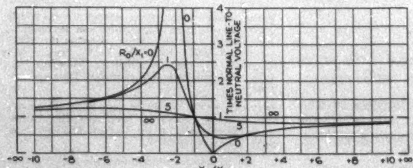
6. The effects of saturation and corona are not included. Both would tend to reduce

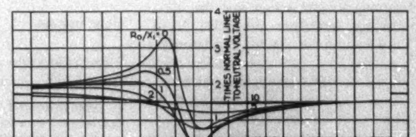
Figure 3. Fundamental-frequency voltages. Line-to-ground fault. Zero-sequence voltage at the fault

$$Z_1 = Z_2 = 0 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$

Figure 4. Fundamental-frequency voltages. Line-to-ground fault on phase a. Phase b voltage-to-ground at the fault

$$Z_1 = Z_2 = R_1 + jX_1$$
$$Z_0 = R_0 + jX_0$$
$$R_f = 0$$
$$R_1/X_1 = 0.4$$





the magnitude of overvoltage. The error in neglecting these factors, in general, increases with increase in the magnitude of overvoltage. In the region of resonance, saturation is an important factor in limiting the excessive fundamental-frequency overvoltages.

7. Overvoltages caused by unusual system conditions are neglected. For example, nonlinear circuit instability may be produced as a result of the opening of fuses or single-pole disconnect switches, thereby causing the magnetizing currents of potential or power transformers to flow through line capacitance with resulting overvoltages.

8. The miniature system used for determining transient overvoltages was set up to represent a low-loss system; that is, the ratio of r_1/x_1 was approximately 0.03. For systems having higher losses, the transient overvoltages will be somewhat less because of greater decrement factors.

9. Tests on the miniature system for determination of maximum transient voltage were made by representing the system by lumped impedance elements. This will, in general, lead to higher transient overvoltages at the point of fault than will be obtained in an actual system. This pessimistic result is due to the fact that the number of circuit natural frequencies is less than that on an actual system, and the resultant transient voltages are therefore likely to be of greater magnitude.

Discussion of Results

FUNDAMENTAL-FREQUENCY VOLTAGES

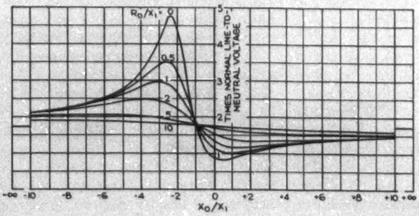
Based on the foregoing assumptions, figure 1 gives the maximum fundamental-frequency voltage to ground which will occur on either of the unfaulted phases at the point of fault following a line-to-ground fault. Positive- and negative-sequence resistances are neglected, but zero-sequence resistance is included. Fault resistance is neglected. Figure 1 has an abscissa of X_0/X_1, where $Z_1 = 0 + jX_1$ and $Z_0 = R_0 + jX_0$ are the positive- and zero-sequence impedances, respectively, viewed from the fault. X_0 may be positive or negative. See equations 10 and 11, appendix *A*.

Figure 2, similar to figure 1, gives the maximum fundamental-frequency voltage on the unfaulted phase at the point of fault following a double-line-to-ground fault. Fault resistance and positive- and negative-sequence resistances are neglected. See equation 13, appendix *A*.

Figure 3 gives the zero-sequence voltage at the point of fault for a line-to-ground fault under the conditions of figure 1. The zero-sequence voltage at the point of fault for a double-line-to-ground fault under the conditions of figure 2, is one-third the voltage on the unfaulted phase given by figure 2. See equations 9 and 12, appendix *A*.

Figure 5. Fundamental-frequency voltages. Line-to-ground fault on phase a. Phase c voltage-to-ground at the fault

$Z_1 = Z_2 = R_1 + jX_1$
$Z_0 = R_0 + jX_0$
$R_f = 0$
$R_1/X_1 = 0.4$



For a system which has its neutrals solidly grounded or grounded through reactance, the ratio X_0/X_1 will ordinarily be positive. That is, the zero-sequence impedance viewed from the fault is inductive rather than capacitive in effect. With neutrals grounded through resistance, X_0/X_1 may be either positive or negative. For an isolated-neutral system X_0/X_1 is negative. Also in a system which is extensive in number of miles of connected line or cable compared with the total admittance of the grounded points, the ratio of X_0/X_1 may be negative. With X_0/X_1 negative, the voltages obtained correspond to those on the left-hand side of the vertical axis. Under this condition, it is possible to obtain high overvoltages, particularly for small values of R_0/X_1 and values of X_0/X_1 in the region of resonance.

With all resistance neglected, infinite fault currents and voltages will occur if $X_0/X_1 = -2$ for a line-to-ground fault, and -0.5 for a double-line-to-ground fault. See appendix *A*, equations 9–13. Values of X_0/X_1 in the neighborhood of -2 or -0.5 are considered to be in the resonance region. Operation in the region of resonance with a small ratio of R_0/X_1 is expected to be unusual, and undoubtedly will occur only for isolated-neutral systems or grounded systems following the loss of the system ground point.

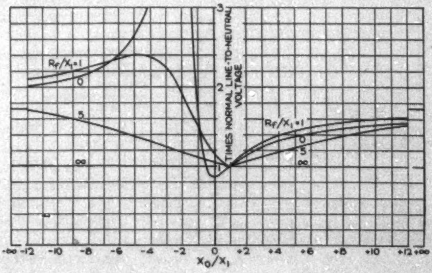
Figures 4 and 5, similar to figure 1, show the overvoltages which may be obtained on phases *b* and *c*, respectively, at the point of fault for the case of a single-conductor-to-ground fault on phase *a*. These two curves apply for $R_1/X_1 = 0.4$. Other curves similar to these have been prepared for early publication by E. M. Hunter with different ratios of positive-sequence resistance to reactance. This particular case is selected as being representative. As will be noted from these curves, the voltages on phase *c* are, in general, higher than those on phase *b*, although this is not true for all ratios of R_0/X_1 and X_0/X_1.

Figure 6, for the line-to-ground fault, shows the effect of fault resistance. Resistance except in the fault is neglected. As R_f/X_1 is increased from zero to $R_f/X_1 = 1$, approximately, the voltage of one of the unfaulted phases is increased and the other decreased, except in the region of resonance where both are decreased. The curve for $R_f/X_1 = 1$ is given in figure 6. As R_f/X_1 is further increased, the voltages decrease. Any increase in fault resistance decreases the overvoltage obtained in the region of resonance.

R_0 includes the effect of resistance in the ground return and in the neutral, while R_f of figure 6 is the resistance in the arc or fault. Figures 1, 2, 4, and 5 can be used to determine the magnitude of the fundamental-frequency fault voltages of systems with isolated neutral, neutral solidly grounded or grounded through resistance, reactance, or impedance. Z_0

Figure 6. Fundamental-frequency voltages. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at fault

$Z_1 = Z_2 = 0 + jX_1$
$Z_0 = 0 + jX_0$



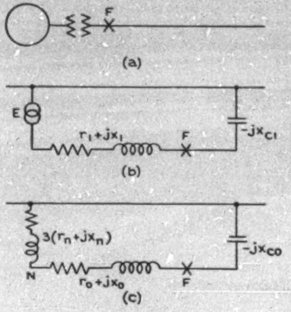


Figure 7

(*a*)—One-line diagram of power system consisting of synchronous machine, transformer bank, and transmission line

(*b*)—Approximate positive-sequence fundamental-frequency impedance diagram

(*c*)—Approximate zero-sequence fundamental-frequency impedance diagram

$= R_0 + jX_0$ is the zero-sequence impedance viewed from the fault and therefore includes the effect of neutral impedance.

Comparing corresponding curves in figures 1 and 2, it is seen that for the same system and fault location, that is, same values of X_0/X_1 and R_0/X_1, higher voltages are obtained for the line-to-ground than for the double-line-to-ground fault, except for the $R_0/X_1 = 0$ curve when X_0/X_1 lies between 1 and 5 and between -0.2 and -1.2, approximately. With X_0/X_1 between 1 and 5, and $R_0/X_1 = 0$, the voltages for the line-to-ground fault are but slightly less than those for the double-line-to-ground fault. As R_0/X_1 is increased, voltages increase more rapidly for the line-to-ground fault than for the double-line-to-ground fault. With $R_0/X_1 = 1$, higher values for the line-to-ground fault than for the double-line-to-ground fault are obtained. From equation 13, appendix *A*, any increase in R_1 reduces the voltage on the unfaulted phase for the double-line-to-ground fault; also, any increase in R_f reduces the voltage except when X_0/X_1 lies between -0.2 and 1.0.

Since resistance is always present, it can be concluded that with $Z_1 = Z_2$, the maximum fundamental-frequency voltage at the fault for the line-to-ground fault is as great as, or greater than, that for the double-line-to-ground fault, except for values of X_0/X_1 between -0.2 and -1.2, and very little resistance in the system. This represents a condition that is not expected to be met in practice except under the most unusual conditions.

TRANSIENT VOLTAGES

Transient voltages here include both the fundamental-frequency component of voltage and the natural-frequency components. Curves for transient voltages following faults in terms of system impedances viewed from the fault cannot be drawn for the general case, as has been done for fundamental-frequency voltages. In the actual system, the transient voltages are affected by the number, connection, and arrangement of the circuits. To simplify the work, and give an indication of the maximum transient voltage to be expected, a system consisting of a synchronous generator, transformer bank, and transmission line open at the distant end was considered. With the fault on the line near the transformer terminals, as a first approximation, the open transmission line was replaced by its lumped capacitance at the point of fault. Figure 7, part *a*, gives a one-line diagram of the system studied; parts *b* and *c*, respectively, show the positive- and zero-sequence impedance diagrams for part *a*. Lower-case letters are used to differentiate the indicated impedances in figures 7 from the resultant effective impedances viewed from the fault which are represented by capitals.

In figure 7 the positive- and zero-sequence fundamental-frequency impedances viewed from the fault are

$$Z_1 = \frac{(r_1 + jx_1)x_{c1}}{x_{c1} - x_1 + jr_1} = R_1 + jX_1$$

$$Z_0 = \frac{[r_0 + 3r_n + j(x_0 + 3x_n)]x_{c0}}{x_{c0} - (x_0 + 3x_n) + j(r_0 + 3r_n)} = R_0 + jX_0$$

For a ground-fault neutralizer, $x_{c0} \simeq x_0 + 3x_n$, and

$$R_0 \simeq \frac{(x_0 + 3x_n)x_{c0}}{r_0 + 3r_n}$$

The coil is generally tuned so that X_0 has a very small positive value.

Resistance Neglected. With all resistance neglected in figure 7, transient voltages are expressed in appendix *A* in terms of the fundamental-frequency impedances viewed from the fault and the ratio of the positive to the zero-sequence capacitive reactance.

With resistance neglected, the fundamental-frequency impedances viewed from the fault in figure 7 are:

$$Z_1 = j\frac{x_1x_{c1}}{x_{c1} - x_1} = 0 + jX_1$$

$$Z_0 = j\frac{(x_0 + 3x_n)x_{c0}}{x_{c0} - (x_0 + 3x_n)} = 0 + jX_0$$

With $(x_0 + 3x_n)$ greater than x_{c0}, X_0 is negative.

Figures 8 and 9 for line-to-ground and double-line-to-ground faults, respectively, give the maximum transient voltages in

Figure 8. Transient voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

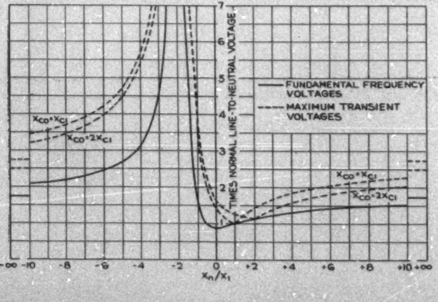
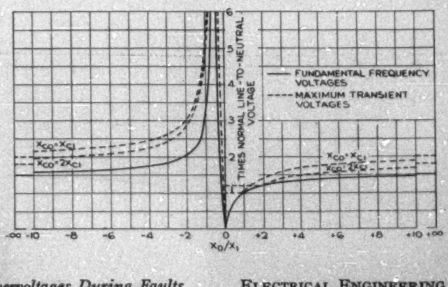


Figure 9. Transient voltages with resistance neglected for system shown in figure 7. Double-line-to-ground fault. Maximum voltage-to-ground of unfaulted phase at the fault



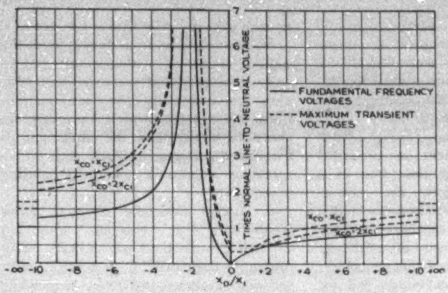


Figure 10. Transient voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum zero-sequence voltage at the fault

per unit of normal peak line-to-neutral voltage on the unfaulted phases at the fault in terms of X_0/X_1 for the system shown in figure 7. The curves in figures 8 and 9 were calculated from equations 19 or 20 and 23, respectively, of appendix *A* with $\theta_0 = 90$ degrees. Since resistance is neglected there will be no decrement, and therefore the peak value of the fundamental-frequency term indicated in figures 8 and 9 by full-line curves, and the peak value of the natural-frequency term are added directly (since they must eventually come in phase) to give the total maximum voltage. Peak values of transient voltages are shown by dashed lines for $x_{c0} = x_{c1}$ and $x_{c0} = 2x_{c1}$.

Figure 10, for the same conditions as figure 8, gives the maximum transient zero-sequence voltage at the fault for a line-to-ground fault calculated from equation 18, appendix *A*. The maximum zero-sequence transient voltage at the fault for a double-line-to-ground fault will be one-third the voltage of the unfaulted phase given by figure 9. See equation 23, appendix *A*.

With X_0/X_1 between 0 and -2 in figure 8, and between 0 and -0.5 in figure 9, the natural frequency, ω_n, is less than unity. See equations 21 and 24, appendix *A*. Higher transient values than those plotted in this region could therefore have been obtained with $\theta_0 =$ 0 degrees instead of 90 degrees.

The natural frequency, ω_n, in per unity of fundamental frequency given by (21) for the line-to-ground fault with $x_{c0} = x_{c1}$ is plotted in figure 11, with abscissa X_0/X_1 and parameter x_{c1}/x_1. From figure 11, at the resonant point ($X_0/X_1 = -2$), the natural frequency is unity and for values of X_0/X_1 between -2 and 0, less than unity. The region between 0 and -2 corresponds to a very low ratio of x_{c1}/x_1, such as would probably not be encountered in a practical system operating normally.

METHOD OF GROUNDING

Reactance Grounding

Figures 8 and 9 can be used to determine transient voltages with resistance neglected when the neutral is solidly grounded or grounded through any reactance, including a ground-fault neutralizer. For a solidly grounded neutral, $x_n = 0$. For a ground-fault neutralizer with resistance neglected, $3x_n + x_0 = x_{c0}$, and $X_0 = \infty$.

Comparison of Resistance and Reactance Grounding

A comparison of the effects of resistance and reactance grounding on the fundamental-frequency fault voltages following a line-to-ground fault can be obtained from figures 1, 4, and 5 when the positive- and zero-sequence impedances viewed from the fault are known.

To compare the effects of reactance and resistance grounding on transient overvoltages following faults, the curves of figure 8 for a line-to-ground fault have been replotted in figure 12 in terms of the impedances indicated in figure 7. Figures 12 and 13 show the magnitudes of the transient voltages which may be obtained following a line-to-ground fault when the neutral is grounded through reactance and through resistance, respectively, with $x_0 = x_1$ and $x_{c0} = x_{c1}$.

The voltages in figure 13 and some points in figure 12 were determined by using a miniature system having the characteristics and features described in appendix *B*. The full-line curves correspond to fundamental-frequency voltages for different values of the ratio of $3x_n/x_1$ or $3r_n/x_1$, where x_n is the grounding reactance in the case of figure 12 and r_n is the grounding resistance in the case of figure 13. As will be noted, exceptionally high voltages are not obtained until the capacitive reactance x_{c0} becomes small compared with x_1. This condition corresponds to a system having a very large amount of line-charging capacity compared with the connected generation. Also, it will be noted that transient voltages do not exceed twice the fundamental-frequency voltages with either reactance or resistance grounding, for the conditions assumed.

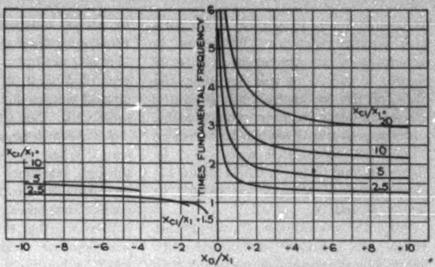


Figure 11. Natural frequency with line-to-ground fault for system shown in figure 7, with resistance neglected and $x_{c0} = x_{c1}$

The sustained voltages for resistance-grounded systems are generally higher than those for corresponding reactance-grounded systems. This is particularly true if the neutral grounding ohms are selected to give the same value of short-circuit current, as can be seen from an analysis of figures 12 and 13. For high ohmic values of neutral-grounding impedance, the transient voltages may be higher for reactance-grounded than for resistance-grounded systems. For low ohmic values, the natural-frequency component of voltage for resistance-grounded systems decreases and becomes negligible for values of $3r_n/x_1 < 5$. Accordingly, a reactance-grounded system may not subject the protective equipment to as high sustained voltages as a resistance-grounded system, but from the standpoint of natural-frequency overvoltages, particularly those associated with switching phenomena, the resistance-grounded neutral may be more desirable.[15,16]

Figures 14 and 15 show the corresponding neutral-to-ground voltages for the cases shown in figures 12 and 13, respectively. This information is of value in indicating the voltages which may occur from neutral-to-ground under the condition of line-to-ground faults. Exceptionally, high overvoltages are not obtained at the neutral except for systems which have a relatively large amount of connected line in proportion to the positive-sequence reactance. The curves, in general, have the same shape as those in figures 12 and 13, as it is essentially the shift or rise in neutral voltage which causes the overvoltage on the open phases.

Figures 16 and 17 are similar to figures 12 and 13, except that $x_{c0}/x_{c1} = 2$ in figure 16, and $x_0/x_1 = 0.5$ in figure 17. Only transient voltages are plotted in figures 16 and 17.

Since the magnitudes of overvoltages which are obtained are considerably affected by the method of grounding, this discussion is classified accordingly.

1. *Solidly Grounded System.* There are many degrees of *solid grounding* and the term means very little as far as the overvoltages are concerned, as it depends upon the number, location, and kilovolt-ampere capacity of the grounding points. However, if the grounded-neutral system is considered to be one in which grounded-neutral lightning arresters may be used (line-to-ground sustained voltages not to exceed 140 per cent to 150 per cent of normal), it becomes apparent that the ratio of X_0/X_1 should be kept below about 3 or 4. See figures 1, 2, and 6. Accordingly, this will ordinarily mean that a large percentage of all transformers or machines must be solidly grounded.

Systems operating in this classification, that is, X_0/X_1 not greater than 3 or 4, have a maximum transient line-to-ground voltage on the unfaulted phases not exceeding 2.0 times normal (see figures 8 and 9). The maximum neutral-to-ground transient voltage at any point in the system, for example, the neutral of an ungrounded bank, is about normal line-to-ground voltage due to the occurrence of a solid fault. See figure 10.

2. *Neutral Grounded Through Reactance.* When a system is grounded through reactance less than that of a ground-fault neutralizer, the zero-sequence impedance viewed from the fault is inductive rather than capacitive and the zero-sequence resistance is relatively small; accordingly, the fundamental-frequency phase-to-ground voltages will not exceed normal line-to-line voltage, and the neutral-to-ground voltage will not exceed normal line-to-neutral voltage. See figures 1–6, with X_0/X_1 positive and $R_0/X_1 < X_0/X_1$.

Following a fault, systems with reactance grounds will have maximum transient voltages to ground on the unfaulted phases not exceeding 2.73 times normal. The voltage to ground of the neutral will not exceed 1.67 times normal line-to-neutral voltage. See figures 8–10.

3. *Neutral Grounded Through Resistance.* When a system is grounded through resistance, the zero-sequence impedance viewed from the fault may be inductive or capacitive, depending upon the number and location of the grounding points and the amount of connected line or cable. With low-resistance grounds, X_0 will ordinarily be positive and the fundamental-frequency phase-to-ground voltages will, in general, not exceed normal line-to-line voltage and the neutral-to-ground voltages will not exceed normal line-to-neutral voltage. With high-resistance grounds, X_0 may be negative. In that case, phase-to-ground voltages may be greater than normal line-to-line voltages, and neutral-to-ground voltages greater than normal line-to-neutral voltages. See figures 1, 3, 4, and 5.

If low-resistance grounding is used, the natural-frequency voltages are practically eliminated and the maximum voltages are essentially the fundamental-frequency voltages which, however, are generally higher than the fundamental-frequency voltages obtained with corresponding values of neutral-grounding reactance.

4. *System Grounded Through Fault Neutralizer.* For systems grounded through ground-fault neutralizers, with resistance neglected, X_0 is infinite; with resistance included, R_0 is very large while X_0 is negative. Based on either assumption, the fundamental-frequency voltages on the unfaulted phases at the fault following a line-to-ground fault are essentially line-to-line voltages. See figure 1. The maximum transient voltages-to-ground on the unfaulted phases are less than 2.73 times normal, and of the neutral-to-ground less than 1.67 times normal line-to-neutral voltage. See figures 8 and 10.

Higher voltages may be obtained at points removed from the ground-fault neutralizers where there is in effect concentrated an appreciable amount of zero-sequence capacitance to ground. This indicates the advisability of placing the ground-fault neutralizer at the centers of the system, and also the desirability of using, under certain conditions, more than one ground-fault neutralizer.

5. *Isolated Neutral.* In the case of an isolated-neutral system, X_0 is negative and of the order of magnitude of the capacitive reactance while R_0/X_1 is relatively small. From figures 1, 4, and

Figure 12. Transient and fundamental-frequency voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

$x_0 = x_1$ $x_{c0} = x_{c1}$

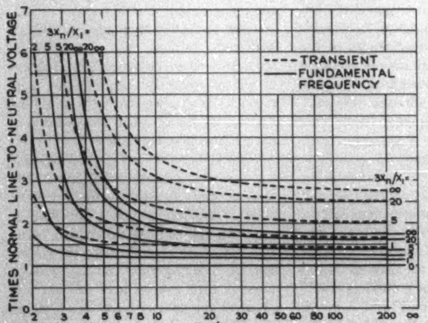
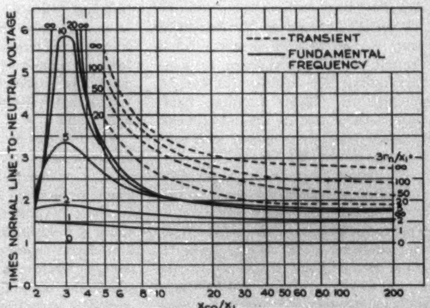


Figure 13. Transient and fundamental-frequency voltages for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground of unfaulted phases at the fault

$x_0 = x_1$ $x_{c0} = x_{c1}$



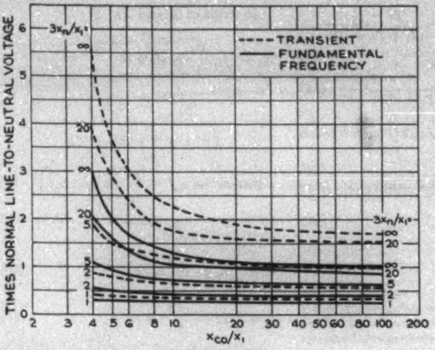


Figure 14. Transient and fundamental-frequency voltages with resistance neglected for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground at the neutral

$x_0 = x_1$ $x_{c0} = x_{c1}$

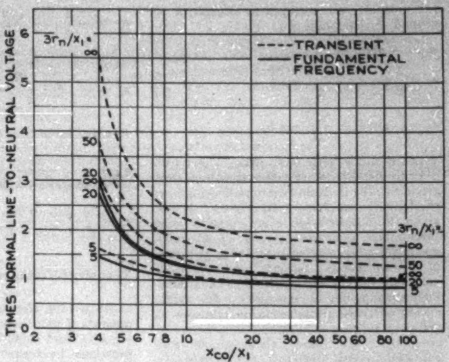


Figure 15. Transient and fundamental-frequency voltages for system shown in figure 7. Line-to-ground fault. Maximum voltage-to-ground at neutral

$x_0 = x_1$ $x_{c0} = x_{c1}$

5, the fundamental-frequency voltages obtained may be in excess of normal line-to-line voltage; and in some cases, particularly when the system is large in extent, considerably in excess, so that a very undesirable condition is created when faults occur. The fact that such voltages may be obtained makes it highly desirable that under no condition of operation shall a system lose its grounding points, if by so doing it is liable to be in the region of resonance. Otherwise, damage to the protective equipment or flashover of major equipment may result. The minimum voltage rating commonly used in isolated-neutral arresters is 1.83 times normal line-to-neutral voltage.

VOLTAGES DISTANT FROM FAULT

The fundamental-frequency and transient voltages given by the curves in this paper are at the point of fault. Under certain conditions higher voltages than those occurring at the point of fault may be obtained. This is particularly true at points which are located at a distance from the fault and the grounding points, but located where there is, in effect, an appreciable amount of zero-sequence capacitance to ground. The fundamental zero-sequence voltage at such points can be readily calculated from the zero-sequence voltage at the fault and the zero-sequence network. Figure 18 shows the simplified zero-sequence impedance diagram with the identity retained of the fault point F and point P, at which voltage is required. The zero-sequence voltage at P is

$$V_{a0}\text{ (at }P) = V_{a0}\text{ (at }F)\frac{Z_z}{Z_y + Z_z}$$

With Z_z capacitive reactance and Z_y inductive reactance, the zero-sequence voltage at P will be higher at P than at F. If it is appreciably higher, additional calculations are required for determining both fundamental- and natural-frequency voltages. The case of higher fundamental-frequency voltages at points distant from the fault than at the point of fault has been discussed in a recent paper.[9]

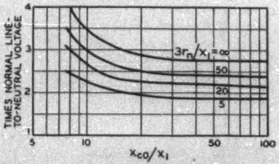


Figure 16. Maximum transient unfaulted-phase voltage-to-ground for system shown in figure 7. Line-to-ground fault. Neutral grounded through resistance

$x_0 = x_1$ $x_{c0} = 0.5x_{c1}$

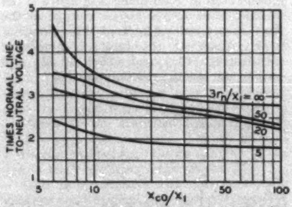


Figure 17. Maximum transient unfaulted-phase voltage-to-ground for system shown in figure 7. Line-to-ground fault. Neutral grounded through resistance

$x_0 = 0.5x_1$ $x_{c0} = x_{c1}$

Nomenclature

$Z_1 = R_1 + jX_1$, $Z_0 = R_0 + jX_0$ = positive and zero-sequence fundamental-frequency impedances, respectively, viewed from the fault point

R_f = fault resistance

$Z_1(p)$, $Z_0(p)$ = operational expressions for the positive and zero-sequence impedances, respectively, viewed from the fault point

Lower case letters apply to the circuit shown in figure 7.

$z_1 = r_1 + jx_1$, $z_0 = r_0 + jx_0$ = positive and zero-sequence fundamental-frequency impedances, respectively, from neutral to the fault point

$z_n = r_n + jx_n$ = fundamental-frequency impedance between neutral and ground

x_{c1}, x_{c0} = positive and zero-sequence fundamental-frequency capacitive reactances, respectively, of transmission line

$\theta = \theta_0 + t$ = angle between direct axis of reference machine and axis of phase a

ω_n = natural frequency in times fundamental frequency

Appendix A. Fundamental- and Natural-Frequency Voltages

The instantaneous phase voltages at any point in a balanced three-phase system in per unit of crest voltages to neutral or to ground at that point are

$$\left.\begin{aligned} e_a &= -\sin\theta \\ e_b &= \frac{1}{2}\sin\theta + \frac{\sqrt{3}}{2}\cos\theta \\ e_c &= \frac{1}{2}\sin\theta - \frac{\sqrt{3}}{2}\cos\theta \end{aligned}\right\} \quad (1)$$

Let e and i with appropriate subscripts refer to instantaneous values of voltages-to-ground at the fault and currents flowing into the fault, respectively. In reference 17 are given the relations for transformation from three-phase quantities (a, b, c) to symmetrical-component quantities for the case of instantaneous currents and voltages, including the transient as well as the fundamental-frequency components. An application of these transformation relations is given in reference 5, except that α and β components are used. However, for the case under consideration, $Z_\alpha(p) = Z_\beta(p) = Z_1(p) = Z_2(p)$, so the application here is quite similar. Using the generalized method of symmetrical components, the following equations are obtained.

Line-to-Ground Fault (Phase a)

$${}_{a1} = i_{a2} = i_{a0} = \frac{-\sin\theta}{2Z_1(p) + Z_0(p) + 3R_f}$$

$${}_{a0} = -i_{a0}Z_0(p) = \frac{Z_0(p)\sin\theta}{2Z_1(p) + Z_0(p) + 3R_f} \quad (2)$$

$$e_b = \frac{1}{2}\sin\theta + \frac{\sqrt{3}}{2}\cos\theta + \frac{Z_0(p) - Z_1(p)}{2Z_1(p) + Z_0(p) + 3R_f}\sin\theta \quad (3)$$

$$e_c = \frac{1}{2}\sin\theta - \frac{\sqrt{3}}{2}\cos\theta + \frac{Z_0(p) - Z_1(p)}{2Z_1(p) + Z_0(p) + 3R_f}\sin\theta \quad (4)$$

Double Line-to-Ground Fault (Phases b and c)

$$i_{a0} = -(i_{a1} + i_{a2}) = \frac{\sin\theta}{Z_1(p) + 2Z_0(p) + 6R_f}$$

$$e_{a0} = -i_0Z_0(p) = \frac{-Z_0(p)\sin\theta}{Z_1(p) + 2Z_0(p) + 6R_f} \quad (5)$$

$$e_a = -\frac{3Z_0(p) + 6R_f}{Z_1(p) + 2Z_0(p) + 6R_f}\sin\theta \quad (6)$$

Line-to-Line Fault (Phases b and c)

$$e_a = -\sin\theta \quad (7)$$

$$e_b = e_c = \frac{1}{2}\sin\theta \quad (8)$$

Fundamental-Frequency Components of Voltage

Introducing vector quantities[5] by replacing $-\sin\theta$ by unity, $\cos\theta$ by $-j$, and $Z_1(p)$ and $Z_0(p)$ by Z_1 and Z_0, respectively, in (2)–(6), crest voltages of fundamental-frequency are obtained.

Line-to-Ground Fault (Phase a)

$$e_{a0} = -\frac{Z_0}{2Z_1 + Z_0 + 3R_f} \quad (9)$$

$$e_b = -\frac{1}{2} - j\frac{\sqrt{3}}{2} - \frac{Z_0 - Z_1}{2Z_1 + Z_0 + 3R_f} \quad (10)$$

$$e_c = -\frac{1}{2} + j\frac{\sqrt{3}}{2} - \frac{Z_0 - Z_1}{2Z_1 + Z_0 + 3R_f} \quad (11)$$

Double Line-to-Ground Fault (Phases b and c)

$$e_{a0} = \frac{Z_0}{Z_1 + 2Z_0 + 6R_f} \quad (12)$$

$$e_a = \frac{3Z_0 + 6R_f}{Z_1 + 2Z_0 + 6R_f} \quad (13)$$

Transient Voltages

To obtain the natural-frequency components of equations 2–6 necessitates replacing $Z_1(p)$ and $Z_0(p)$ by their operational expressions in terms of system constants. This can be done only when the system is given. For the system represented in figure 7,

$$Z_1(p) = \frac{(r_1 + px_1)\dfrac{x_{c1}}{p}}{r_1 + px_1 + \dfrac{x_{c1}}{p}} \quad (14)$$

$$Z_0(p) = \frac{[r_0 + 3r_n + p(x_0 + 3x_n)]\dfrac{x_{c0}}{p}}{r_0 + 3r_n + p(x_0 + 3x_n) + \dfrac{x_{c0}}{p}} \quad (15)$$

Resistance Neglected. With resistance neglected in figure 7, $Z_1(p)$, $Z_0(p)$, Z_1, and Z_0 become,

$$Z_1(p) = \frac{px_1x_{c1}}{p^2x_1 + x_{c1}} \quad (14a)$$

$$Z_0(p) = \frac{p(x_0 + 3x_n)x_{c0}}{p^2(x_0 + 3x_n) + x_{c0}} \quad (15a)$$

$$Z_1 = \frac{jx_1x_{c1}}{x_{c1} - x_1} = jX_1 \quad (16)$$

$$Z_0 = j\frac{(x_0 + 3x_n)x_{c0}}{x_{c0} - (x_0 + 3x_n)} = jX_0 \quad (17)$$

Line-to-Ground Fault. With a line-to-ground fault at F on phase a in figure 7, the instantaneous zero-sequence voltage at the fault in per unit of normal line-to-neutral peak voltage at the fault, obtained by substituting (14*a*) and (15*a*) in (2) and solving the operational equations is

$$e_0 = \frac{Z_0}{2Z_1 + Z_0}\sin\theta - \frac{2\left(\dfrac{x_0 + 3x_n}{x_{c0}} - \dfrac{x_1}{x_{c1}}\right)}{\left(\dfrac{2}{x_{c0}} + \dfrac{1}{x_{c1}}\right)\left[2x_1 + x_0 + 3x_n - x_1(x_0 + 3x_n)\left(\dfrac{2}{x_{c0}} + \dfrac{1}{x_{c1}}\right)\right]} \times \left(\sin\theta_0\cos\omega_nt + \frac{1}{\omega_n}\cos\theta_0\sin\omega_nt\right)$$

In terms of X_0 and X_1,

$$e_{a0} = \frac{X_0}{2X_1 + X_0}\sin\theta - \frac{2\left(\dfrac{x_{c1}}{x_{c0}}X_0 - X_1\right)}{\left(1 + 2\dfrac{x_{c1}}{x_{c0}}\right)(2X_1 + X_0)} \times \left(\sin\theta_0\cos\omega_nt + \frac{1}{\omega_n}\cos\theta_0\sin\omega_nt\right) \quad (18)$$

with $x_{c0} = x_{c1}$, equation 18 checks that given by Fallou.[10]

$$e_b = \frac{3}{2}e_0 + \frac{\sqrt{3}}{2}\cos\theta \quad (19)$$

$$e_c = \frac{3}{2}e_0 - \frac{\sqrt{3}}{2}\cos\theta \quad (20)$$

where

$$\omega_n = \sqrt{\frac{2x_1 + x_0 + 3x_n}{2x_1(x_0 + 3x_n)\left(\dfrac{1}{x_{c0}} + \dfrac{1}{2x_{c1}}\right)}} = \sqrt{\frac{\dfrac{X_0}{X_1}\left(\dfrac{x_{c1}}{x_1} + 2\dfrac{x_{c1}}{x_{c0}}\right) + 2\left(\dfrac{x_{c1}}{x_1} - 1\right)}{\dfrac{X_0}{X_1}\left(2\dfrac{x_{c1}}{x_{c0}} + 1\right)}} \quad (21)$$

Double-Line-to-Ground Fault. With a double-line-to-ground fault at F on phases b and c, the instantaneous zero-sequence and phase a voltage-to-ground at the fault in per unit of normal line-to-neutral peak voltage at the fault are obtained by substituting (14*a*) and (15*a*) in (5) and (6) and solving the operational equations. Expressed in terms of the positive- and zero-sequence reactances viewed from the fault,

$$e_0 = -\frac{X_0}{X_1 + 2X_0}\sin\theta + \frac{\dfrac{x_{c1}}{x_{c0}}X_0 - X_1}{\left(2 + \dfrac{x_{c1}}{x_{c0}}\right)(X_1 + 2X_0)} \times \left(\sin\theta_0\cos\omega_nt + \frac{\cos\theta}{\omega_n}\sin\omega_nt\right) \quad (22)$$

$$e_a = 3e_0 \quad (23)$$

where

$$\omega_n = \sqrt{\frac{x_1 + 2(x_0 + 3x_n)}{2x_1(x_0 + 3x_n)\left(\dfrac{1}{2x_{c0}} + \dfrac{1}{x_{c1}}\right)}} \quad (24)$$

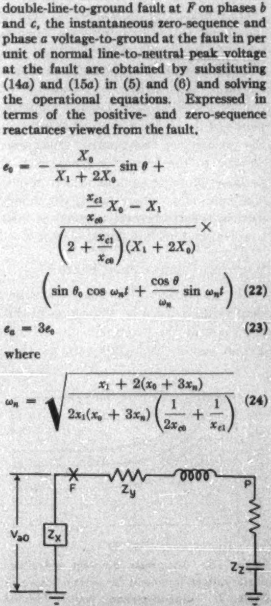


Figure 18. Simplified zero-sequence impedance diagram

Appendix B. Description of Miniature System

The miniature equivalent system used in making the laboratory tests is shown in figure 19.

The reactors used in this system had low ratios of resistance to reactance which varied from about 0.01 to 0.04 at 60 cycles depending upon the portion of the total winding being used. The miniature power transformer was also of low-loss design so that the entire system caused relatively low damping of natural-frequency oscillations. This was desirable for purposes of checking calculations based on no-loss circuits.

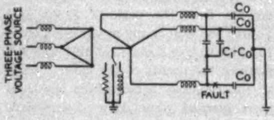


Figure 19. Miniature equivalent system

The miniature system was energized from a three-phase 110-volt 60-cycle voltage source of such capacity that its terminal voltage remained essentially constant regardless of transient conditions imposed by faults in the equivalent circuits.

A synchronous commutator was used to apply and remove repeatedly the fault in synchronism with the system voltage. The commutator drum was driven by an 1,800-rpm synchronous motor by means of a ten-to-one gear reduction so that one revolution of the drum was completed every 20 cycles. The contacts on the switch were so made that during these 20 cycles the fault would be on 5 cycles and off 15 cycles. The relatively long period during which the fault was off afforded ample time for steady-state conditions to be reached before the switching operation was repeated.

A cathode-ray oscilloscope was used to measure the transient voltages. Another contactor on the synchronous commutator was used to control the grid on the cathode-ray tube so that the light beam would appear on the screen for only one cycle out of each 20. This made it possible to obtain a clear picture of the transient voltages during the period of particular interest.

The contactors on the commutator were mounted on a rack which could be rotated to vary the instant of application of the fault corresponding to various points on the voltage wave. Thus it was possible to determine the maximum voltage that could be reached by selecting the angle of fault application which gave the highest transient voltage on the oscilloscope.

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Discussion

P. A. Jeanne: See discussion, page 395.

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Concerning the results in the Clarke-Crary-Peterson paper, it may be observed that the simple form of network illustrated in figure 19 for transient analysis, might lead to appreciable error. The use of such a simple equivalent circuit reduces to an extent the usefulness of the results.

Also, in connection with this paper, the authors have carried the curves to negative value of X_0/X_1 whereas in the previous published work by Evans and Wright[1] only values for positive ratios were considered. The curves in the negative zone are no doubt correct from an academic point of view. However, we are inclined to question their practical use when high values of overvoltage are considered as the constants of the circuit will change in a direction tending to limit the magnitude of voltage. Such changes in circuit constants may be due to corona, magnetizing current of transformers, etc. The practical range of the curves is probably from -1 ratio to the positive values.

A value of X_0/X_1 of 3 to 4 is suggested for a safe application of a grounded-neutral lightning arrester. It is believed that in drawing this conclusion the effects of fault resistance have been ignored. When high values of fault resistance are considered the ratio of X_0/X_1 must be lowered if the fault is at the point of application of the arrester. In the work published by Monteith and Roman[2] it was considered improbable to have a fault at the point of application of the arrester since the arrester should prevent flashover. When the fault is considered at a point other than where the arrester is applied, the ratio of 3 to 4 for X_0/X_1, can be selected and the criterion will be entirely independent of fault resistance.

(See also discussion, page 412.)

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1. Some Effects of Unbalanced Faults, **R. D. Evans** and **S. H. Wright.** Electrical Engineering, June 1931, pages 415–20.

2. Application of Station-Type Lightning Arresters, **A. C. Monteith** and **W. G. Roman.** *Electric Journal*, March 1938, pages 93–8.

Edith Clarke, S. B. Crary, and **H. A. Peterson:** In answer to the discussion by Messrs. Evans, Monteith, and Witzke:

The simple network of figure 19, although an approximation, gives transient voltages at the point of fault not *less* than the maximum transient voltages which may occur (see "Basis of Study," 9), and thus enables us to draw conclusions in regard to the maximum transient voltages which can occur at the point of fault for various conditions of grounding.

The curves for negative values of X_0/X_1 have been found practical in determining voltages corresponding to high negative values of X_0/X_1 beyond the resonance region. As pointed out in the paper, the very high voltages indicated by the curves would not be obtained in the resonance region, since they are critical values and therefore greatly influenced by saturation, corona, and variation of machine reactances with time (see "Basis of Study," 5 and 6). They are useful, however, in that they indicate regions where high voltages will be obtained, that is, voltages in excess of normal line-to-line voltages.

The effect of fault resistance was not ignored in suggesting a value of X_0/X_1 between 3 and 4 for safe application of a grounded-neutral arrester. This conclusion is based on the curves of figure 6, which include the effect of fault resistance. The curves apply to cases in which the arrester and fault locations are far enough from each other so that mutual ground resistance of the arrester and fault is not encountered, and yet not so remote from each other that voltages to ground at the arrester are appreciably different from those indicated. Figure 6 in the region of $X_0/X_1 = 1$ to 5 checks the curves of figure 4 given by Messrs. Monteith and Roman in reference 8.

Power-System Transients Caused by Switching and Faults

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Synopsis: This paper summarizes the results of an investigation of transient voltages on power systems caused by switching and faults. The transient voltages on power systems as measured by the "klydonograph" are reviewed and compared with the flashover values of transmission-line insulation. It is shown that the higher values of transient voltages are produced by intermittent arcs. In part I, the various theories for the production of transient voltages of high magnitude as a result of intermittent arcs are reviewed and extended in order to obtain the highest voltages on typical polyphase systems with the range of natural frequencies and attenuation factors that are encountered in practice. Previous studies are of limited scope and apply principally to the case of an arcing ground on an ungrounded system. The present study shows broadly the range of transient voltages which may be produced with intermittent arcs and applies to switching operations as well as arcing grounds. A typical transmission system is studied with the aid of the a-c network calculator. One of the principal variable factors in this study is the method of system grounding and this includes a range of both resistance and reactance between the limits of a solidly grounded system and an ungrounded system. The study is carried out for four different conditions, namely: (1) arcing grounds, (2) de-energizing an unfaulted line section, (3) de-energizing a line section with a fault on one phase, and (4) de-energizing a line section with a fault on two phases. The results of this study are presented in graphical form in part II and show many interesting properties of systems with respect to the method of grounding, and the characteristics of transient voltages for the different switching and fault conditions. It is the authors' opinion that the transient voltages due to faults and switching deserve more attention than they have received within the last few years.

THE EXISTENCE of transient overvoltages on transmission (and distribution) systems as a result of circuit changes caused by switching operations or faults has long been recognized. Many years ago when transmission systems were first expanding, the effect of arcing grounds received a great deal of attention. The phenomena, however, were not thoroughly investigated at the time because suitable measuring and recording devices were not available and because the immediate difficulties were largely overcome by the adoption of the practice of grounding transmission systems. The invention by J. F. Peters[1] of the "klydonograph," the first really practical surge recorder, made possible the collection of a mass of field data on overvoltages. The introduction of this instrument stimulated extensive investigations of voltages caused both by lightning and other transients. In recent years, considerable attention has been directed to the lightning phase of the problem but other phases have largely been neglected. It is the authors' view that the time has come when further attention should be given to the problems of overvoltages caused by faults and switching operations. To them it seems quite possible that some of the multiphase faults on systems which are now attributed wholly to lightning may, in reality, be caused in part by voltages produced by intermittent arcs.

It is pertinent to review the operating experience which has been obtained on transmission lines in regard to overvoltages produced by switching surges arising from circuit changes or isolation of faulted conductors. Quite a number of klydonograph investigations have been reported in the literature and many of these segregate the overvoltages resulting from switching operations from those due to lightning. Extensive investigations were reported by Cox, McAuley, and Huggins;[2] Gross and Cox;[3] Lewis and Foust;[4] and by a number of European investigators. The Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System, has also carried on some investigations and has made an excellent summary[5] of the more important published data.

The principal results of the switching surge studies made with the aid of the klydonograph have been summarized in figure 1. In this figure, curves *A* and *B* obtained from the original investigation by Cox, McAuley, and Huggins,[2] give the voltages due to energizing or de-energizing operations and the voltages due to faults with subsequent switching, respectively. Curve *C* gives a summary derived from the work of Lewis and Foust,[4] the most recent report of its kind. In order to give a more suitable scale for plotting the results of the surge studies all the surges of a magnitude less than twice normal have been disregarded. The Lewis and Foust paper, however, shows that of all the reported surges above normal voltage, 45 per cent were above twice normal. Figure 1 shows that the limiting value of the surges is about six times normal crest voltage, 5 per cent exceed five times normal, and 20 per cent exceed four times normal.

It will be noted from figure 1 that there is an upper limit to the voltage recorded, indicating the possibility of some limiting factor. Figure 2 shows the ratio of the voltage required to produce flashover, to the normal crest voltage, for different voltage transmission lines equipped with the range of insulator units encountered in practice. The shape of the curve of figure 1 compared with the data given in figure 2, indicates that the magnitude of switching surges recorded could be limited by line flashover. While it is undoubtedly true that a considerable percentage of these switching operations take place with relatively little energy in the oscilla-

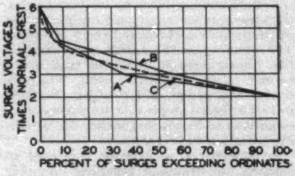

Figure 1. Distribution of surge voltages caused by switching and faults

A—Switching surges—Cox, McAuley, and Huggins

B—Surges from faults—Cox, McAuley, and Huggins

C—Switching surges—Lewis and Foust

A and *B*—Eighteen systems—1925 to 1926

C—Fourteen systems—1926 to 1930

Paper number 39-48, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 14, 1938; made available for preprinting December 27, 1938.

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The authors wish to acknowledge the assistance which they have received from their associates, particularly I. F. Krughoff, who carried out the principal part of the studies with the a-c network calculator.

[1] For all numbered references, see list at end of paper.

tion and at relatively high frequency, it is also true that as systems expand the natural frequency of systems for switching operations decreases and the amount of energy in these oscillations increases. Thus, these factors tend to increase the importance of switching surges.

Part I. Mechanism of Producing Transient Overvoltages as a Result of Circuit Changes

The mechanism of producing transient overvoltages on transmission (and distribution) systems as a result of circuit changes caused by switching operations and faults will now be considered. The magnitude of these transient voltages, as shown in figure 1, will, under some conditions, exceed the sum of two components, (1) the final steady-state voltage, and (2) a transient voltage equal to the difference between it and the initial steady-state voltage. The value of this transient voltage has been assumed to be the one indicated by the conventional theory of circuit changes involving a single switching operation producing a simple circuit change such as a "make" or a "break." Thus the transient voltage, which is due to a "make" such as the sudden and permanent fault to ground, or a "break" such as the sudden and permanent de-energizing of a circuit, are relatively low in comparison with the maximum transient voltages that are known to have been produced by switching operations or faults. In a "simple circuit change" as the expression is used in this paper, it is assumed that the change caused by a fault or switching operation takes place through a path that changes abruptly from a perfectly conducting to a nonconducting condition, or vice versa.

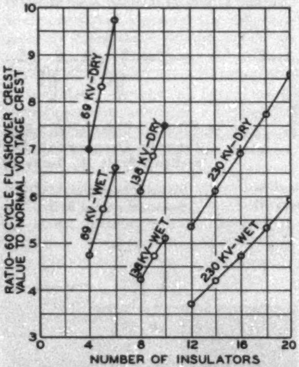


Figure 2. Sixty-cycle flashover voltage ratios for ten-inch suspension units

It is known that the circuit changes which produce the higher values of transient voltages involve arc paths. It has also been recognized that the characteristics of the arc path have a relation to the overvoltages, but the nature of the phenomenon has, in general, not been well understood. A little consideration will show that the conditions for producing high transient overvoltages from switching or faults have in common these features of (1) a circuit change through a path involving an arc, and (2) a circuit change which differs from that of a single "make" or "break" as previously discussed.

In considering the characteristics of an arc path in respect to the production of transient overvoltages, two classes of factors may be recognized, namely:

1. Abrupt forcing of current zero and high extinction voltage.
2. Cumulative action from intermittent arcing.

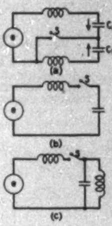
The phenomenon involving the first of these, the abrupt forcing of current zero, is well known in the simple form by which high voltages are produced when inductive circuits are quickly opened. This is not likely to be encountered with conventional types of interrupting equipment for faults and switching operations on power transmission systems. Instead, the probable conditions for the production of overvoltages from the first factor include the opening of the magnetizing circuits of transformers, switching of induction regulators, and other similar operations.

The extinction voltage or the voltage across an arc just prior to its interruption is also a factor affecting the magnitude of transient overvoltages. This factor is of importance on low-voltage but not on high-voltage circuits. In general, it is the authors' opinion, as pointed out elsewhere,[6] that the factors of abrupt forcing of current zero and high extinction voltage are not responsible for producing the more important transient overvoltages on transmission systems. Accordingly, only the effect of cumulative action from intermittent arcing has been considered in this investigation.

Theories of Intermittent Arcing

Various theories of intermittent arcing have been proposed to account for the high voltages produced by arcing grounds on ungrounded systems. These theories were reviewed some 15 years ago by Peters and Slepian[7] and their designations for the principal ones as theory I, theory II, and theory III, have been retained. The application of these theories has been extended in this investigation to the full range of grounding conditions for the arcing ground case and to other cases involving switching and faults. These theories have been studied for simple and complicated circuits and it has been found that the basic features of theories I, II, and III are applicable but that to produce the highest voltages some modifications were necessary to cover the range of natural frequencies and attenuation factors, and the complication of circuits encountered on actual three-phase systems.

Figure 3. Simple circuits illustrating arcing grounds (a) and switching surges (b, c)



The close relationship between the intermittent arcing theories for arcing ground and switching cases makes it desirable to consider them together. The various theories of intermittent arcing are based on different assumptions in regard to the points at which the arc is interrupted and established or re-established. Thus the interruption of the arc may take place at a current zero close to a fundamental current zero or close to a natural- or high-frequency current zero. The arc may be established or re-established at a fundamental-frequency voltage crest or at a natural-frequency voltage crest.

In explaining these theories it is convenient to represent the intermittent arc by a switch, the opening and closing of which are controlled in accordance with the different theories. In using the switch to simulate an intermittent arc, certain dielectric characteristics of the arc path are assumed as discussed subsequently.

The essential features of the three theories are illustrated with the aid of the simplified circuits in figure 3. Figure 3*a* is a circuit for the representation of an arcing ground taking place at point *S*.

Figure 3*b* is a circuit used to represent the switching of a capacitor when supplied from a source through an inductance and corresponds to a switching operation where the disconnected section is left without a discharge path. Figure 3*c* is similar to the previous circuit but differs in that the disconnected section is provided with a discharge path.

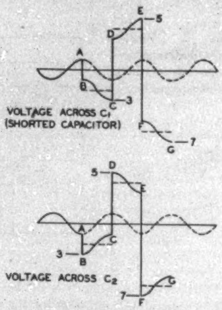


Figure 4a. Arcing ground (circuit 3a, theory III)

Switch closed at *A, C, E, G*
Switch opened at *B, D, F*

Table I. Comparison of Theories of Intermittent Arcing

(a) Initial Condition—Circuit Open

Theory	First Close*	First Open**	First Restrike*	Second Open**	Second Restrike*
I	FF	NF	NF	NF	NF
II	FF	FF	FF	FF	FF
III	FF	NF	FF	NF	FF

(b) Initial Condition—Circuit Closed

Theory	First Open**	First Restrike*	Second Open**	Second Restrike*	Third Open**
I	FF	NF	NF	NF	NF
II	FF	FF	FF	FF	FF
III	FF	FF	NF	FF	NF

* Closing occurs at a voltage crest of FF or NF component.

** Opening occurs at a current zero, determined principally by the FF or NF component.

FF = Fundamental frequency.

NF = Natural or high frequency.

The distinguishing features of the three theories of intermittent arcing may be obtained most readily by reference to table I. Part *a* of this table applies to the case of energizing a circuit, such as initiating an arcing ground in the circuit of figure 3*a* or the closing of a line section in the circuits of figures 3*b* and 3*c*. In all three theories the first closing of the switch is assumed to take place at the crest of the fundamental-frequency voltage across the switch, as this gives the highest transient voltages. In theories I and III the switch is always opened at the first current zero which is determined primarily by the natural- or high-frequency component of the current. In theory II the switch is not opened until the first fundamental current zero. In theory I the arc is assumed to restrike at the crest of the high-frequency oscillation across the switch. This restrike is simulated by reclosing the switch *S*. In theories II and III, the restrike takes place at the crest of the fundamental component of voltage across the switch.

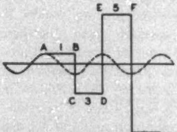


Figure 4b. De-energizing a line section (circuit 3b, theory III)

Switch opened at *A, C, E, G*
Switch closed at *B, D, F, H*

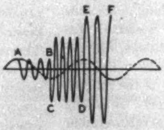


Figure 4c. De-energizing a line section (circuit 3c, theory III)

Switch opened at *A, C, E*
Switch closed at *B, D, F*

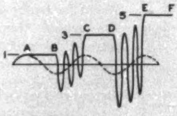


Figure 4d. De-energizing a line section (circuit 3b, theory II)

Switch opened at *A, C, E*
Switch closed at *B, D, F*

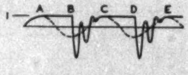


Figure 4e. De-energizing a line section (circuit 3b, theory II)

Switch opened at *A, C, E*
Switch closed at *B, D*

In the second part of table I, the switching sequences are illustrated for the case of de-energizing a circuit. In this case the switch *S* is initially closed, and is opened at a fundamental current zero. After the first opening the restrikes and arc extinctions follow the same sequence as for energizing a circuit.

In order to clarify the above theories several cases of intermittent arcing are discussed in detail for the circuits of figure 3. First, the case of de-energizing the capacitor in the circuit of figure 3*b* is analyzed in accordance with theory III. The successive steps of building up the capacitor voltage are shown in figure 4*b*. Normal capacitor voltage has been added as a dotted curve. The capacitor voltage is normal and at point *A* the switch is opened and a charge is left on the capacitor. The switch voltage is now the algebraic sum of the generated voltage and the voltage due to the charge on the capacitor. It will reach a maximum of twice normal generated voltage crest at one-half cycle of fundamental frequency after the switch is opened. At the point *B* the capacitor voltage has a value of +1 in per unit values, whereas the normal capacitor voltage, with the switch closed, is −1. Now if the switch is closed at this time the capacitor voltage will be accelerated toward a value of −1 but because of the circuit inductance or inertia the voltage will overshoot and, without damping, will reach a value of −3. If now the switch is opened, the capacitor will have a charge corresponding to a voltage of −3. If the switch is again reclosed one-half cycle after opening, the voltage will be accelerated from −3 toward a value of +1 but will overshoot to a

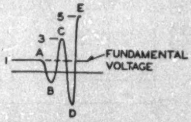


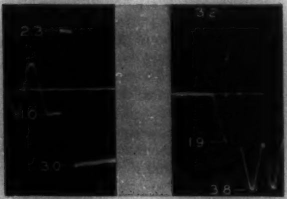
Figure 4f. De-energizing a line section (circuit 3b, theory I)

Switch opened at *A, C, E*
Switch closed at *B, D*

value of +5. It can be seen that the voltage builds up according to the series, 1, 3, 5, 7, . . . and will have no limit if damping is neglected. In this analysis it has been assumed that the inductance is small and that the natural frequency of the circuit is very high in comparison to the fundamental frequency.

Theory III has also been illustrated in figure 4*c* for the circuit of figure 3*c*. This case differs from that of figure 4*b* only in that the capacitor voltage oscillates around zero during the time the switch is open. The capacitor voltage is assumed to be the same in both cases at the time the switch is reclosed. The curves are derived on the assumption of no damping in the circuit.

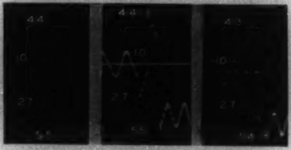
In figure 4*a* are shown the voltages across the capacitors C_1 and C_2 of the circuit shown in figure 3*a*. In this case an arcing ground is simulated by opening and closing the switch *S* in accordance with theory III. The capacitors are assumed to be of equal capacitance and the circuit inductance is very low, so that one-half the generated voltage appears across each capacitor with the switch open. If the switch is closed at *A*, the voltage across capacitor C_1 oscillates around zero because the generated voltage is not impressed on this mesh. With C_1 short-circuited, the steady-state voltage across C_2 is twice normal. Therefore, when the switch *S* is closed, capacitor C_2 is accelerated toward twice normal but will overshoot to three times normal. Now assume that the switch is opened at *B*, at which time C_1 has a charge corresponding to a voltage −1 and C_2 has a charge corresponding to a voltage −3. Because these charges are in opposite directions around the circuit through the generator, the capacitors will not discharge but will equalize with a charge corresponding to a voltage of −2. The steady-state voltage across the capacitor will then be the algebraic sum of one-half the generated voltage, and the voltage due to the charge on the capacitor. By referring to figure 4*a* it can be seen that the voltage across both capacitors is equal to these steady-state values at the instant the switch is opened and as the initial and steady-state voltages are equal, there will be no transient.



(*a*)—Capacitor voltage (*b*)—Switch voltage

Figure 5. De-energizing a line section (circuit 3b, theory III)

If the switch is reclosed at *C* the voltage across capacitor C_1 will again oscillate around zero. With C_1 short-circuited the steady-state voltage across C_2 is twice normal. Therefore, when the switch is closed, the voltage of C_2 is accelerated from −1 toward twice normal but will overshoot to +5 times normal. Now assume that the switch is again opened at *D*, at which time C_1 has a charge corresponding to a voltage of +3 and C_2 has a charge corresponding to a voltage of +5. These charges will equalize leaving a charge corresponding to a voltage of +4 on each capacitor. Adding normal capacitor voltages to the voltage due to residual charges gives a voltage of +3 across C_1 and of +5 across C_2. These are steady-state voltages, and as they are the same as the corresponding voltages, at the instant the switch is opened, there will be no transient. It should be noted that the capacitor voltages build up according to the series 1, 3, 5, . . . which is the same as for the switching operation in figure 4*b*. Thus it can be seen that arcing grounds and switching operations may build up high voltages by the same mechanism of intermittent arcing.



(*a*) (*b*) (*c*)

Figure 6. Arcing ground (circuit 3a, theory III)

(*a*)—Voltage across capacitor C_1

(*b*)—Voltage across capacitor C_2

(*c*)—Switch voltage

In figure 4*d* theory II is illustrated for the circuit of figure 3*b*. This differs from figure 4*b* in that the switch is not opened at the first current zero which is controlled by the natural-frequency component, but it is opened at a later instant corresponding to the fundamental current zero. Theory I is illustrated in figure 4*f* for the circuit of figure 3*c*. In this case the point of switching is controlled entirely by the high frequency. It is assumed that the oscillation is of very high frequency and the fundamental does not change during the interval of time considered.

In the above cases no damping was considered. Damping may appreciably affect the shape of the curves and will definitely limit the maximum voltages obtainable.[8] The effect of damping can be seen by comparing figures 4*d* and 4*e*. In figure 4*e* the high-frequency component of the capacitor voltage is assumed to be damped out in one-half cycle of the fundamental. The voltage is limited to three times normal under the assumed conditions of damping. In practical circuits the damping usually will not be as large as considered in figure 4*e* but will be large enough to require consideration.

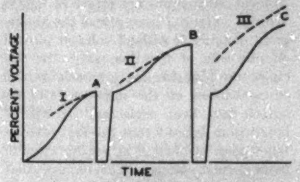


Figure 7. Dielectric recovery characteristics assumed for conditions of figure 5b

Note: Corresponds to the abstract circuit of figure 3*b*. All physical circuits have capacitances on source side which will introduce additional oscillations such as shown in figure 5*b*. Such demand different dielectric recovery characteristics to obtain the two restrikes shown

Another factor which cannot be neglected is the ratio of the natural frequency to the frequency of the generated voltage. In the above cases this ratio has been assumed to be very high. If this ratio is low the voltage will not build up as shown; the mechanism will be the same but the magnitudes will be less. For example, if in figure 4*b* the ratio were low the voltage would not build up to three times normal with one restrike because the fundamental decreases appreciably by the time the high-frequency component reaches its negative crest value. In many actual systems the natural frequency of the circuit may not be much above the supply frequency. For example, in a Petersen-coil grounded system the principal natural frequency is the same as the fundamental frequency.

At this point it is desirable to present some oscillograms of circuit transients in accordance with the foregoing discussion. While these oscillograms were actually taken on the a-c network calculator used in the general studies discussed subsequently, it is sufficient for present purposes to consider them as transients obtained on simplified circuits of figure 3 subjected to switching transients in accordance with theories I, II, and III. Typical oscillograms are shown in figures 5 and 6. Figure 7 is a replot of the oscillogram of figure 5*b* with all voltages plotted on one side of the time axis. In this curve the high-frequency transients due to oscillations on the source side of the switch have been neglected. It will be noted from figure 7 that the first restrike takes place at point *A* at a value close to twice normal voltage. This requires that the dielectric strength of the arc path recovers along some curve such as *I*, that is, along a curve which is above the curve of recovery voltage until at point *A* where they intersect. During the time the arc path is conducting, the dielectric strength of the switch is practically zero. When the arc is again extinguished, the dielectric strength curve again starts from zero but recovers much more rapidly and intersects the curve of recovery voltage at the point *B* causing a second restrike. After the next arc extinction the dielectric strength curve must recover still more rapidly in order to meet the assumed condition that no restrike should occur at the point *C*. These curves show the requirement for the dielectric strength of the arc path to obtain high overvoltages. If curve *I* were not as high as shown, the restrike would have occurred at a lower voltage and the capacitor voltage would not have been as large as shown in figure 4*b*. If the dielectric strength had built up at a more rapid rate, no restrike would have taken place. It can definitely be concluded that the dielectric strength must build up at a higher rate after any extinction than it did after the preceding extinction in order to develop cumulatively higher voltages. This phenomenon is unlikely to take place in open air between stationary contacts because such an arc path is unlikely to develop the required dielectric recovery strength. In confined arcs, where the pressure may increase after each conduction period, this phenomenon may take place. Separation of breaker contacts will have a tendency to cause higher dielectric strength recovery rates after each conducting period because of the increasingly larger contact separation. These requirements of the arc path probably provide an explanation for the difficulties which have been experienced in attempts to produce high voltages by intermittent arc paths in air over insulator strings. In this connection it may be observed that the conditions for producing high voltages by intermittent arcing are somewhat more favorable for the case of the apparatus failure under oil than for the case of a flashover of an insulator string. The sequences may be for an apparatus failure under oil to cause a line flashover instead of for a line flashover to cause apparatus failure.

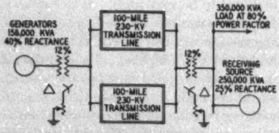


Figure 8. Schematic diagram of system selected for study

Transformer reactance: per cent on 156,000 kva

The foregoing discussion has been based on simple circuits for the purpose of illustrating the essential element of the theories of intermittent arcing. All actual systems are relatively quite complicated and cannot be reduced to the simple circuits used in the illustrations. Because of this complexity of actual systems it has been found that the maximum voltages with intermittent arcing are not obtained exactly in line with the preceding theories. More specifically, the maximum voltages are obtained for simple circuits with the restrikes occurring at either the high-frequency voltage crest or at the fundamental-frequency voltage crest. With complicated circuits it has been found that higher voltages can be obtained by modifying the point of restriking in one direction or the other from these points. This of course is due to the fact that the oscillating circuits have several natural frequencies. The determination of the exact manner of restriking is very difficult to define in analytical fashion. Because of this fact and because of the importance of damping it has been found to be impractical to study arcing grounds and switching transients by the usual methods of mathematical analysis. For this reason it has been found desirable to represent the systems in miniature on the a-c network calculator and to perform the actual switching operations by means of special commutators and this procedure is employed in the study given in part II.

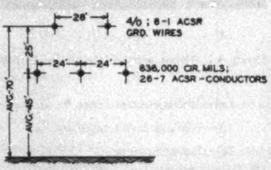


Figure 9. Configuration of transmission line

Part II. A-C Network Calculator Study for a Range of Grounding Conditions on a Typical System

In order to study the magnitude and other characteristics of transient voltages produced by switching operations and faults with intermittent arcing, a typical transmission system was selected for a study on the a-c network calculator. Since these transient voltages are greatly influenced by the method of grounding, the neutral impedances of the system were varied through a wide range of resistance and reactance values, between the limits of the solidly grounded system and the ungrounded system.

The principal characteristics of the system selected for study are given in figure 8. This system consists in general of a hydroelectric generating station, the output of which is transmitted 100 miles over 230-kv transmission lines to a load, which is also supplied by local steam generation. The sending and receiving end transformers are considered to be star-connected on the 230-kv side in order to permit grounding, as discussed subsequently. The reactance characteristics of the different parts of the system are shown in figure 8, and the wire sizes and configuration of the transmission lines are shown in figure 9. It is assumed that the transmission lines are separated so there is no mutual effect between them. Also, the generators at both ends of the line are assumed to be in phase and to have the same internal voltage.

The general method used in setting up a problem on the a-c network calculator has been described in a previous paper.[9] In this method the selected system is set up in miniature on a three-phase basis and the circuit changes are accomplished by means of commutators. These commutators are designed to permit close adjustment of opening and closing a circuit or applying or removing a fault. The transient voltages are measured by a cathode-ray oscilloscope from which records on a film can be secured if desired. Because of the large number of circuit changes required for the representation of intermittent arcs to simulate arcing grounds and switching conditions on a system, it became necessary to provide a larger number of commutators than used in previous studies.[9] Figure 10 shows this equipment together with the measuring and recording apparatus used in the present investigation.

The general method of setting up the network calculator, as previously discussed,[9,10] makes use of equivalent three-phase networks for each circuit element such as machines, transformers, and transmission lines. The character of these equivalent circuits is obvious and requires no comment except for the transmission lines and these are represented by the circuit shown schematically in figure 11.

Figure 10. Switching and recording equipment

Throughout this investigation an attempt is made to obtain the highest voltages for a particular condition. As previously discussed, a slight deviation is made from theories I, II, and III, in order to obtain these voltages. This deviation affects the point at which restriking takes place and this is shifted from the fundamental-frequency voltage crest or the natural-frequency voltage crest to a point to give the maximum transient voltage. In the case of the arcing ground studies the fault is applied at the crest of the normal line-to-ground voltage and is then removed at the first current zero. The point of restriking is adjusted so as to give the maximum voltage for the number of restrikes considered. The fault is always removed at the first current zero following each restrike. In the case of switching operations the circuit is initially opened at a fundamental current zero. The point of restriking is adjusted so as to give the maximum voltage for a given number of restrikes. The subsequent circuit openings are always assumed to take place at the first current zero following the restrike.

In all cases the highest voltages at the point of circuit change are recorded. For example, in the arcing-ground case the voltages are measured at the receiver end. On the other hand, in the case of de-energizing an unfaulted line or the faulted line, the voltages are measured at the sending end, the point at which the switching is actually accomplished. When arcing grounds are considered on the system, several phase voltages as well as the neutral voltage are recorded. In the case of switching operations the voltages are recorded on the phase being switched, both on the line and supply sides as well as across the switch that is opening the circuit.

The voltages recorded are those that take place within $1^{1}/_{2}$ cycles from the first interruption considered. In some cases, either on account of system loss or on account of the relation of the natural frequency to the fundamental frequency, higher voltages may be experienced with one or no restrikes than with two or one restrikes, respectively. In some cases, particularly in the Petersen-coil case, the voltages after the $1^{1}/_{2}$ cycle period may continue to increase to a steady-state voltage of much higher value. In this connection it should be pointed out that with a Petersen-coil grounded system quite high voltages are obtained if the circuit is in tune at fundamental frequency and a residual voltage is produced as by some unbalance. For example, the opening of one phase of a system subjected to a three-phase or a line-to-line fault on the phase being opened will produce a steady-state voltage of many times normal.

In this investigation of transient overvoltages produced by switching operations and faults, four principal cases have been selected for study as follows:

1. Arcing-ground conditions on one phase to ground.

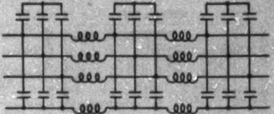


Figure 11. Equivalent network used for representing each 230-kv transmission line of figure 8

2. De-energizing an unfaulted line, one pole unit opening and the other two remaining closed.

3. De-energizing an unfaulted phase with a ground fault on one of the other phases, one pole opening and the other two remaining closed.

4. De-energizing an unfaulted phase with a ground fault on the two other phases, one pole opening and the other two remaining closed.

In general, arcing-ground conditions are for a fault on one phase. De-energizing of a line section is considered more important than energizing because for the latter the intermittent arcing is limited in duration by the closing of the switch. In the case of opening the faulted lines it is assumed that the unfaulted phase opens prior to the pole units of the faulted phase or phases. Such an assumption is based on the ability of the switch to recover dielectric strength at a high rate. This assumption tends to give the higher magnitudes of transient voltage. If the pole unit in the sound phase tends to open after the fault is cleared, then the voltages will be similar to de-energizing an unfaulted line. The voltages will range in values between these limits as the time of relative opening is varied. The conditions selected for study illustrate possible circuit-breaker operations on an actual system.

In this study the transient voltages are obtained for the conditions corresponding to both one and two restrikes. This number of restrikes may be taken as the equivalent of a larger number with the earlier restrikes taking place so quickly that they do not contribute much to the voltage magnitude.

One of the variable factors considered in this study of a typical system is the method of system grounding which includes both resistance and reactance values between the limits of a solidly grounded system and an ungrounded system. When the system is considered solidly grounded, the transformer at the sending end is solidly grounded when one line is considered in operation, and the transformers are solidly grounded at both ends when two lines are in operation. In the case of impedance grounding a reactor or resistor of varying ohmic value is con-

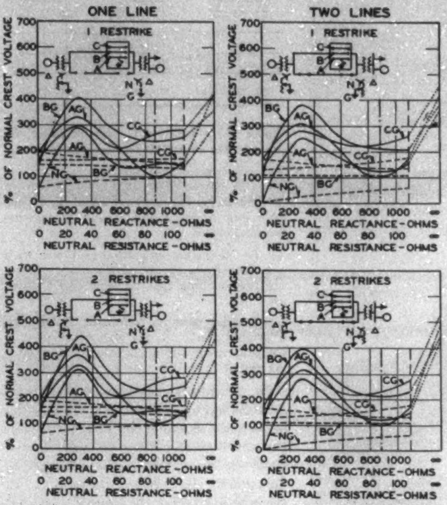


Figure 12. Effect of grounding impedance on transient voltages caused by arcing grounds

Solid curve: reactance grounding

Dotted curve: resistance grounding

Note: Letters on curves refer to lettered points on inset circuit

Petersen-coil reactance: 875 ohms

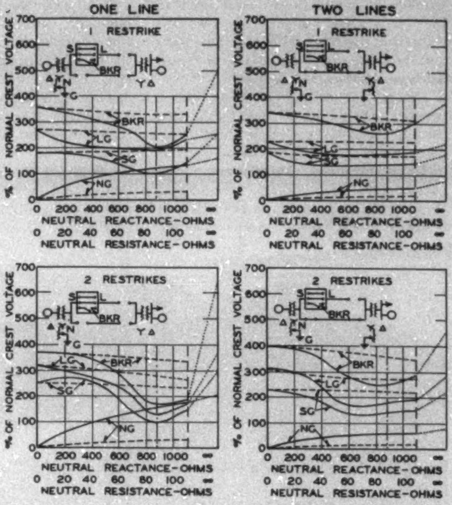


Figure 13. Effect of grounding impedance on transient voltages caused by de-energizing an unfaulted line

See subcaption of figure 12

sidered in the neutral-to-ground circuit at the sending end when one line is considered in operation, and a reactor or resistor of equal value is considered in the circuit in the sending and receiving ends when two lines are in operation. The ohmic values plotted on the figures to be discussed later are the actual values considered in the ground connection at one point. For example, 50 ohms on a system with one ground point is the value considered in the sending end ground. When two lines are considered in operation, 50 ohms corresponds to the ohmic value in the sending-end neutral connection and a like value in the receiving-end neutral connection.

The results of the a-c network calculator study are presented in graphical form in figures 12 to 15 inclusive. These figures give the transient voltages expressed in percentage of the normal line to ground voltage crest and are plotted as a function of the reactance or resistance in the neutral connection. The solid-line curves are for reactance grounding and the dotted-line curves are for resistance grounding. The value of neutral reactance which corresponds to Petersen-coil grounding is indicated. In each of these figures the data are plotted for one and two lines and one and two restrikes.

Discussion of the Results of A-C Network Calculator Studies

The limited range of previous investigations has made impossible a comparison with the present investigation except for the arcing-ground condition on the ungrounded system. For this case, results of the present study given in figure 12 show maximum voltages on sound phases of 5.2 times normal line to neutral voltage and 4.9 times normal as the corresponding value for the faulted phase. These figures are somewhat higher than the limiting values given by Peters and Slepian[7] for theory II and are comparable with their values for theory III. It is to be noted that the maximum values given in this study are based on two restrikes while the values given by Peters and Slepian are the maximum values for an unlimited number of restrikes. Peters and Slepian concluded from their investigation that the most probable mode for an arcing ground was in accordance with theory II. This conclusion is not contradicted, but the present investigation does establish the fact that higher voltages are possible with intermittent arcing in accordance with modifications of theories I and III.

Figure 12 brings out the fact that high transient voltages may be avoided by the use of the solidly grounded system or the Petersen-coil grounded system, both of which have been employed for many years to avoid the abnormal voltages encountered on ungrounded systems. The voltages corresponding to resistance grounding are of fairly uniform and relatively low value for the range of resistance studied. However, it is to be noted that for high values of neutral resistance approaching infinity, the transient voltage values will approach those of the ungrounded system. The study brings out in striking fashion the fact that there is a value of reactance intermediate between the solidly grounded system and the Petersen-coil grounded system for which the transient voltages are almost as high as with the ungrounded system. This value of neutral reactance is approximately one-third of the Petersen-coil value. A similar relation has been found in a study on a lower-voltage system.

Examination of figure 12 indicates that arc suppression with the Petersen coil may be obtained for some deviation from the tuned value, which is in accordance with operating experience. The voltages on the faulted phase, given in figure 12, for

Petersen-coil grounding, do not show a marked change in magnitude as the reactance is varied in proximity to the tuned value. It is of further interest that the magnitudes of transient voltages are higher for two restrikes than for one restrike, and that there is no appreciable difference between these voltages for one and two lines in service.

The transient voltages resulting from the de-energizing of an unfaulted line are shown in figure 13. The most striking feature of this figure is the fact that the lowest transient voltages, with the exception of those across the neutral impedances, are obtained with Petersen-coil grounding. In all cases the neutral-point voltage increases with increasing values of neutral impedances. For the range of practical values of neutral impedance, there is no appreciable difference between the voltages obtained for the case of one and of two lines. However, in the case of a free neutral system the voltages are appreciably lower for the larger amounts of connected line. Again in these studies it is to be noted that the magnitude of transient voltages increases for both one and two restrikes.

Figure 14 shows the transient voltages for the condition of de-energizing a line section with a fault on a phase other than that which is being switched. It is of interest to note that the voltages in all cases of reactance grounding increase from the solidly grounded case to the free neutral case. The voltages between neutral point and ground also increase for resistance grounding as the magnitude of the resistance is increased. It is to be noted that the voltages for the Petersen-coil grounded system are definitely higher than for any of the lower values of reactance grounding. This is to be contrasted with the dip in the voltage curves of figures 12 and 13. In figure 14 there is a definite increase in the voltages with two restrikes as compared to the case with one restrike. As would be expected, the greater the amount of line connected, the lower the magnitude of the transient voltages encountered.

Figure 15 shows the results of a study similar to that of figure 14 except that a double instead of a single line-to-ground fault is applied to the line section being de-energized. In general, the comments are the same as for the case of figure 14. For reactance grounding the transient voltages increase very rapidly for a relatively small addition of neutral reactance, so that for a very nominal amount of neutral reactance the transient voltages closely approach those of the free neutral system. In this case the voltages experienced with the Petersen-coil grounded system are practically the same as for the free neutral system.

It should be emphasized that the results obtained in the a-c network calculator studies are based on a definite number of restrikes which are spaced at such intervals as to give the maximum voltage for this number of restrikes. Thus, in the average case, since the restrikes may not

Figure 14. Effect of grounding impedance on transient voltages caused by de-energizing line with single line-to-ground fault

See subcaption of figure 12

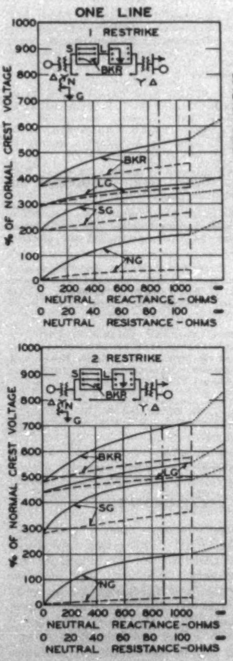
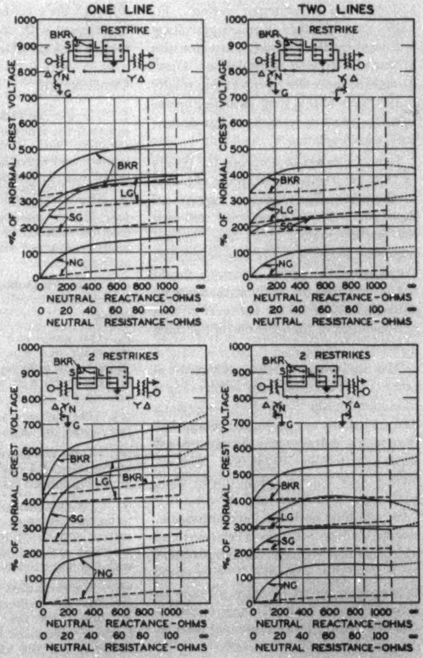


Figure 15. Effect of grounding impedance on transient voltages caused by de-energizing line with double line-to-ground fault

See subcaption of figure 12



occur at the optimum point, the voltages will be of lower magnitude giving a probability curve for the voltage. It is, of course, appreciated that only a minority of the cases of system faults and switching produce abnormal voltages.

The a-c network calculator studies have also been based on the assumption that the transient voltages of increasing magnitude may be impressed on the system without in any way changing the characteristics of the system. Actually, the transient voltages will be limited by other factors which become of increasing importance as the magnitude of transient voltage increases. On some systems the effect of corona will importantly limit the magnitude of transient voltage by introducing losses in the oscillating circuits. Under some conditions excess voltages will produce increases in exciting current particularly at the lower frequencies, but this factor is ordinarily unimportant. Transient voltages may also be limited by the operation of lightning arresters or protective gaps which are adjusted so as to operate below the flashover level of line or apparatus insulation. The presence of these devices may importantly limit the magnitude of transient voltages encountered on a particular system. Finally, the magnitude of transient voltage is limited by the flashover characteristics of line and apparatus insulation. Operating experience does support the results of this study in that some switching operations do result in flashover of line or neutral point insulation.

It is of interest to compare the results of this investigation with those obtained in the field. The maximum voltages of figures 12 to 15 correspond very well with the limiting voltage of six times normal indicated in figure 1. It is believed that the shape of the curves of figure 1 should not be accepted too freely as these are no doubt influenced by the flashover of lines or apparatus, or the operation of lightning arresters.

The study shows when Petersen-coil grounding is used that the voltages are a minimum for the cases of arcing grounds and switching on unfaulted lines, but that higher voltages may be experienced when a faulted line is switched. This coincides with the conclusion drawn some years ago by Oliver and Eberhardt,[11] "All switching, both hand and automatic, should be done with the (Petersen) coil out of service—namely, with the system neutral solidly grounded." Consideration is being given to relay schemes to accomplish automatically this result by short-circuiting the Petersen coil prior to the opening of a faulted circuit.

That more equipment failures have not occurred may be explained by the fact that the voltages have been limited by flashover at a weak point on the system. This might be at one of several pieces of equipment, or, in most cases, at a lightning arrester that operates. In cases where there is considerable energy in the oscillation, particularly on the larger systems, the phenomenon may even cause the failure of a lightning arrester.

If the maximum voltages appeared on apparatus, it might be severely stressed as the voltages are in excess of the 60-cycle tests applied to apparatus. The severity of this will depend on the breakdown characteristics of apparatus at higher frequencies, a point which needs further investigation.

The results of this study should be of assistance in selecting the voltage class of insulation for the transformer neutral and the Petersen coil. For all cases of switching, the neutral voltages for the ungrounded system are substantially the same as for the Petersen-coil grounded system. However, for the arcing-ground case, the neutral voltages are considerably higher with an ungrounded system than with a Petersen-coil grounded system, although these voltages are less than those experienced with an arcing ground on an ungrounded system.

Conclusions

1. The a-c network calculator has made practical the study of transients caused by switching and faults, including arcing grounds and other intermittent arcs, for a broad range of grounding conditions.

2. This investigation shows that

(*a*). Higher maximum transient voltages may be obtained by modifications of previously advanced theories of intermittent arcing grounds.

(*b*). Theories proposed for arcing grounds are applicable to switching with intermittent arcing.

3. The results of a study of the effect of grounding on a typical transmission system, subjected to different conditions of switching and faults with intermittent arcing, as presented in part II, show:

(*a*). The highest transient voltages are obtained with the ungrounded system.

(*b*). These voltages may largely be avoided by the use of the solidly grounded or Petersen-coil grounded systems.

(*c*). For one value of neutral reactance intermediate between the solidly grounded system and the Petersen-coil system, the transient voltages are about as high as with the ungrounded system.

(*d*). The transient voltages for arcing grounds and de-energizing unfaulted lines are lowest for the Petersen-coil system; however, unless the Petersen coil is short-circuited for the cases of de-energizing faulted lines their transient voltages are relatively high.

(*e*). In general, the lowest transient voltages are obtained with the solidly grounded system.

4. The method of investigation and the results of the study on a particular system are held to be pertinent to the problem of determining the voltage class of neutral point insulation on impedance-grounded systems.

5. The results presented in this study are believed to provide an explanation for some of the line and neutral point flashovers, multiphase faults, and arrester failures that have been experienced on actual systems.

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Discussion

D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper represents a valuable addition to the literature on voltage recovery transients. The authors by calculating show the possibility of generation of overvoltages from four to eight times normal due to switching. The question naturally arises whether the assumptions giving rise to these high overvoltages are valid. The figures given rest upon the premise that two restrikes will take place under the worst possible conditions. If no restrikes occurred, these overvoltages would not be generated. If, on the other hand, more restrikes occurred even higher overvoltages might occur. Citation of a few examples of restriking not accompanied by overvoltages hardly proves that such overvoltages would not occur were the assumptions of the paper not realized. The important conclusion to draw from the paper would therefore seem to be that circuit breakers should be selected which have a minimum tendency to restrike.

C. L. Gilkeson (Edison Electric Institute, New York, N. Y.): The use of the a-c network calculator for studies of transients, as developed by Evans and Monteith, has opened the way to an understanding of hitherto obscure phenomena. The present paper, dealing with the relation between transient overvoltage and neutral impedance, is most interesting. At the same time it emphasizes the need for more knowledge in other related fields in order that the significance of the paper may be properly evaluated.

In the discussion of figure 7, the authors have clearly stated the characteristics of the dielectric recovery in the arc, with respect to the recovery voltage, which permits the production of voltages of the type they are here discussing, namely, that the dielectric strength increases between successive restriking of the arc. It seems reasonable to find that such a condition does exist in a circuit breaker, where the length of the restriking path increases with time. Arcs across insulators would not be expected to have such characteristics. A question of importance is whether faults in confined space, such as cable failures, bushing failures, or arcs under oil, in expulsion tubes, or expulsion fuses may have such characteristics. If the condition in the arc, necessary to cause high transient voltages, can occur only during the opening of a breaker the problem takes on one aspect, since its duration is limited, however, if it may occur during an arc in a confined space the problem takes on an entirely different aspect.

In the study of overvoltages, conducted by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell System, a number of records have been obtained for faults in confined spaces, such as cable failures, bushing failures, and arcs under oil. These records were obtained from operating systems and were for the most part from automatic magnetic oscillographs, supplemented in one case by an automatic cathode-ray oscillograph. The systems included an extensive 140-kv isolated neutral system, a 140-kv system equipped with Petersen coils, a 26-kv system with a 75-ohm neutral resistor, and a 44-kv system with a 150-ohm neutral resistor. For faults in confined space, many of the records showed that during a part of the disturbance, the arc was extinguished and restruck, every half cycle, every cycle, or at irregular intervals. In all cases the restrike appeared to be controlled by the fundamental-frequency voltage, occurring at or near the crest of the voltage wave. In no case did the restrike occur at a higher voltage than the normal crest value. In most cases the extinction of the arc appeared to take place at the current zero of fundamental frequency but in some cases at a current zero of higher frequency. An oscillogram illustrating the conditions for a fault in a confined space is given in a paper by Jeanne and Gilkeson, AIEE Transactions, volume 53, 1934.

There have been a few instances where intermittent extinction and restriking of an arc might have been associated with an arc in open air. However, the condition is so rare that it is probable that if all the facts were known it would have been found that these few cases were also associated with faults in confined space.

To summarize the results of these observations, it appears that intermittent arcing of the type discussed does not occur across insulators in open air but may occur for arcs in confined space. While the data do not indicate an increase in dielectric strength between successive arcs they do not preclude this possibility. A study of the dielectric recovery for arcs in confined space, such as cables or bushings or in expulsion tubes or fuses, appears to be warranted.

A point of considerable interest in the paper is the relation between overvoltage and neutral impedance. One important conclusion of the paper is that under certain conditions a Petersen-coil system, badly out of tune, might be subjected to very high transient overvoltage. However, for moderate values of neutral impedance, 100 ohms resistance (the maximum considered in this paper), or 100 ohms reactance, the curves of figures 12 to 15 indicate that the overvoltage is little different from the condition of no impedance in the neutral. It should be recognized that a 100-ohm neutral impedance, on the 230-kv system represented, is equivalent to a much lower impedance on a system operated at a lower voltage.

Figure 14 shows an overvoltage of about 4.4 times normal, for a solidly grounded neutral for two restrikes on a single line. This value of overvoltage is near the upper value of overvoltages recorded for switching surges, and helps to explain why there is no consistent relationship between the observed switching transients and the method of operating the system, whether with impedance in the neutral or solidly grounded.

P. A. Jeanne (Bell Telephone Laboratories, Inc., New York, N. Y.): Due to my contact with the study of problems involved in fault-current limitation, I have found the papers relating to transient overvoltage, presented at this session, most interesting. The authors deserve a great deal of credit for their contributions to a difficult subject. My comments, while presented specifically in connection with the Evans, Monteith, and Witzke paper, also refer in general to the other papers on this subject. Mr. Gilkeson in his discussion, has referred to the data on overvoltages obtained by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell System which tend to substantiate the idea expressed in the Evans, Monteith, and Witzke paper that an arc in air is unlikely to go through the sequence of extinction and restriking required for the cumulative building up of overvoltage according to the arcing-ground theory. I want to add a few comments based on these same data. The oscillograms from the resistance grounded systems and isolated system, referred to by Mr. Gilkeson, confirmed the possibility of some restriking during the opening of breakers which clear a faulted line. For the systems studied, the restriking appeared to be confined to the faulty phase, and usually occurred before the recovery voltage had reached a value equivalent to the crest of the normal-frequency wave. The resulting overvoltage on the sound phases following the restriking did not exceed that to be expected from the simple initiating transient.

An examination of the oscillograms of breaker-cleared two-phase-to-ground faults obtained from the Petersen-coil system over a period of five years showed overvoltages of about the same magnitude as those from the one line-to-ground self-cleared faults, indicating that the clearing sequence requisite for producing the higher voltages is likely to be of very infrequent occurrence.

With regard to the Clarke, Crary, and Peterson paper, I should say that the oscillograph observations tend to substantiate the conclusion reached therein, namely, that the maximum voltage generally will be in the order of that determined by the simple initiating or clearing transient. This was the order of magnitude of the maximum voltage recorded by oscillographs capable of recording up to around 3,000 cycles. Surge recorders, where used, tended to indicate considerably higher maximum voltages than the oscillographs, except in one case where a capacitance-coupling potentiometer in place of the usual string-insulator potentiometer was used to connect the surge recorder to the line. Since many of the surge-recorder records include the effect of lightning, as well as the transients due to a fault itself, it has been found difficult to determine correctly the magnitude of the overvoltage due to the fault alone. By discounting portions of a surge-recorder figure which appeared to be due to lightning or similar steep-wave-front phenomena, it was found that the maximum voltages deduced from the surge recorders tended to be from 30 to 40 per cent higher than shown by the oscillographs. It seems difficult to reconcile these differences, particularly as in a recent installation a good check between a magnetic oscillograph, a cathode-ray oscillograph, and a surge recorder, was obtained on the system where the capacitance potentiometer was employed to couple the latter two types of instruments to the line.

These papers indicate a marked advance in the analytical and laboratory testing technique for determining overvoltages. On the basis of these studies it should be possible to choose a system or systems having the characteristics most likely to contribute to high overvoltage, install automatic cathode-ray oscillographs, and determine whether the high voltages are realized.

C. Concordia, E. M. Hunter, and **H. A. Peterson** (all of General Electric Company, Schenectady, N. Y.): During about the last 20 years, many papers discussing the effects of restriking of the arc in a fault or interrupting device have appeared. Some of these have presented test results, but most have proposed theories which could be fully developed and applied only in an approximate form. In addition, the fundamental assumptions of these theories regarding system behavior were on a rather insecure basis. The present paper is particularly valuable since it corrects the first fault mentioned above and presents the results of a following out of a physically consistent restriking procedure, with damping taken properly into account.

The second item of whether or not the assumptions are sufficiently representative must be determined by comparison with operating experience and with test results. However, at least one of the assumptions (that, in case of clearing of faulted lines, one of the unfaulted phases clears first) appears very likely to be correct except in the case of single-line-to-ground faults on systems grounded through Petersen coils. In such systems, for line-to-ground faults, breaker action should not ordinarily be required at all because clearing should be accomplished by the Petersen coil. In case the fault is a solid one, and breaker action is required, the faulted phase might clear first. If the faulted phase does clear first in the particular system investigated, voltage will not be built up across the switch to any great degree for either resistance or reactance grounding, and voltages on the unfaulted phases will also be much lower than for interruption of the unfaulted phases. The reason for this is evident when the natural frequency or frequencies of oscillation of the system investigated are considered. For low natural frequencies it is not possible to build up high voltages due to restriking in the faulted phase because of the limited number of restriking-reclearing cyles that can take place before the fundamental component of voltage (which is the changing axis of oscillation of the natural-frequency components) passes through zero. In fact, if the natural frequencies are sufficiently low, the magnitude of the recovery voltage across the breaker following the first restrike and reclearing may be even lower than that following interruption at normal current zero.

The condition of restriking at the maximum point of each preceding recovery-voltage wave is of course seldom realized in practice. Instead, with certain types of interrupting devices, restriking might occur at lower voltages, but on the other hand, might continue for several cycles, with all phases restriking in case of interruption of line charging current. The magnitudes of voltage indicated by the curves of this paper are thus to be considered primarily as indications of the *relative* severity of different methods of grounding. Similar restriking studies which we have made on systems indicate that the voltages are about the same for either resistance or reactance grounding of the same ohmic magnitude, except for the dip in the curves with reactance grounding for certain cases.

It is important to bear in mind also that the results of this investigation apply only to a specific system. Similar switching procedure carried out on different systems has indicated that, in particular, neutral voltages are considerably affected by the amount of capacitance interrupted relative to the total system capacitance.

The authors conclude that all switching on Petersen-coil systems should be done with the Petersen coil short-circuited, and state that this conclusion is in agreement with that of J. M. Oliver and W. W. Eberhardt (authors' reference 11). It should be pointed out that the overvoltages discussed by Oliver and Eberhardt arose chiefly from series resonance. This source of overvoltage has been effectively reduced, in all subsequent Petersen-coil installations, by the use of iron cores instead of the air-core reactors used in Oliver and Eberhardt's application. Moreover, in the operating experiences to date on 22 applications of coils in the United States, extending over several years of operation, there has, to our knowledge, so far been no indications of dangerous overvoltages.

E. W. Knapp (The Shawinigan Water and Power Company, Montreal, Que., Canada): The papers which have been presented are of special interest to the power company operating long high-voltage transmission lines. It might be of interest to mention at this time a few instances of overvoltage during switching. The system under consideration consists of a double-circuit steel-tower line with metallic bussing at the receiving end only. The lines are 135 miles in length and operate at 165 kv. The insulation consists of 10 and 12 units; there are two overhead ground wires and some buried counterpoise. At the receiving end of the line there is an automatic oscillograph with a sensitivity of about 5,000 cycles. Voltage records are taken from line potential transformers and current records are taken from bushing-type current transformers on the line oil switches.

During the past few years there have been recorded a number of cases of momentary overvoltage. In some cases the sound line became involved at about the time that the initially faulted line cleared. In one case during 1938 during a line-to-ground fault on phase C, voltages of 425 kv and 660 kv crest were recorded on phases B and A to ground, respectively. In another case during a line-line-to-ground fault on B and C phases, successive increases of voltage occurred on phase A until a crest value of 720 kv was reached before either flashover or clearance. Both of these troubles were during lightning and both lines were eventually involved.

Perhaps the most interesting operation occurred due to a piece of wire creating a line-to-ground fault on phase B on one line about 18 miles from the receiving end. At the instant of clearing at the receiving end, a crest voltage of 320 kv was recorded on phase C which flashed over. The sending end of the line was cleared a few cycles later throwing 174,000 kw on to the sound line. The faulted line was then automatically returned to service with practically no loss of load and without additional flashover. There was no lightning during this trouble.

R. D. Evans, A. C. Monteith, and **R. L. Witzke**: Mr. Prince has commented on the assumptions on which our study of transient voltages due to switching operations were based. He has indicated that if there are more than two restrikes higher transient voltages than those given in the paper might be encountered. The magnitude of transient voltages to be expected is not to be increased in direct proportion to the number of restrikes that take place. While several restrikes may take place, the effect on the transient voltages as indicated by klydonograph studies will not exceed that of two restrikes occurring at such intervals as to produce maximum cumulative action. This is due to several factors including the probable sequence of opening of the pole units in an actual breaker, as pointed out in the paper. The spacing between restrikes is fully as important as the number of restrikes and it is misleading to consider the total number of restrikes alone since without proper spacing the action will not become cumulative.

Mr. Prince has also raised the question as to the characteristics of breakers that are desirable from the standpoint of minimizing transient overvoltages. If restriking is wholly avoided, cumulative action is prevented. However, it is undesirable for a breaker to have such characteristics as to force current zero or such as to operate with a high extinction voltage. High dielectric recovery strength, low extinction voltage, and nonforcing of current zero are, to an extent, conflicting considering the full operating range of a breaker. In the present state of the art the emphasis, in our opinion, should be placed on the circuit condition. The principal significance of our paper has to do with the selection of the circuit condition for minimizing transient voltage, as for example, by the use of a solidly-grounded instead of a reactance-grounded system, etc.

Gilkeson and Jeanne have reviewed the work on transient overvoltages which has been done by the Joint Subcommittee on Development and Research of the Edison Electric Institute and Bell Telephone System. This work has been of great value and confirms the thought expressed in our paper that arcs over insulators in air rarely produce transient overvoltages, but that the conditions of arcing in a confined space, as for example, in the failure of bushings or cable insulation, are more favorable for cumulative action by intermittent arcs.

Mr. Gilkeson has commented on the fact that the records obtained on cable and bushing failure showed intermittent arcing with restrikes occurring every half cycle, every cycle, or at intervals, but that these do not show that restriking has taken place at greater than normal voltage. However, it is to be noted that the dielectric recovery characteristic of the arc path varies from a value such that breakdown occurs at normal voltage or less when the arc restrikes every half cycle, to a value which is nearly twice normal voltage for the condition in which the arc is interrupted at a current zero and restrike does not take place within a half cycle. Thus the dielectric recovery strength varies from a value somewhat less to a value somewhat more than that necessary to produce cumulative action. This would mean that there should be a tendency under some conditions to start cumulative action. Mr. Gilkeson has pointed out that evidence of this action has not been obtained although the data does not "preclude the possibility of such action."

Mr. Gilkeson has also pointed out that the condition for cumulative action is of very short duration if it arises from a switching operation but may be of long duration if it arises from an intermittent arc or incipient

Influence of Resistance on Switching Transients

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MEMBER AIEE

THE voltage recovery transient appearing across the terminals of a circuit-interrupting device has been the subject of numerous studies during the past ten years. Their importance has justified these studies because they determine in part the severity of the duty impressed upon circuit-interrupting devices. These studies have consisted of mathematical analyses of circuit conditions, calculating board investigations and studies of oscillographic records of the actual transients, as they are obtained on systems and in high-power laboratories.[1]

The damping of the voltage recovery transient, is a function of the resistance of the circuit. It depends not upon the absolute value of the resistance, but upon the value of the resistance relative to the inductances and capacitances. Important variations in the transients can be produced by changing the values of the resistances.

For the consideration of complicated systems, use may be made of the a-c network calculator as discussed in another paper, "Power-System Transients Caused by Switching and Faults" by Messrs. Evans, Monteith, and Witzke (AIEE TRANSACTIONS, volume 58, 1939, pages 386–97). The calculating board can be set up to represent the capacitance and inductance relations and if these are chosen with proper respect to the resistances of the circuit, an approximation of the damping will be obtained.

In mathematical studies, the inductances and capacitances of the various parts of the circuit are determined or estimated and from them the voltage recovery transients are calculated. Except in cases more simple than are usually encountered in system studies, the resistances of the circuits cannot be included because of the added complication of the mathematical expressions. Consequently, the rate of damping of the transient is assumed on the basis of experience and a general knowledge of the effect of resistance is essential in these studies.

The damping of voltage recovery transients can be studied in relatively simple circuits which contain resistance, inductance, and capacitance. These transients can be calculated and the rate of damping determined. The mathematical expressions for these circuits are relatively complicated but a graphical representation of the transients can be used to demonstrate the relations.

This paper presents graphically the effects of resistance on the closing and opening transients of inductive and capacitive a-c circuits. Typical simple circuits are assumed and the transients corresponding to various values of resistance are shown as curves. They demonstrate clearly the effect of resistance on switching transients.

The transients are described for single-phase circuits having a voltage source uninfluenced by the transients.

Closing Transients of Inductive Circuits

When an a-c circuit containing resistance and inductive reactance is closed by a switch or when the resistance or reactance is altered, the steady-state condition of the system is changed and usually a current transient exists before the new steady-state is established. This is caused by the difference in magnitude of the instantaneous values of the steady-state currents before and after the instant the switch is closed. Since the current in the circuit cannot change instantly to the new value, a transient current component of amplitude equal to the difference is superimposed on the new steady-state current and decays as an exponential function of time. Such transient currents are shown in figure 1 and figure 2 and can be expressed by equation 1 given in the appendix.

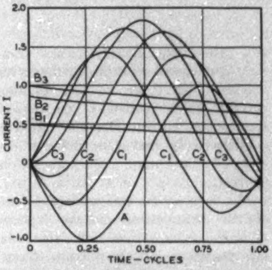

Figure 1. Effect of time of closing on asymmetry of current in an inductive circuit

If a circuit is closed at a time corresponding to a normal current zero, current is established without any transient as shown in figure 1 by the curve marked *A*. This current is symmetrical and contains no transient component. (Transients in the symmetrical component of current due to demagnetization of the generators are not considered in this paper.)

When the contacts close at a time other than a normal current zero, a transient component of current flows

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Paper number 39-51, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 9, 1938; made available for preprinting January 3, 1939.

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[1] See bibliography at end of paper.

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fault in a cable or other confined space.

Concerning the discussion by Concordia, Hunter, and Peterson, it is to be pointed out that from the standpoint of overvoltages produced by switching on the Petersen-coil-equipped system as discussed in the paper, there would be no appreciable difference caused by the substitution of an iron-core reactor for an air-core reactor. This is due to the fact that the magnitude of overvoltage produced by switching with intermittent arcing is not critical with respect to the tuned value of Petersen-coil reactance.

This method of producing overvoltages with a Petersen-coil system is not to be confused with that involving resonance. The latter effect was encountered in the investigations reported in the paper for certain fault and unbalanced circuit conditions such as produced by the opening of a breaker pole. In a symmetrical system high voltages should not be produced by series resonant action except as a result of some switching operation. Since switching operations may in themselves produce high transient voltages as a result of intermittent arcing it is not necessary, as Concordia, Hunter, and Peterson have done, to postulate both a switching operation and a series resonance for the unbalanced condition to account for high transient voltages.

E. E. Knapp has cited a case in which transient voltages greater than those corresponding to a simple break have been experienced. This supports the assumptions made in the study reported in our paper.

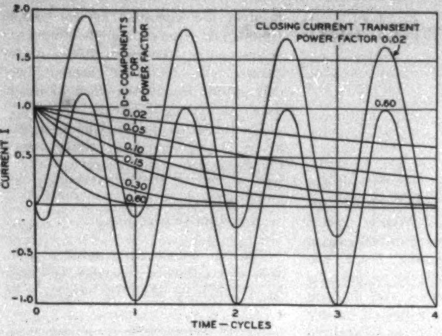


Figure 2. Effect of power factor on d-c component of closing current transient

and decays exponentially. These transient components are shown by the curves marked *B* in figure 1. The resulting current, which consists of the normal symmetrical current plus the transient component, is shown for different degrees of asymmetry by the curves *C* in figure 1.

The decay of the transient component of the asymmetrical current depends upon the value of the inductance and the resistance of the circuit. These same two elements, the resistance and inductance, determine the power factor of the circuit. Consequently the decay of the d-c component varies with the power factor of the circuit. The higher the resistance relative to the inductance; the higher the power factor of the circuit and the more rapidly the transient component decreases. This is shown in figure 2. The d-c components for fully asymmetrical waves are plotted for circuits having power factors from 0.02 to 0.60. The total currents including both the a-c components and the d-c components are plotted for two power factors. These curves show that in circuits having a low power factor, the asymmetry of the current will be maintained for a relatively long time. This becomes important in some current transformer and relay applications, because of possible saturation of the magnetic circuit. At the other extreme, the transients decay very rapidly; at 60 per cent power factor, the transients disappear at the end of one cycle.

These relations are important in the application and testing of circuit breakers. A high-speed circuit breaker operating on a low-power-factor circuit will interrupt the circuit before the d-c component has disappeared. A circuit breaker operating on a higher-power-factor circuit, opening after several cycles due to a relay timing or slower mechanical action will open only symmetrical currents.

The international rules for high-voltage circuit-breaker testing are based largely on the performance of circuit breakers that do not open until the d-c component has disappeared. Consequently, for 80 per cent of the tests demonstrating rupturing capacity they specify that the current shall have a direct component not greater than 20 per cent of the crest value of the alternating component. On test circuits having very low power factors, this value is not obtained until 10 to 14 cycles after the beginning of the short circuit. The curves in figure 2 show that by inserting additional resistance in the test circuit to raise the power factor to the maximum permitted by the rules (0.15) the time required to reach 20 per cent asymmetry can be reduced to only 1.6 cycles. A brief duration of the short circuit during testing is desirable because it minimizes the demagnetizing effect and results in less decay of the alternating component of current and in a higher restored voltage.

Testing under American rules is conducted with both symmetrical and asymmetrical currents up to the breaker rating or over the range of power available. Consequently in American laboratories, low circuit resistance is desirable to give a slowly decreasing d-c component.

Closing Transients on Capacitive Circuits

When a capacitive circuit is energized by the closing of a circuit breaker, a transient occurs which can cause an overvoltage on the system. The capacitance, if composed of relatively short cables or lines, so that the inductance is negligible, can be assumed to be lumped. The circuit which energizes it will have inductance, resistance, and capacitance. In the circuit shown in figure 3*a* it is assumed that the capacitance, *C*, to be energized is lumped and the capacitance, C_1 of the rest of the circuit is negligible.

When the arc strikes, the voltage at the switch will become ground potential and a voltage drop will appear across the inductance because the capacitance *C* is assumed to be uncharged and at ground potential. The inductance and capacitance of the circuit, connected by the striking of the arc, produce an oscillation with a possible crest value equal to twice the normal crest voltage. The resistance damps this circuit but its effectiveness depends upon its magnitude relative to L/C. The voltage recovery transient for several values of resistance are plotted in figure 4. The smallest value of *R* is comparable with those obtained in high-power laboratories. For resistance values less than the critical resistance, $R = 2\sqrt{L/C}$, the voltage transient is defined by equations of damped oscillations. For resistance values greater than the critical value, the voltage transients are defined by exponential equations and curves. The equations of these transients are numbers 2, 3, and 4 of the appendix.

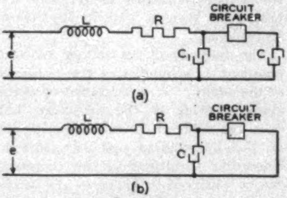


Figure 3

(*a*)—Energizing a capacitive circuit

(*b*)—Opening a simple inductive circuit

In the closing of a high-voltage circuit by a conventional circuit breaker, the arc will strike before the contacts touch and the arc resistance will exert some damping effect on the transient. However, even with the maximum crest occurring with minimum damping, no dangerous voltage would be impressed on the system by the closing transient of this type.

In the curves of figure 4 it has been assumed that C_1 was negligible with respect to *C*. This causes the voltage transient to start at zero because the capacitance C is uncharged at the beginning of the transient. If C_1 is large enough to raise the potential of the capacitance C when the two are connected together by the closing of the breaker, the transient will start not from zero but from the potential taken by the capacitances. The amplitude of the transient is no longer the system potential at the time the arc strikes, E_m, but is the difference between E_m and the potential of the capacitors. The capacitance used in the formula for determining the transient is, of course, the sum of the capacitances C and C_1.

During the closing of a high-voltage breaker, it is possible for the arc to be extinguished and to restrike before the breaker is completely closed. This phenomenon may produce higher voltages because the line may be charged to a high potential by the first transient and the amplitude of the second transient be increased thereby. However, the conditions on closing are not favorable for the building up of high overvoltages since the gap between contacts is becoming smaller. The phenomena is the same during the interruption of charging currents and more favorable conditions exist when the contacts are separating. Therefore this phenomenon will be described in connection with the interruption of charging currents.

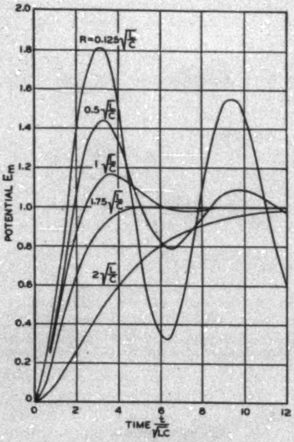


Figure 4. Voltage transients

Closing of capacitive circuit
Opening of inductive circuit

When the capacitance is distributed over a long line it cannot be represented as a single lumped capacitance and more complicated circuits must be used. The principles are the same but the possible combinations are too numerous to consider here.

Opening Transients of Inductive Circuits

The opening of a short circuit or the opening of the circuit energizing an unloaded transformer may be considered as the opening of an inductive circuit. These currents lag the applied voltage by relatively large angles and consequently the voltage appearing across the contacts after the interruption is approximately the crest value of the applied voltage. The small capacitances of the circuit, which play no part in determining the current, are important in determining the voltage recovery transient. Likewise, the resistance of the circuit, which was negligible while the current was flowing, damps the voltage recovery transient. The resistance may be a small parallel load which is not removed by the opening of the circuit breaker or may be due to the resistivity of the conductors forming the circuit.

SERIES RESISTANCE

Figure 3*b* represents a simple series circuit involving inductance, capacitance, and resistance. Frequently, this circuit is a part of a more complicated circuit and can be considered as a unit in itself independent of the rest of the circuit. The opening of this circuit is mathematically the same as the closing of a capacitive circuit, figure 3*a* and equations 2, 3, and 4. It has voltage recovery transients which are shown in figure 4. When the curves are applied to voltage recovery transients following the opening of a reactive circuit, the time scale will generally represent a shorter interval because the capacitance between the breaker and the inductance usually will be much smaller than the line or load capacitance.

For high-power laboratory circuits having relatively low resistance with respect to the inductance, the damping of the oscillation corresponds roughly with the curve for $R = 0.125\sqrt{L/C}$. Similar system circuits could be expected to have similar transients.

COMBINATION OF SERIES AND SHUNT RESISTANCE

The opening of a circuit by a switch may not completely unload the source, or a circuit breaker may open in two steps, the first one inserting a resistance for limiting the current. Circuits of these types are represented by the diagram in figure 5.

This circuit too may be oscillatory or nonoscillatory depending upon the circuit constants. The critical condition is

$$\frac{R}{L} - \frac{1}{R_1C} = \frac{2}{\sqrt{LC}}$$

The transients are similar to those shown in figure 4 for the simple series circuit.

The equations applying to this circuit are numbers 5, 6, and 7 in the appendix. The curves indicate and the equations show that the voltage transients occurring during the opening of an inductive circuit cannot attain potentials above two times the normal crest value of voltage.

GENERAL

The effects of an arc voltage prior to the last current zero and of the extinction of an arc before the normal current zero have been neglected but are important in reactive circuits. Both of these phenomena increase the amplitude of the transient and tend to raise the potential at the crest. Therefore, the maximum voltage which can be reached depends upon the characteristics of the circuit breaker and upon the circuit. A review of a large number of oscillograms of single-phase interrupting tests on high-voltage circuits showed no voltage in excess of 2.75 times the crest value of line-to-ground voltage and only a few reached 2.25 times.

Opening Transients of Capacitive Circuits

The interruption of a capacitive circuit would take place without a transient if the circuit had no inductance and no resistance, if the arc was extinguished at current zero, and if the dielectric strength of the point of interruption increased to a little more than twice the crest of the applied voltage in a half cycle of the system frequency. A negligibly small transient occurs if the dielectric-strength

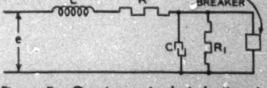


Figure 5. Opening a simple inductive circuit having both series and shunt resistance

condition is fulfilled. On high-voltage circuits, such high dielectric strength is very difficult to obtain at the first current zero so the arc usually restrikes. The restriking initiates a transient which may produce high overvoltages. The circuit and the transients following a restrike are shown in figure 6.

Normally, the resistance is relatively low and the transients, both current and voltage, are oscillatory. On the first restrike, the current will pass through zero at times other than the normal current zero and if extinguished, may leave the capacitance charged to a voltage either above or below the applied voltage. With no resistance in the circuit and the worst possible rate of increase of dielectric strength, the theoretical rate of increase in voltage at successive restrikes would give 1, 3, 5, etc., times the normal crest of system voltage as shown in figure 7. However, this required that the gap after the first interruption (*a*) holds while the voltage across it increases from zero up to twice the normal crest (*a* to *b*), that it breaks down at this value and after a half-cycle of the transient interrupts again (*c*). If the breaker opening leaves the line charged as at *c*, the potential on the source side of the breaker will tend to return to the value of the applied voltage. It will do this through a transient similar to that which occurs on the opening of an inductive circuit, namely a damped oscillation produced by the inductance of the supply circuit and the capacitance still connected to it. This transient is damped by the resistance of the circuit but will reach a value approximately as shown at *d* and impress nearly four times normal crest voltage on the gap. As this transient dies (*d* to *f*) the voltage returns to approximately two times the crest value and increases due to change in the applied voltage from approximately two times to four times the crest value. This is reached at point *g*. To continue the building up of voltage, the arc must now restrike at *g* with little more voltage on it than it had at the point *d*.

Obviously this dielectric performance is almost, if not quite, impossible. Oscillographic data of actual interruptions by circuit breakers show a much more gradual build-up of voltage. In some cases, the breakdowns occur before the crest value of voltage is reached so that the amplitudes of the intermediate transients (*b* to *c*) are reduced. In other cases, the transient current may not be extinguished at its first current zero. If the current flows for two half-cycles of the high-frequency transient, the voltage on the line is reduced to a very low value. The damping of the transient by the resistance of the arc and of the metal conductors in the circuit also tends to reduce the rate of voltage build-up.

The influence of these modifying factors is shown by the oscillograms reproduced in figure 8. These oscillograms were taken during the interruption of capacitive currents in a high-power laboratory. In an oscillogram the line *A* represents the voltage across the terminals of the breaker, the line *B* the current flowing in the circuit, the line *C* the trip-coil current, and the line *D* the voltage on the source side of the breaker. Due to the circuit used, the oscillations in the voltage *D* are a maximum. The large capacitance representing the line was disconnected from the low capacitance of the source. Consequently, on a restrike, the amplitudes are not reduced by the equalization of potentials between them.

Oscillogram *a* shows the interruption of the charging current without any restriking. The voltage appearing across the breaker contacts was approximately two times the crest value of the applied voltage but the arc space did not break down.

Oscillogram *b* shows one restrike after a current pause during which the voltage rose to approximately 25 per cent of the crest value. The restrike was extinguished after a half-cycle of the high-frequency current. The arc did not restrike subsequently although the value of voltage appearing across the breaker contacts was approximately 1.7 times the crest value of the applied voltage.

Oscillogram *c* shows a restrike at a voltage approximating 50 per cent of the crest value of the applied voltage and the extinction after a half-cycle of the high-frequency current. This extinction occurred at approximately a normal voltage zero so that the subsequent voltage applied across the terminals of the breaker did not exceed the normal 60-cycle voltage. The arc did not again restrike.

Oscillogram *d* shows a restrike after approximately one half-cycle of restored voltage during which the potential applied across the breaker increased to approximately two times the crest value of the system voltage. The high-frequency current flowed for three half-cycles before it was extinguished. This left the line with very little charge as indicated by the almost symmetrical voltage wave appearing across the breaker contacts subsequent to the final interruption.

Oscillogram *e* shows the interruption of the charging current followed by a restrike at very nearly two times the normal crest of voltage applied across the breaker. Several cycles of high-frequency current flowed before the breaker interrupted and left the line charged approximately at the crest value of voltage. After a half-cycle of current interruption, the voltage across the breaker had again increased to two times the crest value of the applied voltage, and another restrike occurred. After about seven half-cycles of the high-frequency current, the arc was extinguished, the line being left charged at a potential about half the crest value of the system voltage. The maximum voltage appearing across the breaker subsequent to the final interruption was approximately $1^1/_2$ times the crest value of the system voltage.

Oscillogram *f* shows the best example of building up of voltage across the terminals of the breaker. Three restrikes occurred and on the last interruption, the line was left charged to about 1.85 times the normal line-to-ground voltage and during the subsequent half-cycle of restored voltage, the potential difference across the breaker increased to about 2.85 times the normal line-to-ground voltage.

On a 220-kv system where it was suspected that the interruption of line charging current was producing excessive

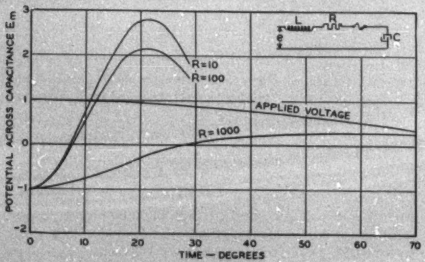


Figure 6. Restriking at crest of voltage wave with capacitance charged to potential of the previous crest

Curves show the effect of resistance on the transient voltage across the capacitance

L = 0.1 henry

$C = 10^{-6}$ farads

overvoltages, tests were made recently with a cathode-ray oscillograph recording the line voltage. Sections of lines up to 246.4 miles were disconnected and the maximum voltage recorded was 2.43 times the crest value of the system line-to-ground voltage. These voltages are harmless and indicate that on a typical system the high overvoltages obtained theoretically by neglecting the dielectric characteristics of the breaker, are seldom encountered.

Resistors in Circuit Breakers

Because of the effects of resistance on switching transients, circuit breakers using resistors have been built for many years. In Europe, prior to 1930, oil circuit breakers either plain-break or explosion-pot types sometimes had auxiliary contacts which inserted a resistance between terminals. The auxiliary contacts closed before and opened after the main or arcing contacts. Due to the relatively short gap between the arcing contacts when the auxiliary contacts were just touching, the probability of the resistance functioning to facilitate opening of the circuit was remote and their main service was in reducing the severity of the transients which occur on closing capacitive and transformer circuits. These resistances damped the inrush current to lines and limited the inrush current when a transformer was energized. The advantages gained hardly justified the additional complication and they disappeared from the market.

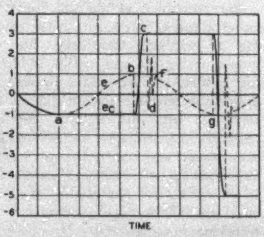


Figure 7. Theoretical transient voltages appearing at the two terminals of a circuit breaker during the interruption of a capacitive current

e = Voltage at the terminal connected to the source

e_c = Voltage at the terminal connected to the capacitance being disconnected

Note the transient *c-d-f* following a restrike which impresses almost as much voltage across the circuit breaker as the normal cyclic voltage change *f* to *g*

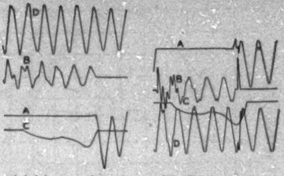
Some modern interrupters are being built with resistors which aid not only in closing circuits but also in the extinction of arcs. They facilitate the interruption of both charging current and reactive current. The resistors damp the transients which occur during the interruption of leading currents and reduce the overvoltages produced. They are generally inserted by being connected across either a part of the gap formed by the arcing contacts or a separate gap. The insertion of the resistor increases the power factor of the circuit, shifts the current zero to a point in the cycle where the applied voltage is less, and reduces the overvoltages which can occur on the system.

In lower-voltage circuits where heavy currents are the problem, resistors are used to facilitate the interruption of short-circuit currents and thereby increase the interrupting ability of the circuit breakers. An arc inserts the resistance which reduces the current, increases its power factor, and thus brings the short circuit within the limits which can be interrupted directly by the arc extinguishing means provided for the complete opening of the circuit.

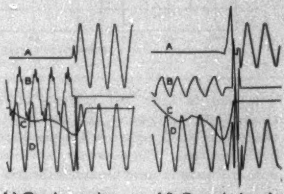
The value of the resistance used depends upon the rated voltage and the rated rupturing capacity of the breaker. The magnitude of the voltage appearing across the gap which inserts the resistance depends upon the system voltage, the reactance of the circuit, and the resistance. The relations are given in the appendix as equation 8. These relations are demonstrated by figure 9 with voltage plotted for values of r/x of 0.1, 1, and 10. With $r/x = 0.1$ the voltage appears across the resistance and arc gaps at a very low rate and reaches only a low maximum value. The arc could be easily extinguished but the resistance makes only $^1/_2$ per cent difference in the total impedance of the circuit and consequently does not aid to any measurable extent in the ultimate interruption of the circuit.

With $r/x = 1$ the voltage appears more rapidly and reaches a value of about 50 per cent of the applied crest on the first peak. The second peak, a half-cycle later and consequently less important, reaches 70 per cent.

With $r/x = 10$ the transient component decreases very rapidly and disappears in about 20 degrees. The voltage rises rapidly across the resistance and inserting gap but does not exceed the crest value of applied voltage.

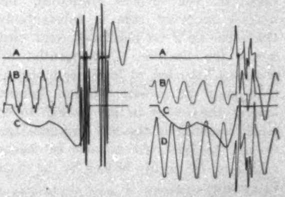


(*a*) No restriking (*b*) One restrike



(*c*) One low-voltage restrike with symmetrical restored voltage across breaker

(*d*) One high-voltage restrike with symmetrical restored voltage across breaker



(*e*) Two high-voltage restrikes

(*f*) An example of building up voltage across breaker

Figure 8. Typical oscillograms of the interruption of charging current at about 22 kv in a high-power laboratory

A—Voltage across breaker
B—Current
C—Trip-coil current
D—Voltage line-to-ground on supply side

Note that none of the oscillograms show the theoretical rate of voltage increase given in figure 7

The impedance of the circuit would be increased about ten times and consequently the current to be interrupted by the second gap is reduced to about ten per cent of the full short-circuit current.

Still further reduction in the current can be obtained by higher values of r/x. However, the effect of capacitance across the gap becomes important when the current flowing through the resistor becomes comparable with the current required to charge the capacitance.

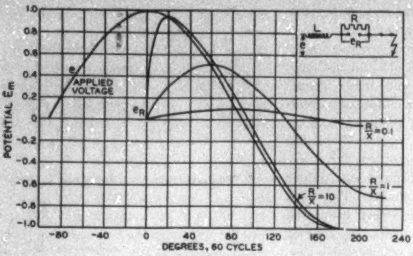


Figure 9. Voltage transients across a gap inserting resistance in an inductive circuit

The sum of these two currents flows through the inductance and the conditions upon which equation 8 is based are no longer true. Consequently, equation 5, 6, or 7 must be used to express the relations. For values of r above the critical value of resistance, the circuit is oscillatory and the crests of the voltage recovery transient exceed the crest of the applied voltage. Except for values of resistance close to the critical resistance, the voltage recovery rate is substantially the same as if no resistance were used across the breaker. Consequently, resistances intended to facilitate interruption must be chosen between definite limits; they must be low enough to be easily inserted by the first gap and high enough to limit the current to values which can be readily broken by the main interrupter.

If the resistance is properly chosen for the maximum interrupting capacity, its insertion during operations at currents below the rated interrupting capacity cause no more difficult conditions because r/x is smaller and the voltages across the resistance are lower. For low currents at higher power factors the voltage drop across the resistor is only a fraction of the drop across the total circuit resistance.

The resistor may be inserted by an arc rupturing device having a high arc voltage, which effects the transfer during the course of the cycle rather than at the current zero. As soon as voltage appears across the connections to the resistor a part of the current is transferred to the resistor. An arc voltage maintained higher than the voltage drop in the resistor will result in the transfer of all the current from the arc, without waiting for an arc-current zero.

The resistor makes the final circuit much easier to interrupt. That high-power-factor circuits are easily opened is generally known. Typical transients showing why this is true are given in figure 10 for various values of r/x from 0.125 to 2. All the curves are drawn for the same values of L and C which have been assumed very large. The transients have a natural frequency of only 900 cycles per second and are spread sufficiently to be easily studied. These curves show clearly that the insertion of a resistor in a reactive circuit reduces the crest value of the transient and also the voltage recovery rate and consequently facilitates the interruption.

The interrupting performance of a resistance-inserting breaker is practically independent of the severity of the circuit recovery-voltage characteristics because the voltage transients across its contacts are controlled largely by the resistor.

With charging currents the resistor can reduce the magnitude of the overvoltages which occur during interruption. The resistor may be inserted by a high arc drop in a manner similar to that on an inductive circuit. If it is inserted at a current zero, the voltage which appears across the parallel gap increases slowly and to a magnitude determined by the relation of the resistance to the inductance and the capacitance of the circuit. If inadequate gap is available a restrike will occur. After the resistance is inserted, the capacitance is charged through the resistor and the voltage of the capacitance is lower than the applied voltage. Any restrike across the second gap occurring before adequate contact separation is obtained will be through the resistor and will be damped by it. Thus the resistor facilitates the extinction of charging currents by reducing the voltage of the capacitance and by damping transients if they occur. Consequently, the resistor reduces the overvoltages which can occur.

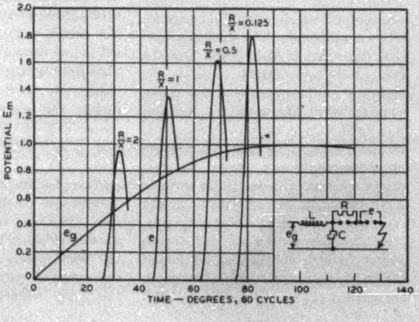
Except when the resistor provides the cheapest means of increasing the upper limit of the interrupting ability of a circuit breaking device, its complication is undesirable. Usually, the extension of existing designs will produce a simpler and cheaper means of obtaining the desired interrupting ability.

Arc Resistances in Circuit Breakers

The resistances of the arc spaces in circuit breakers play a part in the damping of switching transients. For example, during the interruption of charging current, the arc strikes through an appreciable distance and introduces arc resistance into the circuit. The arc resistance depends upon current and the arc voltage varies with the arc length. Consequently, the resistance of the arc space exerts an appreciable damping effect upon the transient produced by the restriking.

That the resistance of the arc space may be sufficiently low to influence the voltage recovery transient on inductive circuits was demonstrated in a paper presented before the Institute in 1933. The demonstration that current flowed through these arc spaces during the recovery transient was based upon cathode-ray oscillograms. Four oscillograms were shown which gave approximately the same range and type of transients as those represented in figure 4 of this paper. The transients were not exactly

Figure 10. Voltage transients on a gap interrupting an inductive circuit into which a resistance has been inserted



the same however, since the resistances producing the damping were variable instead of constant.

Summary

An inspection of switching transients shows that the resistances of the circuits play an important part in limiting the amplitudes and the durations. Although only a few typical circuits have been analyzed, the general damping effect of resistance has been shown. The calculation of more complicated circuits becomes extremely difficult if the resistances, reactances, and capacitances are all considered. Consequently, calculations of more complicated circuits generally are limited to a consideration of the inductances and capacitances only. The damping of the transients is not included in the calculations and approximations for them have to be made. It is believed that the charts presented in this paper will help in choosing suitable damping factors.

In the design of circuit breakers resistors can be used to increase the interrupting ability of the circuit breaker at the expense of simplicity in construction.

The resistances of the arc spaces of circuit breakers can appreciably influence switching transients by increasing the damping of the circuit.

Appendix

The equations given below are the basis for the curves in the illustrations.

Closing Transients of Inductive Circuits

$$i = \frac{E_m}{Z}\left\{\cos(\theta - \theta_0 - \theta_1) - \left[\cos(\theta_0 + \theta_1) - \frac{i_0 z}{E_m}\right]\epsilon^{-\frac{r}{x}\theta}\right\} \quad (1)$$

E_m = crest value of applied voltage

i_0 = i for $\theta = 0$, the start of the transient

r, x, and z are the resistance, reactance, and impedance of the circuit

θ_0 = phase angle of voltage at $\theta = 0$

$\theta_1 = \tan^{-1}\frac{x}{r}$

Closing Transients on Capacitive Circuits and Opening Transients on Inductive Circuits

For $R > 2\sqrt{L/C}$

$$e = E_m\left\{1 - \frac{LC}{\sqrt{R^2C^2 - 4LC}} \times (n_1\epsilon^{-m_1t} - m_1\epsilon^{-n_1t})\right\} \quad (2)$$

$$m_1 = \frac{RC + \sqrt{R^2C^2 - 4LC}}{2LC}$$

$$n_1 = \frac{RC - \sqrt{R^2C^2 - 4LC}}{2LC}$$

For $R = 2\sqrt{L/C}$

$$e = E_m\left\{1 - \left(1 - \frac{Rt}{2L}\right)\epsilon^{-\frac{Rt}{2L}}\right\} \quad (3)$$

For $R < 2\sqrt{L/C}$

$$e = E_m\left\{1 - \frac{2\sqrt{LC}\epsilon^{-\frac{Rt}{2L}}}{\sqrt{4LC - R^2C^2}} \times \sin\left(\frac{\sqrt{4LC - R^2C^2}}{2LC}t + \tan^{-1}\frac{\sqrt{4LC - R^2C^2}}{RC}\right)\right\} \quad (4)$$

Opening Transients of Inductive Circuits Having Both Series and Shunt Resistances

For $\frac{R}{L} - \frac{1}{R_1C} < \frac{2}{\sqrt{LC}}$

$$e = E_mR_1\left\{\epsilon^{-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{R_1C}\right)t}(A_1 \sin bt + B_1 \cos bt) + C_1 \cos(\omega t + \alpha)\right\} \quad (5)$$

$$A_1 = \frac{C_1}{b}\left[\omega \sin\alpha - \frac{1}{2}\left(\frac{R}{L} - \frac{1}{R_1C}\right)\cos\alpha\right]$$

$$B_1 = -C_1 \cos\alpha$$

$$C_1 = \sqrt{\frac{1 + R_1^2\omega^2C^2}{(RR_1^2\omega^2C^2 + R + R_1)^2 + \omega^2(R_1^2\omega^2C^2L + L - R_1^2C)^2}}$$

$$b = \frac{\sqrt{4LC - \left(\frac{L}{R_1} - RC\right)^2}}{2LC}$$

$$\theta = \tan^{-1}\frac{\omega L}{R} + \frac{3\pi}{2} \text{ or } \tan^{-1} - \frac{R}{\omega L}$$

$$\psi = \tan^{-1}\frac{\omega L(R_1^2\omega^2C^2 + 1) - R_1^2\omega C}{R(R_1^2\omega^2C^2 + 1) + R_1}$$

$$\gamma = \tan^{-1}\omega CR_1$$

$$\alpha = \theta - \psi - \gamma$$

For $\frac{R}{L} - \frac{1}{R_1C} > \frac{2}{\sqrt{LC}}$

$$e = E_mR_1C_1\left\{\epsilon^{-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right)t} \times (D_1\epsilon^{ft} + D_2\epsilon^{-ft}) + \cos(\omega t + \alpha)\right\} \quad (6)$$

$$D_1 = \frac{1}{2f}\left\{\left[-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right) - f\right] \times \cos\alpha - \omega \sin\alpha\right\}$$

$$D_2 = -D_1 - \cos\alpha$$

$$f = \sqrt{\frac{(L - RR_1C)^2 - 4LCR_1^2}{2LCR_1}}$$

For $\frac{R}{L} - \frac{1}{R_1C} = \frac{2}{\sqrt{LC}}$

$$e = E_mR_1C_1\left[\epsilon^{-\frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right)} \times \left\{\left[\omega \sin\alpha - \frac{1}{2}\left(\frac{R}{L} + \frac{1}{CR_1}\right)\cos\alpha\right]t - \cos\alpha\right\} + \cos(\omega t + \alpha)\right] \quad (7)$$

The voltage across a gap inserting a resistor in an inductive circuit, figure 9, is given by equation 8. This equation assumes that the resistance-inserting gap is an arc-rupturing device which has a negligible arc voltage and which interrupts at a normal current zero.

$$e = \frac{E_mr}{x\sqrt{1 - \left(\frac{r}{x}\right)^2}} \times \left[\cos\left(\theta - \theta_1 - \cot^{-1}\frac{r}{x}\right) - \epsilon^{-\frac{r}{x}\theta}\cos\left(\theta_1 - \cot^{-1}\frac{r}{x}\right)\right]$$

E_m is the crest value of the 60-cycle voltage wave

$\frac{r}{x}$ is the ratio of the resistance and reactance of the circuit

θ_1 is the phase of the applied voltage at the start of the transient

If the extinction voltage is not negligible an additional term is inserted after $\cos\left(\theta_1 - \cot^{-1}\frac{r}{x}\right)$.

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Discussion

W. F. Skeats (General Electric Company, Philadelphia, Pa.): Mr. Van Sickle is to be congratulated upon the clarity of his exposition of the subject. Undoubtedly many will obtain a much clearer understanding of the influence of resistance from reading this paper.

A warning must be sounded, however, with reference to connecting too closely the rate of decay of the d-c component of short-circuit currents with the power factor. The resistance influencing the former is substantially the d-c resistance of the circuit, whereas the resistance involved in the latter is the a-c resistance and may be considerably higher. Similarly the resistance involved in the damping of the higher-frequency oscillations which occur upon interruption of a circuit, is likely to be considerably greater than the normal-frequency resistance, and must be so in order to bring about the damping that is observed.

It is also easy to make a mistake in the consideration of load resistance. A synchronous motor, for instance, even though it may be operating at unity power factor must be represented for consideration of transients not as a resistance, but as the combination of a reactance and a generating source. Lighting and heating loads are pure resistance but may not be very effective for damping because of the reactance of transformers and other apparatus which must in general be connected between these loads and the circuit breaker.

Mr. Van Sickle's statement at the end of the section on "Opening Transients of Capacitive Circuits," "These voltages are harmless and indicate that on a typical system the high overvoltages obtained theoretically by neglecting the dielectric characteristics of the breaker, are seldom encountered," seems contradictory to conclusion 5 of the paper by Messrs. Evans, Monteith, and Witzke (AIEE Transactions, volume 58, 1939, pages 386–97) which, after a discussion of overvoltages based on only a slight modification of the theory discussed by Mr. Van Sickle, reads, "The results presented in this study are believed to provide an explanation for some of the line and neutral point flashovers . . . that have been experienced on actual systems."

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): When considering the broad subject of switching transients, it is always of interest to compare field tests with the analytical results. Mr. Van Sickle has included test data of the line-to-ground voltage for the condition of opening charging currents on a 230-kv solidly grounded system. The oscillogram to which Mr. Van Sickle refers shows seven restrikes and a maximum voltage of about 250 per cent of normal crest. The voltage obtained falls in the range for similar conditions given in figure 13 of our present paper (AIEE Transactions, volume 58, 1939, pages 386–97) which shows 230 per cent for one restrike and 320 per cent for two restrikes. In considering this comparison it is to be noted that the system under test was of considerable greater total mileage but that only one-third of total was deenergized by the switching operation. These figures are in reasonable agreement and emphasize the statement in our paper that there may be a large number of restrikes in the actual case of interrupting the circuit but only a limited number are significant in producing the overvoltage. (See also discussion, page 412.)

R. C. Van Sickle: Mr. Skeats brought out a very valuable point in emphasizing the need for choosing the correct values of inductance and resistance in the analysis of circuit transients. The values used should be those which correspond to the frequencies of the transient.

In the discussion of the closing transients of inductive circuits, the decay of the d-c component is given as a function of the power factor of the circuit. The frequencies of the transients were 60 cycles or less and the assumption was made that the values of the resistances and inductances were the same for both components of the transients. This is a good approximation not only for the simple single-phase circuit which was discussed but also for three-phase circuits. The International Electrotechnical Commission Publication No. 56 "I.E.C. Specification for Alternating Current Circuit Breakers," uses the same assumptions in an appendix which gives a method of determining the short-circuit power factor of a test from the decay of the d-c component.

When a more exact determination is desired, both the resistance and the inductance values should be taken more accurately.

The difference in the 60-cycle resistance and the d-c resistance will be determined largely by the skin effect in the larger conductors of the circuit. In parts of the circuit such as the bus structure this may make a 15 or 20 per cent difference but for the entire circuit only a much smaller increase would be expected.

The values of the inductances determining the a-c and d-c components are not the same. The d-c component depends upon the negative-sequence reactance. The a-c component depends upon the direct-axis reactance, and in the simple circuit discussed in the paper, it was assumed constant.

Mr. Monteith and Mr. Evans have shown the agreement of this paper with theirs.

Power-System Voltage-Recovery Characteristics

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WITH the increased use of protector tubes, a thorough knowledge of the recovery voltages to which they may be subjected has become highly desirable. The current range over which a tube can be expected to function successfully is necessarily dependent on the voltage recovery characteristics of the system in which the tube is applied. Since many systems have a large range of fault currents for the different fault and system conditions, it is essential that the corresponding voltage recovery characteristics be carefully considered. This paper presents the results of an investigation to determine and evaluate the importance of the factors which influence the voltage recovery characteristics of power systems. A very large number of tests were made on a specially designed miniature equivalent circuit representing a transmission line, connected apparatus, and the fault-clearing device. In that a miniature system was used, this investigation is similar to that made by Messrs. Evans and Monteith and presented in two recent papers.[1,2] The results presented in this paper represent refinements and extensions to the understanding of the phenomena which will be of interest to both designers and users of protector tubes or other fault-clearing devices. Factors which have not been evaluated previously are shown to influence the phenomena considerably.

Special electronic devices were developed for the purpose of applying and removing the fault in synchronism with the source voltage. These devices have inherent characteristics which closely simulate the arc voltage characteristics of protector tubes.

The point on the voltage wave corresponding to the instant of application of the fault, is shown to be very significant under certain conditions. When this is varied, results obtained indicate the indefiniteness of a first crest in the transient voltage recovery characteristics and point to the maximum crest, time to maximum crest, and the initial recovery rate as being more descriptive.

In the case of double-line-to-ground faults, the conditions under which they would be most likely to occur on an actual system are considered.[5] The effect of this consideration, and the effect of restriking of the first tube attempting to clear are illustrated. Misleading results are shown to be obtainable if these considerations are neglected.

Generalized results are presented in a form such that they can be applied to any solidly grounded system operating between 13.8 and 138 kv.

Conclusions

1. Different angles of fault application materially affect system voltage recovery characteristics. When this factor is considered, the significance of a first crest is lost in many cases, and the results indicate that time to maximum crest and maximum crest voltage are the main system voltage recovery characteristics which lend themselves to summarization. Initial rate of rise of recovery voltage is important, but since it varies greatly with fault location in a given system, no attempt is made to summarize the results obtained for the initial period. Instead, a method of calculating the maximum initial rate of voltage rise is indicated which is adequate for many cases. It should not be inferred from this that the events taking place during the interval of time up to voltage crest are unimportant in the functioning of the tube. Actually, the entire voltage recovery characteristic is important up to the time of maximum voltage reached.

2. In studying double-line-to-ground-fault recovery voltages, the probable angle of fault occurrence in an actual system must be considered. Under certain conditions, much more severe recovery voltages can be obtained in a controlled setup than are likely to be obtained in an actual system. It seems evident that if a tube can interrupt a single-line-to-ground fault in a solidly grounded system where tower-footing resistance is encountered, it generally will be able to interrupt a double-line-to-ground fault.

3. Crest recovery voltages obtained in the solidly grounded systems investigated varied from 1.5 to 1.75 when tower-footing resistance was neglected. The effect of losses in the system (including tower-footing resistance) is to decrease the overshoot for single-line-to-ground faults. It appears that 1.75 is near an upper limit which may be reached in higher-voltage systems while 1.5 would be more likely to be reached in a lower-voltage system. This is in general agreement with field tests and theoretical considerations. It should be recognized, however, that system loads and interconnections generally would tend to reduce slightly the maximum overvoltage reached in an actual case.

4. Neutral grounding resistance produces a decrease in maximum overshoot in a manner very similar to that produced by tower-footing resistance.

5. Neutral grounding reactance increases the time to crest, since the zero-sequence frequency is reduced as more reactance is added in the neutral. The effect upon the crest voltage reached appears to depend greatly upon the length of line involved.

6. The magnitude of the arc voltage corresponding to the arc voltage across the protector tube (over a practical range) appears to have little effect upon the voltage recovery characteristics.

7. Length of line, system voltage, and fault current all play an important part as shown in figure 8 of this paper. It is expected that this information will be helpful as an aid to the proper selection of tubes for solidly grounded systems.

8. The results of this investigation appear to be in general agreement with those published by Messrs. Evans and Monteith,[1,2] although, since the systems studied were different in the two investigations, direct comparisons are difficult to make.

9. While this investigation has been chiefly concerned with recovery voltages obtained when faults are interrupted by protector tubes, it is felt that some of these results may be of value to designers and users of other fault clearing devices as well. The flexibility of a miniature setup of this type with the advantages of electronic switching devices has been, and promises to continue to be, helpful in arriving at a better understanding of numerous closely related phenomena.

Equipment Used

The equivalent system used to represent the transmission line was specially designed for this investigation.[6] This made possible the use of units having characteristics simulating those of an actual transmission line over the desired range of frequencies. One factor which influences the phenomena quite appreciably is the resistance-frequency characteristic of the units representing the transmission line.[7] A three-phase equivalent π section as used in this study representing ten miles of typical line is shown in figure 1. This

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Paper number 39-49, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted October 26, 1938; made available for preprinting January 3, 1939.

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The author acknowedges the helpful suggestions of Edith Clarke and S. B. Crary and C. Concordia, which have contributed to the success of this investigation. Acknowledgment is also made of S. B. Farnham's assistance in making many of the laboratory tests.

1. For all numbered references, see list at end of paper.

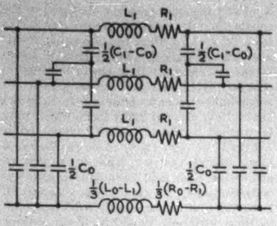


Figure 1. Artificial-line-section connections

is similar to the circuit used in obtaining the results of references 1 and 2.

Source reactances were simulated by means of reactors having the desired characteristics. Transformers were used as shown in figure 2 to make it possible to have a low ratio of X_0/X_1 at the source when desired.

Electronic switching circuits were developed for applying the fault at any desired point on the voltage wave and for interrupting the current or currents at or near the normal current zero. The operation of these circuits is described in the appendix. The circuit used to apply and remove single-line-to-ground faults is shown in figure 3. Use of the thyratron tube to carry the fault current made it possible to represent quite accurately the arc voltage inherent in the protector tube.[4] By variation of the constants, the circuit can be adjusted to apply the fault at intervals of from one to about six cycles, the allowable frequency of repetition depending upon the amount of damping in the circuit under investigation. Once the frequency of fault application has been selected, the phase-shifter can be rotated to vary the point of application of the fault on the voltage wave throughout the range of probable occurrence in an actual system. Thus the effect of point of application of the fault could be investigated easily with this circuit since the thyratron interrupts the fault current automatically at the first subsequent current zero regardless of when the fault occurred. This factor was found to be of particular significance under certain system and fault conditions. The magnitude of the arc voltage could be varied at will by inserting a storage battery in series with the thyratron.

The circuit for studying double-line-to-ground faults is shown in figure 4. A third unit can be incorporated to investigate three-phase faults.

Under some conditions in studying the double-line-to-ground fault, it was desirable to simulate the restriking of the first phase attempting to clear. This was accomplished by means of utilizing a synchronous switch to carry the first loop of current and transferring to the tube on the last loop. A circuit used to accomplish this is shown in figure 5.

A three-phase 110-volt 60-cycle, sine-wave generator was used to energize the miniature system. This was of sufficient capacity so that voltage at its terminals remained essentially constant regardless of transient disturbances imposed in the circuits. Thus auxiliary equipment could be energized from the same source, thereby providing the necessary means for synchronization of fault application.

To observe the transient recovery voltages, an oscilloscope was used. This afforded a convenient means for observing the repeated transient since the image could be made to appear stationary on the screen. Time exposure photographs could be taken of this image if desired. However, it was found more practical to take actual measurements directly off the calibrated screen of the oscilloscope.

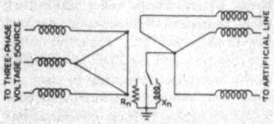


Figure 2. Equivalent circuit for source reactances

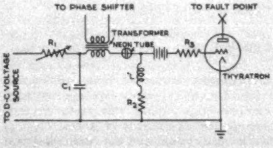


Figure 3. Recurring-fault circuit used in studying transient recovery voltages following single-line-to-ground faults

Discussion of Results

In discussing transient recovery voltages which occur when fault currents are interrupted by protector tubes, it should be emphasized that generally there are two basic differences between these voltages and those obtained in circuit-breaker operation. In the first place, as has been pointed out elsewhere,[1,2,4] protector-tube recovery voltages are generally of a slower nature than those very fast ones sometimes obtained in a circuit-breaker operation which severs one part of a system from another.

As a second point of difference, it should be noted that circuit-breaker interruptions more often occur from a steady-state condition of fault current while a protector tube interrupts generally from a transient condition. Ordinarily a circuit breaker is called upon to operate after fault current has been flowing for several cycles which is of sufficient duration for the natural frequencies to disappear from the fault current and for the d-c offset to be greatly reduced. Protector tubes usually operate in a single half cycle, and therefore, in many cases the natural frequency oscillations in the fault current may not be damped out when interruption takes place. The amount of this transient disturbance present (and also the amount of d-c offset) for a given case is a function of the point on the voltage wave at which the fault occurs.

The inherent flexibility which the fault-simulating circuit possesses made it possible to investigate the effect of fault angle. Figure 6*a* shows several single-line-to-ground-fault recovery voltage transients, each one for a particular value of the angle of fault application as indicated. The fault angle is designated as θ_A which is so defined that a value of

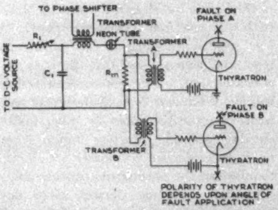


Figure 4. Recurring-fault circuit used in studying transient recovery voltages following double-line-to-ground faults

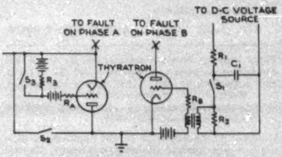


Figure 5. Recurring-fault circuit used in studying transient recovery voltages following double-line-to-ground faults

S_1, S_2, and S_3, are synchronous mechanical switches. Phase *A* carries two loops of fault current and phase *B* carries one

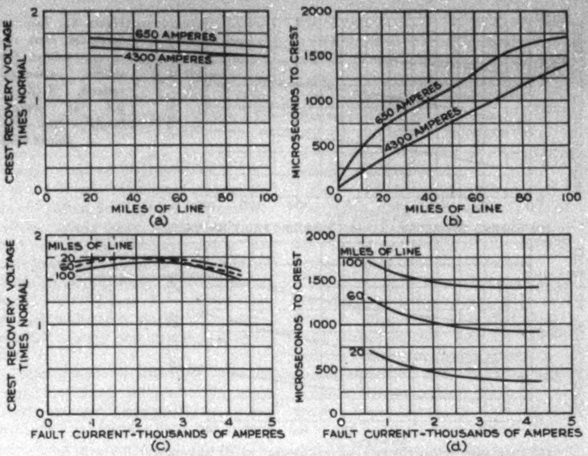


Figure 7. Effect of line length and fault current on recovery voltage. $X_0/X_1 = 0.4$ at source. Currents are root-mean-square symmetrical values for line-to-ground fault in 115-kv system

$\theta_A = 90$ degrees corresponds to the time at which the steady-state voltage on phase A is a maximum, the single-line-to-ground fault always being applied on phase A. Values of θ_A from 0–90 degrees indicate application of the fault before voltage crest is reached, while values of θ_A from 90 degrees – 180 degrees indicate application of the fault after the voltage crest. Figure 6c shows the crest values of the curves in 6a plotted as a function of θ_A. The maximum crest recovery voltage was obtained for $\theta_A = 78$ degrees. In this case, line losses were high enough so that the natural-frequency oscillations in the fault current were damped out before interruption took place.

Figure 6b shows several recovery voltage transients as obtained on the miniature system representing 100 miles of low-loss line and source reactances as indicated. In figure 6d the crest values reached in figure 6b are plotted as a function of θ_A. For this case, the maximum recovery voltage of 1.7 was reached for either of two values of θ_A, namely 88 degrees and 120 degrees, while in between these two peaks lies a low value of 1.2 at 108 degrees. This characteristic of alternate peaks and valleys in the crest recovery voltage versus θ_A curve is particularly marked in the case of low-loss lines and for low values of fault current.

From observations of the transient fault current, it was possible to associate these peaks and valleys with the oscillations in the fault current which were of a frequency essentially inversely proportional to the length of line represented in the miniature system. Corresponding field tests indicate that this frequency corresponds to the frequency with which a voltage (or current) surge travels four times over the line length involved.[3] The number of such traversals which can take place during the time that the fault is on is a function of the angle at which the fault is applied. Further analysis indicated that each peak of recovery voltage in figure 6d could be associated with fault-current interruption at an instant of minimum rate of change of fault current, and each valley with interruption at a maximum rate of change of fault current. Also, since initial rate of rise of recovery voltage is proportional to the rate of change of fault current just prior to interruption, it was possible to associate a maximum initial rate of rise with fault interruption at a maximum rate of change of current and a minimum initial rate of rise with fault interruption at a minimum rate of change of current. If a system has losses low enough so that these fault-current natural-frequency oscillations exist when the first normal current zero is reached, these alternate peaks and valleys in the crest recovery voltage versus θ_A curve will occur.

The significance of the curves of figure 6 is that in general it is necessary to determine a recovery voltage envelope obtained by applying the fault over a range of values of θ_A. It is conceivable that a single-line-to-ground fault could occur for any value of θ_A between 0 and 180 degrees. However, it has been found that the recovery-voltage envelope is generally determined by values of θ_A between 70 degrees and 110 degrees approximately.

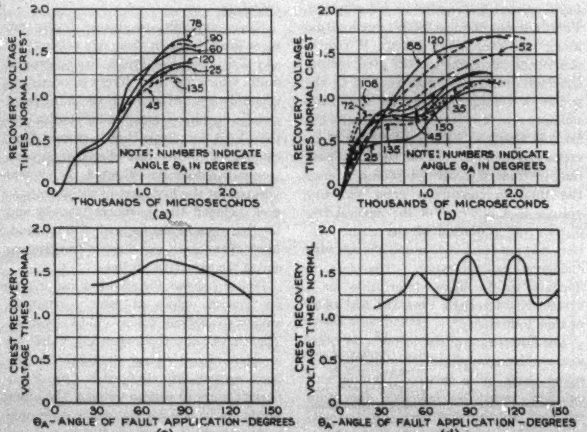
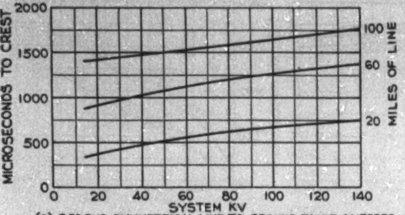


Figure 6. Effect of fault angle on recovery voltage. $X_0/X_1 = 0.4$ at source supplying 650 amperes root-mean-square symmetrical line-to-ground fault current in 115-kv system

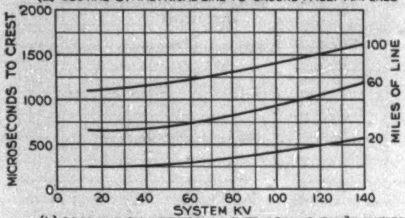
(*a*) and (*c*)—60 miles of high-loss line

(*b*) and (*d*)—100 miles of low-loss line

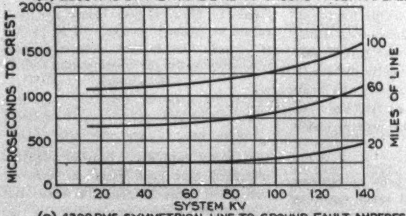
Figure 8. Time to maximum crest recovery voltage for solidly grounded systems. $X_0/X_1 = 0.4$ at source. Single-line-to-ground fault



(a) 650 RMS SYMMETRICAL LINE-TO-GROUND FAULT AMPERES



(b) 2200 RMS SYMMETRICAL LINE-TO-GROUND FAULT AMPERES



(c) 4300 RMS SYMMETRICAL LINE-TO-GROUND FAULT AMPERES

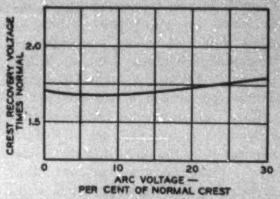


Figure 9. Effect of arc voltage. $X_0/X_1 = 0.4$ at source. One hundred miles of line. Six hundred fifty amperes root-mean-square symmetrical line-to-ground fault current in 115-kv system at source

When all possible angles are considered, in many cases the identity of a first crest is lost in a more or less smooth envelope rising to a maximum crest value of recovery voltage at a definite time for a given case.

The entire recovery-voltage envelope is of importance, and for successful operation it is essential that the recovery dielectric strength within the tube should be greater at all times than the voltage recovery characteristics. Since it is difficult to summarize recovery characteristics during the initial period, only the maximum crest value in the recovery-voltage envelope and the time required to reach this value are indicated in the summary of results.

There is another reason for using only the maximum crest value reached as an indication of the severity of recovery voltage. It was found that the initial period of recovery voltage is quite definitely a function of fault location along the line while the maximum crest voltage reached and the time to maximum crest voltage were not greatly affected by fault location. The reason for this is that the initial transient period following interruption is governed by the rate of change of fault current just prior to interruption. Fault current changes with fault location along the line, being greater if near a source and becoming less the more remote the fault location is with respect to the source of voltage. Therefore, the initial voltage recovery rate is greater if the fault is near the voltage source, and decreases as the fault location becomes more remote from the voltage source. Since the time to maximum crest recovery voltage is a measure of the natural frequency of oscillation of the entire system, it is not subject to much change with change in fault location. Therefore, it appears to be more descriptive as a system characteristic than the first crest or time to first crest.

The initial rate of rise of recovery voltage can be approximately evaluated for many cases by a relatively simple calculation, based on injecting the symmetrical fault current back into the network at the point of fault.[8] If the short-circuit current at any point along a line is known, then the initial rate of rise is

$IZ\omega \times 10^{-6}$ volts per microsecond

where

I = crest value of the symmetrical fault current
ω = $2\pi f$ (f is system frequency)
Z = surge impedance viewed from the fault point

The initial rate of rise calculated from this expression is the maximum that can be obtained (neglecting natural-frequency components in the fault current) since a symmetrical current wave is assumed. For any instant of fault occurrence which would give a d-c offset component in the fault current, the initial rate would be less. For any system having low losses such that the natural-frequency oscillations would not be damped out when interruption occurred, the maximum initial rate would be higher. In such cases, the maximum initial rate would be difficult to calculate.

Since single-line-to-ground faults are more likely to occur than any other type, and also, since the double-line-to-ground faults which are most likely to occur are generally less severe from the standpoint of tube operation, most of the results presented are for single-line-to-ground-fault conditions. After the effects of several factors had been investigated, the system selected for the first part of the investigation included the source reactances and the line open at the far end with no load. Most of the work was done at the sending end of the transmission line.

Figure 7 shows the effect of line length for several values of fault current in amperes referred to a 115-kv system. The curves are approximately valid for a system of any voltage provided that the fault currents are changed in proportion to the change in system voltage. In other words, it is possible to define a simple system such as these studied here more generally in terms of X_1 and X_0 at the source, but it is believed that the results are of more significance when plotted with amperes as a parameter. The results indicate that lower currents and shorter line lengths at a given system voltage tend to give higher crest values of recovery voltage in solidly grounded systems. Time to crest increases with line length. This can be explained by the fact that voltage reflections from the far end of the line return less frequently than for shorter lengths and therefore a longer time is required for this building up process. Higher currents reduce the time to crest since the short-circuit current in the miniature systems studied is a measure of source reactance. An increase in current is associated with a decrease in source reactance, which in turn increases the natural frequency of the system and consequently results in a shorter time to crest voltage when interruption takes place.

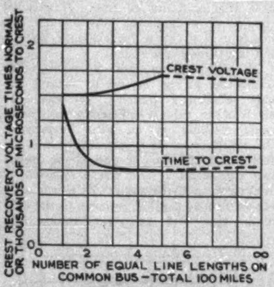


Figure 10. Effect of line grouping. X_0/X_1 = 0.4 at source. Four thousand three hundred amperes root-mean-square symmetrical line-to-ground fault current in 115-kv system at source

Figure 8 is a more comprehensive summary of results obtained for various lengths of line, for various system voltages, and for three values of fault current. These curves are of importance in that they show clearly the effect of varying any one of the three factors. The curves are valid for the relatively simple solidly grounded systems studied, but the qualitative effects of the factors involved can be shown to be valid in general for the more complicated systems on which tubes may be desired. Therefore, this summary may be considered a guide to proper selection of protector tubes. Crest recovery voltages are not summarized since tests indicated that in solidly grounded systems, line-to-ground-fault recovery voltages do not vary greatly unless influenced by losses, either in the connected apparatus or in the ground (tower-footing resistance). Since this summary in figure 8 excludes the effects of these factors, the crest recovery voltages for any of these cases is probably between 1.5 and 1.75 with 1.65 to 1.7 being typical for higher-voltage systems and 1.55 to 1.6 being typical for lower-voltage systems.

It was of interest to check some of the results shown in figure 8 against calculated results based on injecting the fault current into the network at the fault point and considering the distributed constants of the line. When this was done for several cases, it was found that calculations based on single-circuit traveling-wave theory[3] were in essential agreement.

Figure 9 shows the effect of varying the magnitude of arc voltage in the fault. Over a reasonable range, this factor has little effect on the crest value of recovery voltage for a given case. However, it can be shown that varying this factor does change the fault angle which gives the maximum overshoot.

Figure 10 shows the effect of line grouping on a common bus. This curve shows that for a given aggregate number of miles of line, the time to maximum crest recovery voltage is materially reduced as the line is broken up into more short sections. A minimum value is reached, however, which corresponds to lumping the capacitance of all lines at the point of fault. This fact suggests the possibility of calculating recovery voltages approximately using lumped constants when no long lines are involved.

Figure 11 shows the effect of neutral grounding through resistance. The qualitative effect is very similar to that observed when tower-footing resistance is inserted (see figure 15). This probably is to be expected since the circuit connections differ only slightly for the two conditions.

The effect of reactance grounding is shown in figure 12. Both maximum overshoot and time to crest are significantly altered by this factor. As more reactance is added, time to crest becomes greater because of the lower zero-sequence frequency. This soon becomes the predominant factor in determining both time to crest and maximum overshoot, particularly for the short lines where the effect of the distributed constants of the line become relatively small.

Figure 13 shows the effect of line length and short-circuit amperes when a line is fed from both ends. Qualitatively, the effect is very similar to that shown in figure 8 for lines fed from one end.

The effect of fault location along a line receiving power from both ends is of interest. Figure 14 shows that there is little change in crest recovery voltage, but that there is a significant change in time to maximum crest as the fault point is moved along the line. This effect was much less pronounced for lines fed from only one end. However, a fault at the midpoint of a line fed from both ends in this case is in effect the same as a fault at the far end of two 50-mile lines in parallel and bussed at both ends. Therefore, it appears that the effect shown in figure 10 is playing an important part.

The effect of tower-footing resistance for a line fed at both ends is shown in figure 15 for a single-line-to-ground fault.

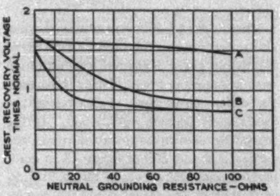
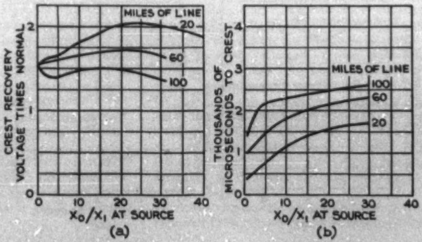


Figure 11. Effect of neutral grounding resistance. One hundred miles of line. X_0/X_1 = 0.4 at source

A—650, *B*—2,200, *C*—4,300 root-mean-square symmetrical line-to-ground fault amperes in 115-kv system for zero neutral resistance

Figure 12. Effect of neutral grounding reactance. Single-line-to-ground fault of 4,300 amperes root-mean-square symmetrical at source in 115-kv system for X_0/X_1 = 0.4



The crest voltage reached is seen to be reduced as the tower footing resistance is increased, and the extent of this effect is greater for the larger available short-circuit currents.

The effect of tower-footing resistance for a line fed at both ends is shown in figure 16 for a double-line-to-ground fault. Three pairs of curves are shown, each set corresponding to certain conditions of fault clearing as indicated.

In the miniature system it was possible to apply the fault simultaneously on phases *A* and *B* at any instant in region 1 of figure 17. In an actual case, double-line-to-ground faults would be quite unlikely to occur in a portion of this region. Present understanding of the behavior of lightning discharges in initiating power-system faults indicates that region 2 of figure 17 would probably be much more susceptible to double-line-to-ground faults since the polarities of the two phases involved are alike while the polarity of the third phase is of opposite sign.[5] Under these conditions, phase *A* will carry only a small minor loop of current prior to the first current zero in that phase. This means that quite likely it will not clear when it tries to interrupt first. Therefore tests made for faults applied in region 2 permitted phase *A* to carry current until the second current zero was reached. The curves marked *A*-2 and *B*-2 were obtained when this procedure was followed. Under these conditions phase *B* cleared first, although with appreciable tower-footing resistance, the difference in time of clearing of the two phases became very small. If the preceding assumptions are correct, the curves *A*-2 and *B*-2 of figure 16 in comparison with the curves of figure 15 indicate that in an actual case, the single-line-to-ground-fault recovery voltage is generally more severe than the corresponding double-line-to-ground recovery voltages where tower-footing resistance plays an important part in determining the minimum values of current to be obtained.

The two curves labelled *A* and *B* are the maximum recovery voltages on phases *A* and *B* respectively obtainable for applying the fault simultaneously at any instant on phases *A* and *B*, and each phase clearing in proper sequence at its first subsequent current zero. Curve *A* shows the highest recovery voltage reached on phase *A* for a fault applied simultaneously on phases *A* and *B* at such an instant that voltage on phase *A* was in the vicinity of maximum. Curve *B* shows the highest recovery voltage reached on phase *B* for a fault applied simultaneously on phases *A* and *B* at such an instant that voltage on phase *B* was in the vicinity of maximum. Phase *B* as defined will be carrying much less current than phase *A* for a practical range of tower-footing resistance, and in fact, may even be carrying less current than would flow for a single-line-to-ground fault. Analysis of figures 15 and 16 indicates that the double-line-to-ground fault under these conditions may be much more difficult to interrupt than the single-line-to-ground fault for certain values of tower-footing resistance.

The preceding analysis indicates that phase *B* (which would tend to clear first) may be difficult to interrupt. Therefore phase *B* was permitted to restrike in the miniature system so that phase *A* would be forced to interrupt first. When this was done, the pair of curves indicated as *A*-1 and *B*-1 were obtained. This made the recovery voltage on phase *A* very high while on phase *B* it had been reduced. The situation had not been entirely relieved since the recovery voltage on phase *A* was so high as to cause possible difficulties of interruption even though it was carrying the greater current.

Therefore it appears that if double-line-to-ground faults could occur at any time, without regard to the relative polarities of the phases involved, the recovery voltages associated with this type of fault would be a very important factor in the selection of protector tubes in many cases. It is quite likely, however, that relative polarities determine the conditions under which a double-line-to-ground fault can occur in an actual system, and for that reason the relatively high recovery voltages which can be obtained in a controlled miniature system are seldom, if ever, obtained in an actual system for this type of fault.

Appendix. Operation of Recurring-Fault Electronic Switching Circuits

In figure 3, the fault point designates any place in a network at which the recovery voltage following clearing of a fault at that point is desired. This fault is shown occurring to ground, but in the general case, this need not be true. It may be thought of as occurring between any two points in a network, for example, line-to-line in a three-phase system.

The thyratron in figure 3 can be made conducting at any instant by making the grid sufficiently positive, provided the anode of the tube is sufficiently positive to cause striking of an arc. Essentially, this results in a short circuit between the fault point and ground, and in the miniature equivalent system would represent a fault on the actual system By synchronizing an applied positive impulse to the grid of this tube with the system voltage, the fault may be repeatedly applied at any point on the voltage wave. Fault current will flow from the time the fault is applied until the first subsequent current zero when the thyratron will automatically cut off the fault current. The voltage appearing across this tube will correspond to the actual recovery voltage of the system represented.

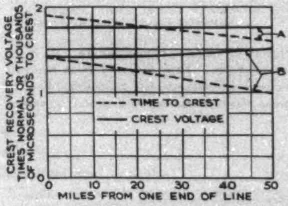


Figure 14. Effect of fault location. One hundred miles of line with equal generating capacity at both ends. Solidly grounded system

A—1,240 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

B—2,740 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

The thyratron serves to simulate the arc voltage drop of certain devices, such as expulsion tubes, for instance. The amount

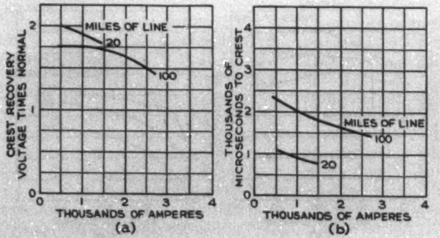


Figure 13. Effect of fault current. Equal generating capacity at both ends of line. Solidly grounded system. Currents are root-mean-square symmetrical amperes for line-to-ground fault at one end in 115-kv system

of arc drop is fairly constant for the tube during the time of current flow. The magnitude of arc voltage for specific cases can be varied by inserting a fixed direct voltage (for example, a storage battery) in series with the tube and the over-all drop can be made greater or smaller than the arc voltage of the tube alone.

The purpose of the portion of the circuit on the left-hand side in figure 3 is to time the application and removal of the fault so that the transient condition is repeated in synchronism with the system voltage. The time constant of the series circuit R_1C_1 can be varied to correspond roughly to the time for two, three, or any desired number of cycles of base frequency (preferably not more than about six). If the neon tube is not conducting, the voltage across C_1 rises exponentially with time when the direct voltage is applied. With the transformer unexcited, this same voltage appears across the neon tube. When this voltage reaches a certain value, the neon tube will suddenly become conducting and discharge the capacitor C_1 rapidly through L and R_2, the time constant of this path being small as compared with R_1C_1. The purpose of L is to prolong the duration of this discharge so that the thyratron will have ample time to ionize. Immediately after the discharge, the neon tube becomes nonconducting and the cycle repeats itself automatically.

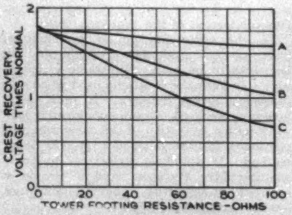


Figure 15. Effect of tower-footing resistance. Equal generating capacity at both ends of line. Solidly grounded system

A—100 miles of line; 414 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

B—100 miles of line; 1,240 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

C—20 miles of line; 1,460 root-mean-square symmetrical line-to-ground fault amperes at either end in 115-kv system

The purpose of the transformer is to add a small alternating component of system or reference voltage. The resultant voltage appearing across the neon tube while it is not conducting is the sum of an exponentially rising voltage and an alternating voltage of reference frequency. Therefore if the time constant R_1C_1 is made such that breakdown voltage of the neon tube is not reached on the second cycle, for instance, but is reached on the third, the frequency of firing the neon tube is made to interlock with the system frequency, and this will be repeated automatically every three cycles. The frequency of repetition depends essentially on the time constant R_1C_1. If the impulse of voltage appearing across L and R_2 when C_1 is suddenly discharged through the neon tube, is applied to the grid of the thyratron, then the fault condition desired will be repeated in synchronism with system frequency.

The primary of the synchronizing transformer is excited from a phase shifter which is excited from the same source used to energize the miniature system. By rotating the phase shifter, the complete circuit can be made to apply the fault repeatedly at any desired point on the voltage wave. When an oscilloscope is used to view the repeated transient, it can be made to appear stationary on the screen.

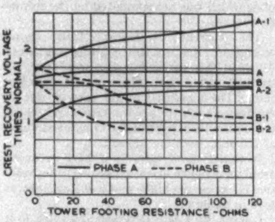


Figure 16. Effect of tower-footing resistance and angle of fault application. Equal generating capacity at both ends of 100-mile line. Double-line-to-ground fault on phases A and B at one end. Solidly grounded system. $I_A = I_B$ = 1,640 root-mean-square symmetrical amperes in 115-kv system for zero tower-footing resistance. See figure 17 for definitions of regions indicated

A and *B*—Maximum for interruption at first current zero

A-1 and *B*-1—Maximum for phase *A* carrying one loop and phase *B* two loops

A-2 and *B*-2—Maximum for phase *B* carrying one loop and phase *A* two loops

The tendency of a protector tube to interrupt before normal current zero can be simulated by means of inserting a small resistor in series with the faulting device and synchronizing as above a current surge of controlled magnitude and shape through this resistor. Thus a voltage is built up in opposition to the flow of fault current just prior to interruption so that the fault current is forced to zero sooner than it would be if normal conditions continued to exist. This introduces the characteristic rise in arc drop just prior to interruption of current flow and is of significance in some cases, particularly where the natural frequencies are high. Thus the effect of arc voltage characteristics can be simulated in great detail, not only for protector tubes, but for other interrupting devices as well.

The circuit shown in figure 4 is very similar to that of figure 3 in principle. In this case, two thyratrons are fired simultaneously in synchronism with the system frequency, thus making it possible to simulate double-line-to-ground-fault conditions in the miniature system.

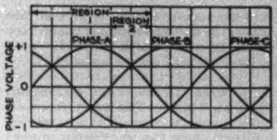


Figure 17. Regions of fault application

The circuit of figure 5 was used in applying double-line-to-ground faults when it was desired to investigate region 2 of figure 17 and permit current to flow in phase *A* until interruption occurred at the second current zero. The first loop of fault current in phase *A* was carried by S_2 and the second loop by the thyratron in parallel with it. Since phase *B* carried only one loop, it was carried entirely by the thyratron. S_1, S_2, and S_3, were all operated by synchronous contactors opening and closing at the proper times to produce the desired switching sequence. The contactors were mounted on an adjustable rack so that the angle of fault application could be varied as desired.

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Discussion

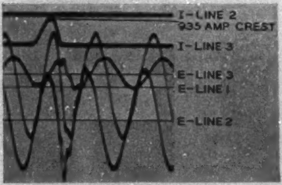
D. C. Prince (General Electric Company, Philadelphia, Pa.): This paper represents a valuable addition to the literature on voltage recovery transients. It is understood to refer primarily to the behavior of expulsion tubes. It has not yet been shown whether rate of rise of recovery voltage, or time to recovery crest and its value or some other characteristic of the recovery transient limits the capacity of certain interrupting devices. In fact, different interrupting devices may well be sensitive to different elements in the recovery characteristic.

In due course it is hoped that an actual recovery strength curve will be available for each interrupting device. When that time arrives the true significance of papers such as Mr. Peterson's can be measured.

E. J. Wade (General Electric Company, Pittsfield, Mass.): This paper is an excellent contribution to the literature on recovery voltage and Mr. Peterson is to be commended for his exhaustive study of the subject.

Because of the similarity in the results of this paper and that by Evans and Monteith, they mutually support each other although differing in details. This statement has even more significance when it is remembered that the circuit for the line representations and the switching methods were both different.

In almost every respect the agreement, where the results can be compared, is very close but it may be worth while to examine one point of difference which can perhaps be explained. Comparing figure 4 of the Evans-Monteith paper with figure 6 of Mr. Peterson's paper, it will be noted that the first crest of the recovery voltage is much more prominent in the Evans-Monteith paper, and the voltage thereafter drops considerably before increasing again to the second crest. In Mr. Peterson's paper (figure 6) the first crest is less marked and thereafter the voltage does not drop before being increased by the reflection from the far end of the line. Wave shapes which have been measured during field tests more nearly approximate the curves given in Mr. Peterson's paper and the question arises as to whether this difference in wave shape is due to a difference in the assumed constants, or to the method of representation of the line.



(a) Magnetic oscillogram (voltages measured at generator bus)

(b) Cathode-ray oscillogram. Voltage on line 3

Figure 1. Single-phase test on 115-kv circuit. Fault at near end of 60-mile line

It is of interest that the data represented by figure 6a of Mr. Peterson's paper was obtained with the circuit set up to simulate, as nearly as possible, the conditions found on a field test on the lines of the Boston Edison Company.

The oscillograms for this test are shown in figure 1 of this discussion. In general, the wave shapes, and time to crest, check very closely with Mr. Peterson's results, although there are minor differences in details, which may be attributed to the fact that the 60 miles of line was obtained by looping a 30-mile section of double-circuit line on opposite sides of the same towers. This introduces a small effect due to coupling which was not simulated by the miniature setup.

The current oscillogram also shows oscillations due to the starting transient which in this particular case were entirely damped out before the current reached zero. The measured initial rate of rise of voltage was 190 volts per microsecond which is a satisfactory check of the calculated value of 175 volts per microsecond.

It would be of interest if Mr. Peterson could give some data regarding the relative losses in the high- and low-loss lines as used in figure 6.

I agree with Mr. Peterson that the first crest is not of as much importance as the entire shape of the recovery voltage wave, and because the first crest tends to be obscured in some cases, further agree that it would be preferable to use 90 per cent of the crest of leg voltage as this gives a definite value of voltage at which the corresponding times may be compared.

R. D. Evans, A. C. Monteith, and **R. L. Witzke** (all of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The subject of switching transients has been under discussion for some years. Due to the complexity of the problem, analytical work has been rather limited, the best conclusions being taken from actual system performance. Recognizing the needs for a method of calculation and for further refinement in theories, the a-c network-calculator method or miniature-system method was developed and presented[1] two years ago at the summer convention in Milwaukee. At that time it was introduced as a method that could be used for analyzing recovery-voltage problems, but it was also recognized that it "opened the way to a systematic general investigation of recovery voltage transients and related problems" and presented "a practical method of determining the electrical transients of systems." A review of the discussions presented indicates that at that time the method was considered radical and skepticism was expressed as to its broad usefulness.

It is encouraging to us to note that several papers presented on this subject today make use of this method. It is our firm belief that this method will receive even broader usage.

The paper by Mr. Peterson is of interest in that it so closely parallels our work, the results of which were presented[2] at the last winter convention. Where comparison can be made the checks are surprisingly close. Any differences appear to be due largely to differences in assumptions rather than to differences resulting from the actual application of the method or from the introduction of refinements.

We do not clearly understand Mr. Peterson's statement as to the difficulty of summarizing the first crests and including the effects of initiating transients. A summary of measurements of first crests was presented in our paper.[2] In that work we made adjustments to include the effect of the initiating transients, the fault being applied so as to make the first crest a maximum. We feel that this is essential as in a large number of cases the design of the protector tube is determined by the first crest.

Quite often, depending upon the system characteristics, the recovery voltage curve may have several crests before maximum is reached. In our paper presented last year,[2] we not only summarized the first crest but also the succeeding significant crest, having in mind the protector-tube application. Mr. Peterson apparently has summarized the maximum crest with its corresponding time. The fact that our paper summarized the "significant crest" and Mr. Peterson's paper the "maximum crest" will account for differences in the data. This is a possible explanation of why Mr. Peterson's summary showed relatively higher voltages for the high-voltage systems. It was our opinion, that the times associated with these higher voltages were so long as to make the slightly lower voltage with a considerably shorter time more important in the application of the protector tubes.

In the representation of systems we have given considerable thought to the networks to be used. We found it necessary to weight the constants used when employing a π-type network. In most cases, however, we used a more complicated network with a weighting of the constants proportioned for the particular system being studied. It would be interesting to know whether Mr. Peterson used a simple π network or one with weighted constants for the short line sections.

Electronic devices for controlling the application and removal of the fault are convenient and advantageous where the electronic tube characteristic closely follows that of the arc path desired for the study. This arc characteristic does not, however, lend itself to broad transient analysis. It is for this reason that, after giving consideration to the use of the electronic device and the synchronously driven switch, we chose the latter, preferring to have one device for general investigation.

In eliminating the consideration of the double line-to-ground condition for the application of protector tubes, Mr. Peterson has considered the zone where the instantaneous voltages on two phases are of the same polarity and of practically equal magnitude. It is our opinion that the power-system voltage is low in comparison with that of lightning and therefore that the effects of polarity and magnitude of the normal system voltage are negligible. Lightning can strike any phase, and cause the breakdown of one tube which in the more severe cases will cause a rise in the potential of the ground point and result in a flashback through one or two of the remaining tubes. If this is correct the double-line-to-ground fault can occur at any point and cannot be eliminated from consideration.

There is an intermediate range of tower resistance that makes it possible for the first tube attempting to clear for the double-line-to-ground fault to hang on and for the second tube to clear. The condition would then be that of a single-line-to-ground fault and the first tube would then clear. Such operation would subject the first tube to considerable erosion. However, for both the lower and the higher values of resistance, the severity on the two tubes is so closely the same that if one fails to clear, the other will probably not clear. We therefore feel that the recovery voltage conditions for double-line-to-ground should be considered in the selection of the tube.

In conclusion it is of interest to point out that no factor has been uncovered in these investigations to question the grounded-neutral system which is in general use in this country.

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Harold A. Peterson: The interest aroused in the general subject of overvoltages as indicated by discussions both written and oral reflects the importance of being able to obtain readily quantitative results such as those presented in my paper. The miniature-system method of actual system representation appears destined to continue to play an important part in arriving at a better understanding of numerous closely related transient and steady-state phenomena. Since this practical tool for evaluating voltage recovery characteristics for a variety of interrupting devices under various operating conditions has been developed to a stage of usefulness, it is hoped that similar progress can be made in determining the recovery dielectric strength curves for each interrupting device as well, so that the problem of co-ordination between such curves and voltage recovery characteristics can be placed on a sound engineering basis as Mr. Prince suggests.

In comparing figure 6 of my paper with figure 4 of the Evans-Monteith paper (reference 2), Mr. Wade has brought out a significant point. The following discussion will explain the cause of this point of difference in results and will answer several of the questions raised by Messrs. Evans, Monteith, and Witzke in their discussion. The reason for this point of difference can best be understood by considering various methods of representing the transmission line in miniature. Figure 2 of this discussion shows a family of voltage-recovery curves obtained for an assumed actual system. Each curve corresponds to a different method of representing the actual system as indicated. The fault was left on sufficiently long in each case so that interruption took place from a steady-state condition (that is, there were no initiating transients and only symmetrical power current was flowing). It will be observed that the curve *A* obtained using nine π sections for the 90 miles of line closely approximates the behavior of a uniformly distributed constant line (curve *D*). This approximation is very good, even to the timing of the return of the reflected wave from the far end of the line. Since no losses were assumed in calculating curve *D*, it is to be expected that the crest voltage after reflection would be higher than that actually obtained in any of the miniature system setups.

Curve *C* obtained for a weighted-constant double-π representation shows a tendency to give a higher first crest voltage than that obtained either with the nine π sections or by distributed-constant calculations. There is a pronounced voltage drop after this first crest is reached before the voltage continues on upward to the maximum crest value. It will be observed also that the axis of oscillation of this first period is the true exponentially rising recovery characteristic.

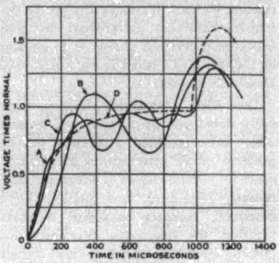

Figure 2

Curve *B* shows the recovery characteristic for an equivalent double-π representation (not weighted). This gives a still higher first crest voltage, but the axis of oscillation appears to be approximately the exponentially rising true recovery characteristic.

Several points of interest may be noted in this figure. All three methods of miniature representation considered yield essentially the same time to maximum crest voltage, and only slight discrepancies appear to exist for the magnitude of the maximum crest voltage. The greatest discrepancies occur during the initial period before the reflection returns from the far end of the line. The first crest voltage obtained for a double-π representation, weighted or not, characterized by magnitude and a time to that magnitude, *does not lie on the true system recovery characteristic.* With the nine π section representation there are several oscillations, small in magnitude, which deviate only slightly from the true recovery characteristic. It is for this reason the statement was made that it is difficult to summarize the initial part of the recovery characteristics in terms of a first crest and a time to first crest. This analysis may account for the fact that some of the first crest voltages obtained by Messrs. Evans and Monteith[2] appear to be high. First crests, at least in part (depending on initiating transients), are characteristic of the miniature system representation, and in general, may not be characteristic of the actual system it was intended to represent. As indicated by the curves of figure 2, time to 90 per cent normal voltage would have more significance in interpreting results obtained in our miniature system where each artificial π line section represents ten miles of actual line. Experience, as illustrated by these curves, indicates that for lengths of line as short as 20 miles, such π representation does not give very accurate initial recovery conditions. However, maximum crest and time to maximum crest voltage are not in appreciable error.

It is important to point out that the entire recovery voltage characteristic is important, and therefore any attempt to summarize the severity of the initial period by means of a single magnitude and the corresponding time cannot be entirely adequate.

The low-loss line as used in obtaining results in figure 6 of my paper had an R_1/X_1 and R_0/X_1 ratio of 0.10. The high-loss line had an R_1/X_1 ratio of 0.40 and in addition had a high neutral-return resistance of two ohms per mile to damp out quickly the fault-initiating transients.

Electronic devices for controlling and removing the fault in a miniature system are distinctly advantageous. Fault interruption always takes place precisely at a current zero without adjustment. This is true even for very high natural frequencies of the circuit under investigation. High frequencies present an almost insurmountable difficulty to mechanical devices if flexibility of control for recurring transient conditions is desired. In addition, when electronic devices are used, various arc-path characteristics can be simulated as indicated in my paper. This is of importance where it is desired to know the effect of assumed or known arc characteristics of certain interrupting devices. If the miniature-system voltage base is sufficiently high, the normal arc drop in the thyratron tube becomes insignificant. In cases where such conditions do not prevail the arc drop can be reduced to zero as indicated in the appendix.

It was not my intention to eliminate the double-line-to-ground fault from consideration in the application of protector tubes as Messrs. Evans, Monteith, and Witzke infer. The curves shown in figure 16 were simply intended to illustrate the effects of restriking in the case of a double-line-to-ground fault. A double-line-to-ground fault can occur at any instant regardless of polarity, although the region indicated in figure 17 is probably most susceptible. In case restriking does occur for a double-line-to-ground fault, clearing becomes a selective process. The phase to clear first will then be the one which can be most easily cleared. If the tube is not able to clear the easier phase to interrupt of the two, the fault may continue until cleared by braker action. However, in general, it is necessary to consider every possible instant of fault application and interruption at the first current zero in each phase to insure proper protection over a period of time since any restriking results in excessive erosion of the tube walls.

A New High-Capacity Air Breaker

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Synopsis: Air circuit breakers for lower voltages of simple design are being replaced by improved types, which incorporate specially designed circuit-interrupting devices for the purpose of improving the interrupting efficiency and minimizing the formation of arc flame and gases. A new form of deionizing arc interrupter is described which is equally effective for both d-c and a-c circuits. A new air circuit breaker has been designed to utilize this interrupter in which carbon arcing contacts are replaced by refractory metal, and laminated-brush-type main contacts are replaced by silver-faced solid copper. These improvements have led to greatly increased current-carrying capacity in breakers of a given size and also large increases in interrupting capacity with a minimum of noise and flame, which permits the breakers to be readily mounted in enclosures and cubicles of small physical size.

I. Introduction

INTERRUPTION of low-voltage circuits in air is so easily accomplished by the mere separation of suitable contacts that it has been unnecessary to develop efficient arc-interrupting devices in order to make air breakers workable. The usual breaker construction permits the formation of an unrestricted arc which lengthens due to a self-generated magnetic field until interruption is accomplished, and meanwhile discharges ionized gases in undesirable quantity to the surrounding space. Such simplicity is permissible if adequate mounting space is available for the breaker, and if there is no objection to the thunderous detonation which accompanies the arcing.

Compact metal-clad switchgear, however, has grown up around the conception of adequate circuit breakers which will take care of the gases and other by-products of interruption in the devices themselves without imposing complications on the bus and mounting structure. Standard practice involves air-insulated busses and connections for the most part with conductor insulation only as protection against inadvertent contingencies. The increasing use of air breakers in metal enclosures is, therefore, creating a demand for carefully controlled arc interruption which will confine the arc to deionizing chambers and prevent electrical breakdown between phases or to ground as the result of ionized flame widespread about the breaker.

Deionizing interrupting devices, originally developed by Slepian and associates,[1] have provided a solution to the problem of air interruption in confined space for several classes of breakers,[2] and recently Dickinson,[3] and Sandin,[4] have described compact enclosed breakers utilizing the deionizing principles for opening circuits of moderate short-circuit current. The development of enclosed air breakers which will interrupt currents up to 120,000 amperes root-mean-square and which will have universal application to both d-c and a-c circuits of 750 volts and below, has however necessitated a new form of interrupting device. In addition, the unusual stresses of such high short-circuit currents have made necessary the development of a new breaker itself for the purpose of minimizing contact burning, increasing interrupting speed, decreasing size, and improving mechanical adequacy. Both the interrupting chamber and the breaker are described in this paper.

II. Deionizing Interrupter

Interruption of high short-circuit currents with scant outward display and virtual freedom from external ionized flame requires a confined arc which dissipates the least possible energy. Although the a-c circuit may be opened with a theoretically very small energy dissipation by rapidly deionizing the arc path near a normal current zero,[1] the d-c circuit discharges an amount of energy in the arc somewhat greater than that stored in the circuit electromagnetically when it is interrupted.[5] The d-c energy dissipation and arcing time can be minimized by developing the highest permissible arc drop which will not endanger the circuit insulation immediately upon separating the contacts, and sustaining this value until interruption is complete. The arc drop which is the product of volts per inch and inches length can be achieved either by considerable lengthening of the arc or by the development of a high voltage gradient along its axis. The latter means is preferable first because the necessary arc drop can be developed most quickly by the introduction of deionizing means to increase the voltage gradient, and second because a smaller structure is required if the arc is kept short. The product of arc length and gradient should be kept substantially constant to avoid overvoltages. It is also necessary that the breaker contacts mechanically separate at high speed in order to provide immediately the necessary arc length.

For best interruption of the a-c circuit, the arcing time should also be kept small. On high-voltage circuits it is preferable to permit the arc to continue to a normal current zero, at which point the interruption is completed. On low-voltage circuits, however, it is also practical to interrupt the circuit in a d-c manner by bringing the current to zero with a limited arc voltage. The power loss in the arc may be increased if this is done, but since the arcing time is shortened the energy dissipation with high current and low circuit voltage will not necessarily be increased.

The usual methods of interrupting an arc do not possess all of the characteristics enumerated. Simple lengthening of the arc with a magnetic field does not sufficiently confine it nor limit its length. Furthermore, the volt-time characteristics of magnetically driven arcs do not have the desired shape for d-c interruption. Finally, magnetic-blowout breakers are notoriously noisy.

Interruption by means of gas blast has been very successful in the operation of fuses. Such means are peculiarly difficult to apply to air breakers however, because of the necessity of almost complete restriction about the contacts. Furthermore, gas-blast devices are generally noisy and have a limited life since material is eroded away in forming the necessary gas.

The deionizing arc chamber[2] meets the requirements of a-c interruption very successfully, and is more efficient than other methods in common use. It is not applicable for currents as high as 100,000 amperes, and because of its principle it is not adaptable for the interruption of the higher-voltage d-c circuits.

An arc may be driven into a narrow slot formed by walls of non-gas-forming insulating material, and if sufficient venting is provided for the gases together with sufficient constriction of the arc, considerable arc drop can be developed. Slepian[6]

Paper number 39-34, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 19, 1938; made available for preprinting December 30, 1938.

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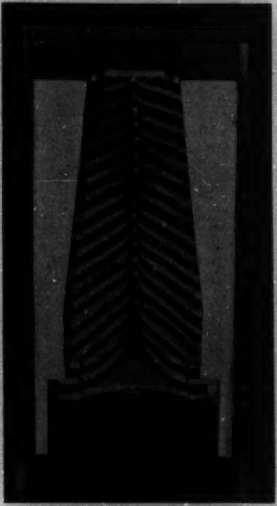
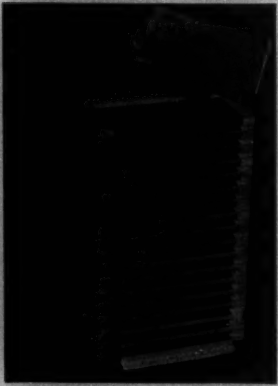
[1] For all numbered references, see list at end of paper.

has published data showing that in a one-eighth-inch slot, 800 root-mean-square volts per inch can be interrupted. Deionization chambers made with this principle do, however, fail to limit the arc length and voltage drop, and present problems in venting with very high currents.



(*a*) Bottom view showing arrangement of insulating plates in arc chamber



(*b*) Top view showing location of iron plates

Figure 1. Arc chamber

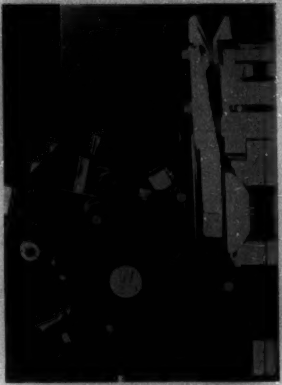
During experiments with arcs in narrowly slotted plates, spaced apart to allow venting, it was found that if the upper part of the slot were closed the magnetic field would drive the arc to the top of the slot, after which the arc core would remain stationary but the field would drive gases turbulently through the arc and deionize it. The construction is similar to one described by Slepian in 1933.[7] In principle the magnetic field acts on the electrons which in turn furnish the gas particles with a resultant velocity by bombardment. Hereby a strong blast of gas is passed through the slots and the arc must, therefore, ionize fresh gas in considerable quantities to maintain a conducting path.

The arrangement finally developed consists of a large number of non-gas-forming insulating plates with V-shaped slots as shown in figure 1. They are spaced with intervals at right angles to the arc path mainly to provide free venting space to the top of the arc chamber for escape of arc gases, thus reducing back pressure which would tend to direct ionized gases downward. It will be noted that the slots are quite narrow at the upper end, while at the lower end they are widened out considerably. This provides room for movement of the arc tips and contact arm. The constriction of the arc in the slots is also important in causing the arc to assume a section which makes the magnetic and gas action most effective.

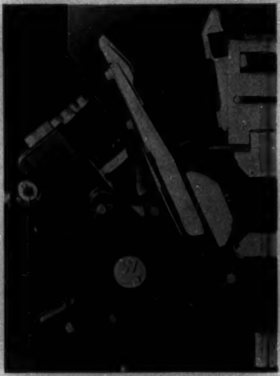
For convenience, the term "magnetic blast" has been adopted to describe the action, and the device is called a magnetic-blast interrupter.

The magnetic field may be provided by a series coil, but it has been found that an adequate field may be self-induced by building the chamber to contain iron plates as shown in figure 1. With the exception of the actual arc tips it is necessary to keep the arc from striking any metal parts in the arc chamber. Unless this is done the volatile metal resulting from contact with the arc interferes with the deionizing action; and the display of molten metal is objectionable. To obtain this result the iron plates used for intensifying the magnetic field to create the blast are located directly above and in the same plane with the insulating plates so as to be definitely out of the arc path.

The interrupting chamber built in this way limits the arc length to a definite value. During d-c interruption, the field is strongest when the current is high at the beginning of the interruption. Consequently, both the deionizing action produced by the magnetic-blast effect of the field and the ionizing action produced by the heavy current in the arc are initially high. As the current decreases, both ionizing and deionizing forces subside and the voltage gradient of the arc remains substantially constant. Since the length is limited and constant when the full value is reached, the arc drop as a function of time approximates the desired curve shape. There is no high overvoltage at the instant of interruption.



(*a*) Closed position



(*b*) Open position

Figure 2. Circuit-breaker contact construction

During a-c interruption, an early current zero is forced, but the arc drop is not high until the end of the arcing half cycle because of the normally sinusoidal current and its effect on the field. There is only one half-cycle of arcing except when the

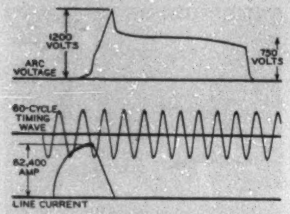


Figure 3. Oscillogram showing interruption of 62,400 amperes at 750 volts direct current

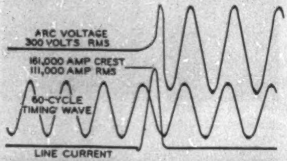


Figure 4. Oscillogram showing single-phase interruption at 111,000 amperes, 300 volts, 60 cycles

contacts part just prior to current zero, in which case the time will be slightly longer.

The arc is well enclosed by the chamber, and the gases which pass between the plates and out the top of the chamber are deionized by the plates so that very little flame passes out of the top. Breakers equipped with these interrupters have been tested in enclosures with the top surface only one inch above the arc chamber, and interruption was satisfactory. The chamber is effective as a muffler, and noise is greatly reduced by its use.

The voltage drop of the arc in the chamber can be as much as 350 volts per inch direct current, and 550 volts per inch root-mean-square, alternating current. Currents as high as 120,000 amperes root-mean-square have been interrupted satisfactorily.

III. Air Breaker Construction

An air breaker designed to handle considerable current must incorporate a plurality of contact pairs in order to isolate properly the functions of carrying load current and interrupting short-circuit current. If high short-circuit currents are to be interrupted, it is necessary to use three sets of contacts. The first set, or main contacts, serves only to carry load current. In order to make the over-all dimensions of an air breaker as small as possible for a given current rating, it is necessary to use materials and constructions for the main contacts which will carry the load currents with as little ohmic loss as possible.

The most satisfactory structure for this purpose has been found to consist of an upper and lower main-contact stud constructed of copper with a solid copper bridging member, which breaks contact when the breaker is opened. The effectiveness of the contacts is greatly increased by the use of silver plates brazed on to the contact faces of both moving and stationary members. The silver should be in the form of a thick plate rather than thin electroplating, in order to provide long life and freedom of maintenance. Sufficient spring pressure must be used to insure a low contact drop with normal load current. It is impractical and unnecessary to utilize spring pressures so high that the main contacts will not be blown apart by heavy short-circuit currents, because in the event of short circuit these contacts must part as the breaker opens; and other parallel contacts must, therefore, be so designed that the main contacts will be well protected.

High-pressure silver line contacts are superior to low-pressure silver surface contacts. As shown in figure 2, the line contact is obtained by machining the face of one silver plate to have the profile of a segment of a circle. Proper alignment of the contacts is necessary to ensure good line contact. This has been achieved by placing the silver at a 45-degree angle on the main contact studs and machining the bridging member so that the contact lines lie on a cylindrical surface. The cylinder will properly align itself with the two planes formed by the stationary stud contact members as the bridge member is somewhat free to slide into proper position.

This main contact construction has proved so effective that the over-all dimensions of the new breaker which will handle 1,600 amperes are no larger than breakers of earlier form, using laminated brush construction, which could carry only 800 amperes with the same temperature rise.

In order to protect the main contacts from being burned as the breaker passes and interrupts heavy short-circuit currents, it is necessary to use a set of protective contacts and a separate set of arcing contacts, as shown in figure 2. The contacts must obviously separate in proper sequence, that is the main contacts part first, then the secondary or protective contacts, and finally the arcing contacts. In order to provide for the proper mechanical sequence of operation it has been usual practice in air-circuit-breaker designs either to make the contact-carrying arm a flexible member or to provide in it a pivoted joint and springs mounted to hold the contacts closed. The flexible-arm construction is not satisfactory since it is too readily bent by magnetic forces associated with extreme short-circuit currents, with the result that the arcing and protective contacts may open before the main contacts. A rigid contact arm, when provided with a separately moving member which carries the arcing contacts and the necessary springs, is difficult to design in a neat manner and also has the disadvantage that its weight and inertia are considerable, which results in a decreased mechanical speed of breaker opening. As shown in figure 2, the necessary relative motion of the secondary and arcing contacts has been provided by a movable platform member on a stationary-contact side of the breaker. As the breaker opens, this platform member moves directly with the contact-supporting arm in such a way that the secondary and arcing contacts do not slide on each other or separate. The necessary freedom for the moving platform member is provided by a slot in the side plates which support it. After a sufficient degree of motion has taken place to separate completely the main contacts, the pin which is a part of the platform member reaches the end of the slot in the side plates and the secondary contacts part. With further breaker motion the platform member pivots about this pin, the secondary contacts on the stationary side move backward, and the arcing contacts remain closed until the stop at the upper portion of the platform member reaches its final position. At this point the arcing contacts separate.

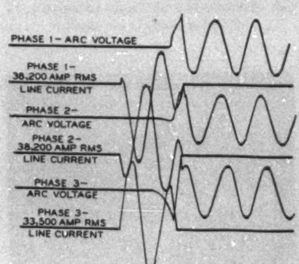


Figure 5. Oscillogram showing interruption of a three-phase short circuit at 600 volts, 60 cycle

By means of this construction it has been possible completely to enclose the springs and shunts and to provide a simple design unusually neat in appearance.

The secondary-contact material must be chosen to resist some arcing, but at the same time the contact drop must be kept low in order to provide necessary protection for the main contacts. A tungsten alloy has been used. The arcing contacts must be very resistant to damage from the arc, and a similar tungsten containing a higher percentage of tungsten has been used. This material is much superior to carbon since the contacts may be made smaller, more free from breakage, and have better thermal capacity and conductivity.

It is important to use adequate shunts for connection between the secondary and main contacts in order to transfer the current from the main contacts to the secondaries without burning the mains. The shunt, contained within the moving platform member, is inverse in form, so that the heavy short-circuit current will tend to close the secondary contacts tightly together. The current path to the arcing contacts cannot be constructed in this way since the magnetic field must be such as to blow the arc upward from the arcing contacts when it is formed. Consequently, the spring pressure has been made adequate to keep the arcing contacts together under sufficient pressure, even against the effect of very high short-circuit currents.

Figure 6. A complete electrically operated circuit breaker

To protect completely the main contacts from the effect of the arc between the upper contacts, a pair of horizontal overlapping baffles were inserted directly below the secondary contacts. Any gases moving downward are consequently deflected and there is no danger of the arc striking between the mains when interrupting heavy currents.

It is pointed out in section II that high mechanical speed of opening is necessary to minimize the energy which must be dissipated in the interrupter. This has been achieved in the breaker by making the moving contact arm and its associated members as light as possible and by using heavy springs to accelerate the breaker initially. With short-circuit currents in the higher brackets complete operation, which includes tripping of the breaker, mechanical opening, and arc extinction, takes place in a single half cycle. Even in the case of lower short-circuit currents, the operating speed is unusually fast and complete operation does not ordinarily exceed two cycles.

To withstand the forces imposed upon the structure as the result of extreme short-circuit current and the high opening speed, an air dashpot shock absorber was designed to absorb the kinetic energy of the moving-contact arm. Without a device of this kind rebound was noted. The dashpot, however, is so effective that rebound is eliminated. At the same time the stresses are greatly reduced, so that no part is in danger of mechanical breakage.

IV. Interrupting Tests

To obtain the necessary range in current at 600 to 750 volts direct current, test apparatus consisting of four 1,500-kw generators was used. D-c tests were made on single-pole breakers with currents ranging from only a few amperes up to and including 62,000 amperes (table II).

Both single-phase and three-phase short-circuit tests were made at 440 and 600 volts, 60 cycles, using a test set consisting of a three-phase 3,000-kva bank of low reactance transformers. These tests ranged in current values from a few amperes up to 120,000 amperes root-mean-square with crest values over 200,000 amperes. However, in order to obtain currents above 45,000 amperes the low-voltage windings of the transformers were connected in parallel, causing a reduction in voltage to approximately 300 volts. Since all tests above 45,000 amperes were made single phase with 300 volts across the pole, they were equivalent in effect to similar three-phase tests at 520 volts (table I).

Table I. A-C Tests

Test Number	Volts, 60 Cycles	Kind of Test	Actual Current Measured by Oscillograph		
			Root Mean Square		Crest
Single phase					
1	575	O	4,750		7,900
2	575	O	7,900		11,200
3	575	O	22,300		34,000
4	575	O	32,500		52,000
5	288	O	46,000		72,000
6	288	O	65,000		92,000
7	288	O	76,000		112,500
8	288	O	111,000		161,000
9	288	O	124,000		204,000
Three phase			Phase 1	Phase 2	Phase 3
10	600	O	34,400	42,200	36,600
11	600	O	38,200	38,200	33,500
12	600	CO	13,800	14,800	17,700
13	600	CO	28,800	30,200	24,000

Table II. D-C Tests

Test Number	Volts, 60 Cycles	Kind of Test	Actual Current Measured by Oscillograph
14	750	O	20,000
15	750	O	36,900
16	750	O	62,400

The oscillograms, figures 3, 4, and 5, show how rapidly and smoothly the arc voltage increases and the effective arc-extinguishing action obtained by this means. The arcing time is definitely limited to one-half cycle or less and this coupled with fast breaker action provides an over-all operating time varying from three cycles at low short circuits to one-half cycle at very heavy currents.

The use of the new deionizing interrupting chamber greatly reduces the amount of noise and disturbance which ordinarily takes place when heavy short circuits are opened by air breakers. The ionized flame which ordinarily accompanies interruption was also greatly reduced, and interrupting tests with these breakers in metal cubicles of dimensions very little larger than that of the breaker itself indicated no danger of flashover between phases or to ground.

V. Conclusions

Utilizing the new form of arc-interrupting device in conjunction with an air circuit breaker improved both electrically and mechanically in fundamental respects, has made it possible to build an air circuit breaker of comparatively small size which will interrupt short-circuit currents up to 120,000 root-mean-square on alternating current, or 62,000 amperes on direct current. The complete breaker electrically operated is shown in figure 6. The noise and demonstration which are usual in air-circuit-breaker practice have been greatly reduced. The ionized flame has also been reduced to an extent that these breakers can be compactly mounted in steel cubicles without the necessity of providing unusual barrier arrangements or dead space above the breaker for taking care of the ionized gases.

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Discussion

Charles P. West (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Messrs. Ludwig and Grissinger have described the latest advance in the circuit-interrupting art. It is another step in the series of breaker improvements founded on Doctor Slepian's fundamental concepts of arc control. The commercial developments arising from these early theories are affecting not only arc-interrupting devices, but the supporting switchboards, structures, housings, and other associated gear as well.

This new breaker design makes it possible to meet the ever increasing demands for reduction in the size of structures. The ability to carry and interrupt current in a given space has been greatly increased. For instance, it is now possible to house three manual three-pole 600-volt 1,600-ampere 60-cycle 40,000-ampere-interrupting-capacity metal-enclosed drawout circuit-breaker units in a space 26 inches wide and 90 inches high. A three-pole drawout unit for 2,000 amperes is shown in figure 1 of this discussion. It requires a width of 30 inches and height of 45 inches. Figure 2 shows the rear of this truck. Note the liberal design of the primary contacts. The safety interlocking features common in metal-clad gear are provided. Three-thousand ampere units are the same width and 60 inches high. Such compactness is now possible because so little clearance over the arc box is necessary and the new breaker frame is much smaller.

Figure 1

Figure 3 shows a structure for 2 600-ampere, 12 1,600-ampere manual, and 4 2,000-ampere electrically operated units. Semiflush instruments and relays for two generators, two transformer banks, a bus tie breaker, and 16 feeders are provided. Two three-phase reactors are mounted in the superstructure. Housing these high-current-capacity drawout breakers in limited spaces requires liberal ventilation. This feature must be carefully watched in the design of the structure and bus compartments. Another factor demanding attention is the locating of the copper for the bus and connecting circuits. As the breaker units get smaller, the width per circuit decreases and it is more difficult to provide accessibility and the required electrical clearances.

Permissible temperature rise in apparatus is basically determined by the top operating temperatures which the materials used can stand continuously without damage. Different classifications are recognized and maximum temperatures determined by the design features of each. As materials and finishes are improved, apparatus can be safely worked at higher temperatures. For instance, bus temperatures are limited so that no trouble will occur in joints. Silver plating eliminates harmful oxides and higher temperatures might well be permitted. Air breakers of the type described by Messrs. Ludwig and Grissinger employ silver-faced solid contacts. Operating temperatures for such a design are less than 30 degrees centigrade and need never be limited to the 20-degrees-centigrade rise often thought desirable for laminated brush breakers. In fact, no serious or permanent damage would result from emergency operation considerably above their rating.

As the performance improves, the size decreases and safety factors are increased, air breakers are finding wider application in fields such as central-station auxiliary service. Here the utmost in reliability is demanded. Low-voltage metal-clad switchgear, as illustrated, provides the safety, interchangeability, reliability, compactness, appearance, and trouble-free operation which are essential to the perfection in design for which central-station engineers are striving.

These various points are discussed to emphasize that, as breaker design progresses, the associated gear keeps pace with it so that the full benefits are made available to industry.

E. A. Childerhose (Jackson and Moreland, Boston, Mass.): The manufacturer is to be congratulated on having taken another successful, even if tardy, step in the development of air circuit breakers. The advantages of air circuit breakers over oil circuit breakers are so numerous that the latter (all voltages) should be obsolete within the next ten years, with the possible exception of special applications in mines and similar locations where the explosion hazard exists and the use of any open arc is prohibitive.

The designers of this new type of breaker have done a commendable job in reducing the space requirements, and have thereby earned the blessings of many a designer, harassed by limited space in which to locate his switchgear. They have incorporated features found in some older breakers, discarded some of the old features, and developed new ones. They have succeeded in developing a snappy, high-speed mechanism that is in many ways superior to the old-style breaker which it supplants. In doing so they have undoubtedly had to compromise between the best and something that is good enough to do the work in order to accomplish the major features desired in the new breaker. These, and the absence of features familiar to the operating man in the old style breakers, naturally raise questions in his mind as to why they were done or omitted. He knew the limitations of the old equipment, and now wants to know what the limiting features of the new will be.

Gone is the familiar brush contact and in its place a silver line contact. Instead of a multitude of small surface contacts is a single line contact. A little bit of arcing

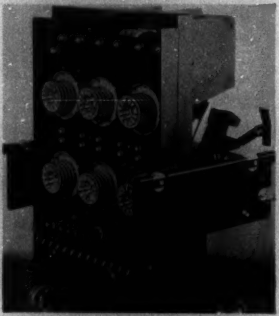

Figure 2

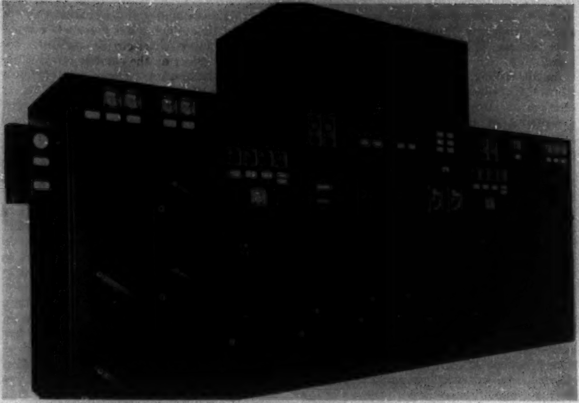

Figure 3

might render a couple leaves of the old brush ineffective, but it reduces the new contact from a line to two points, or possibly even only one! What effect has this on the current-carrying capacity? Will it not be materially reduced, and will there not be an increase in the probability of overheating when the breaker is carrying rated, or even normal operating loads? Silver has a lower melting point than copper and is an excellent solder. There appears to be considerably more danger of a slightly pitted line type of silver contact freezing under moderate overloads, or when opening small-magnitude short circuits, than there is with the brush type of contact.

The spring pressure on the main contacts is not sufficient to prevent them opening under heavy short circuits. The current then travels through the shunt around the bottom contact, and the secondary and arcing contacts. Is the reactance of these shunt circuits sufficiently low to prevent arcing across the main contacts? It is again asked, "What will be the effect of arcing on these silver line contacts?" This is an important consideration when the breaker is adjusted for delayed opening, though because of high operating speeds it may not matter when the breaker is set for instantaneous opening.

The contact pressure of the single line contact has been increased over that used in the brush in order to make it carry rated load. How will it compare when carrying overloads? It is questionable that it has as great an overload capacity.

The authors show oscillograms of the breaker opening 124,000 amperes on an "O" test. How does this compare with the standard "two CO" test for oil circuit breakers? What would the comparative interrupting capacity be? The "two CO" test is such a common reference that many are apt to assume that the currents reported are on that basis, though the authors have made no attempt to intimate such a rating. In what conditions were the contacts after breaking these high currents? Was the breaker operable and capable of carrying rated load after the tests? What was the rating of the breaker tested? Is it to be assumed that all sizes of this new breaker are capable of interrupting this amount of current?

When you buy a car you can test it on the open road and determine the utmost it can produce in speed. When you buy a breaker the manufacturer tells you what the interrupting capacity is, but you are unable to test it for yourself. When you are told that these new breakers can interrupt 124,000 amperes, is that all that they can interrupt, or is there considerable margin of safety in that figure? When you use steel, or wood, or concrete in a structure you know their strength fairly accurately, and can apply safety factors, depending upon the use being made of the material. Why cannot similar information regarding circuit breakers be obtained from the manufacturers, and the purchasing engineer determine the safety factor which should be used for any particular installation?

The paper states that these breakers may be used in cubicles of dimensions very little larger than that of the breaker itself. This brings up the question of temperature rise in such a small confined space. Little is known of this feature to the operating man or to the station designer, and less has been published regarding it. A frank discussion by the manufacturers, and dissemination of their test data on temperature rises in enclosed metal-clad switchboards would be of interest to many. More knowledge of the data used in the recently increased temperature rises permitted by the National Electrical Manufacturers Association would be welcome.

It is acknowledged that silver contacts may be safely operated in excess of 70 degrees centigrade, the limit for copper, but that will increase the ambient temperatures within switch cubicles. Will the higher ambient endanger insulation, will it injure instrument transformers and other equipment which present standards permit being operated at their safe maximum operating temperature in a 40-degree centigrade ambient? It may be that we are not taking full advantage of the possibilities afforded by silver contacts.

This discussion has resolved itself into a series of questions. But they are all questions that a prospective user of new equipment wants answered before he decides to purchase. It is hoped that Messrs. Ludwig and Grissinger will see their way clear to answer them.

The paper brings out another point that is of poignant interest to engineers and the country as a whole, namely, the increasing use of silver in industry. Is it not time that the artificial price of silver were abolished and the metal allowed to assume its true position in industry and the life of the country?

J. W. Seaman (General Electric Company, Philadelphia, Pa.): It is with considerable pleasure that I take this opportunity to commend Messrs. Ludwig and Grissinger on an able presentation of a subject which is of considerable interest, particularly so, to those of us who have been engaged in the pursuit of similar interests for the past several years.

The trend toward metal switchgear has long been recognized by those of us engaged in the art, and I believe I can justifiably say, has been considerably furthered by the work my associates have done during the past several years, although we have not especially publicized the results obtained.

Requirements

To design and build air circuit breakers suitable for metal-clad gear several requirements must be met as borne out by Messrs. Ludwig and Grissinger's presentation.

The first of these is a reduction of size. The use of solid-silver line contacts has been an important factor in accomplishing this, as the authors have pointed out. I should be the last to belittle the undoubted advantages of this construction inasmuch as it has been standard in our designs for some *eight years*. The use of silver alloys for arcing tips is another step in this direction and is now, I believe, quite well recognized.

We engineers would be remiss in our duties if we allowed the reduction in physical size, gained largely by the adoption of a solid-silver contact construction, to be penalized by using (for interrupting means) the conventional plain-break construction. Such means, as we know, demand a prohibitive air space over the contacts when enclosed, in order not to impair the current-rupturing ability.

The necessity for some suitable arc-controlling means has, therefore, long been recognized. If such means is properly engineered, it will (as we have demonstrated through several years of practical application) provide other advantages far greater than the mere saving of space.

Interrupters

Messrs. Ludwig and Grissinger have described a particular arc-controlling means. Happily, as borne out by their presentation, the field in this direction is not particularly limited. There are other equally advantageous means. The one with which I am most familiar is known as the pin-type arc quencher.

The type of interrupter described by Messrs. Ludwig and Grissinger functions it seems, due to what we may call a resistance characteristic, that is, the arc resistance is increased as a result of decreasing the diameter of the arc core as it travels into the V-shaped slots. To obtain successful operation, Messrs. Ludwig and Grissinger have recognized the necessity of obtaining venting in order to reduce the possibility of developing back pressure sufficient to keep the arc from entering the slots. If the arc is not allowed to enter the restricted slots, this device would apparently become totally ineffective. Apparently the size of the enclosing housing may, therefore, have an important effect upon the performance of this device. The magnetic-blast theory attributed to this interrupter we will leave in the capable hands of Messrs. Ludwig and Grissinger. I believe it sufficient to say that the evidence presented indicates successful operation within the apparent limits of the testing facilities.

The arc-controlling means with which I am particularly familiar is an arc-chute structure comprising a multiplicity of pins through which the arc is driven by magnetic means. This device functions due to the efficient presentation of the arc to large cooling surfaces in combination with a multiplicity of cathode drops. That it has no particular voltage or current limitations has been demonstrated by numerous tests. The arcing time averages less than one-half cycle over current ranges up to and including 125,000 root-mean-square amperes at 600 volts, both single phase and three phase. Due to its freedom from restriction troubles it has been successfully applied over the entire range of air-circuit-breaker sizes during the past three years.

With these test data and operating experience available, we are naturally gratified to find that Messrs. Ludwig and Grissinger concur with us in recognizing the necessity for suitable arc-controlling means, to greatly reduce the usual disturbance associated with the opening of arcs in air, and permit retaining the space advantages already gained by the improvements in contact structure construction.

There is another phase involved in satisfactorily meeting this trend toward metal-clad gear with modern air circuit breakers. This is perhaps not as well appreciated as the phases we have just been discussing but is rapidly assuming greater importance. The increasing demand for electrically operated devices reveals that most present-day air circuit breakers require considerable additional space in order to obtain electrical operation. In the line of air circuit breakers with which I am most familiar, this problem is being met by designs which inherently require only the same small space for electrical as for hand operation. The reception accorded these new breakers clearly indicates that this last trend is not to be lightly ignored.

In concluding, let me again congratulate Messrs. Ludwig and Grissinger on the introduction of their new breaker. With the awakening of new interest even greater improvements in the art, I am sure can be made in the future.

G. G. Grissinger: Mr. Seaman, in his discussion, indicates that the size of the enclosure may have an important effect on the interrupting performance of this device. Recognizing that a limitation of this sort might exist, very thorough tests were made using an experimental enclosure having an adjustable top which could be raised or lowered so as to vary the distance between the top of the breaker and the top of the enclosure. These tests proved conclusively that breakers of this type may be very closely confined without impairing their interrupting ability, since with the top of the enclosure only one inch above the top of the breaker, three-phase short circuits both "O" and "CO" as high as 97,000 amperes root-mean-square were interrupted satisfactorily.

Mr. Childerhose submits a number of very interesting comments. Most of the questions raised can be answered best by reference to test results.

Theoretically, it would appear that a laminated copper brush with its multitude of point contacts would offer much less resistance to the flow of current and, therefore, be more efficient than a solid butt contact. The solid contact, however, can be made much shorter than a brush and with adequate spring pressure behind them the solid silver faces provide a very effective contact-making means. Although the millivolt drop across the contacts may be slightly higher than that of a corresponding brush at the same current, the gain in reducing the length of the current path more than makes up for the difference. Furthermore, a much more durable silver surface can be applied to the faces of solid contacts.

Mr. Childerhose raises a question concerning the effectiveness of the solid contacts after a heavy or a moderate current interruption. Practically no pitting of the silver contacts occurs except when interrupting very heavy short-circuit currents. Temperature tests on these breakers after a series of interrupting tests ranging from small currents to over 100,000 amperes had been made, showed only a few degrees increased temperature rise, even though the breaker had received no maintenance whatever. Consequently, it is apparent that the slight pitting which takes place when interrupting heavy short-circuit currents has very little effect on the current carrying capacity of the breaker. In no case did "freezing" of the silver contacts occur.

As Mr. Childerhose points out, a number of factors influence the protection received by the main contacts. Among them are spring pressure, resistance and reactance of the shunt circuits, and the conductivity and design of the protective contacts. Consequently, all of these factors were given careful consideration in this development. As a check on the final arrangement chosen, additional short-circuit tests were made in which the circuit breaker was blocked so as to prevent tripping. These are ordinarily referred to as short time or five-second tests. The current after reaching a steady-state condition, measured approximately 42,000 root-mean-square amperes and the breaker on which the test was made was rated at 1,600 amperes. This breaker also carried rated current after these tests, with no maintenance, at a temperature rise only three degrees higher than that previous to the tests.

Due to the relatively low thermal capacity as compared with large rotating machines or transformers, circuit breakers are maximum-rated devices, that is, it is not intended they carry more current than their rating for an appreciable length of time. However, the greater durability of the solid contact at higher temperatures makes it eminently better suited to carrying continued overloads than the laminated copper brush which would be severely damaged by excessive temperatures.

The standard interrupting duty cycle for air breakers, as defined by AIEE Standards No. 20, comprises an "O" followed at a two-minute interval by a "CO" test. The 124,000-ampere test referred to by Mr. Childerhose was made on a 3,000-ampere a-c breaker and was an "O" test only. However, many tests have been made both "O" and "CO," particularly on the smaller frame sizes and while the short-circuit currents on these did not exceed 97,000 amperes, three phase, this value nevertheless represents a considerable factor of safety over NEMA standards, which specify 40,000 amperes for a 1,600-ampere breaker and 60,000 amperes for a 2,000-ampere breaker.

In all cases, after completion of the tests, the breakers were in condition to carry rated current without maintenance, although of course the temperature rises were two or three degrees higher than that of a new breaker.

High-Power "De-ion" Air Circuit Breaker for Central-Station Service

R. C. DICKINSON
ASSOCIATE AIEE

Synopsis: The "De-ion" principle of arc interruption in air has been applied over a wide range of a-c services. Difficult switching problems have been met by them. A new breaker has been developed and tested in excess of 37,000 amperes for the 15-kv powerhouse class. It may be supplied for masonry or steel-cell mounting or as part of complete metal-clad switching equipment.

DE-ION air circuit breakers have met some outstanding problems in moderate-voltage switchgear in the past eight years. Since this type gives highly satisfactory operation under severe duty conditions without oil, water, compressed air, or other arc-extinguishing fluid, it is only natural that pressure has been brought by leading users for the extension of this line in the powerhouse field. The purpose of this paper is to describe an important addition to the line tested in excess of 37,000 amperes for the 15-kv class and providing continuous carrying capacities up to 4,000 amperes.

To recapitulate some important services now being performed by De-ion air circuit breakers:

They are giving consistently reliable protection against short circuits up to 500,000 kva at voltages from 12,000 to 16,500, in well-distributed locations throughout the country.

They are handling with a minimum of maintenance, reversing motor service in steel mills in which annual individual breaker operations in excess of 50,000 have been recorded.

Equipped with a special high-speed tripping arrangement, they are opening difficult short circuits on rectifier installations at 12 kv with an over-all breaker time of approximately three cycles (60-cycle wave) and have eliminated potential breakdown such as occurred previously under less effective protection.

In one application they are installed against short-circuit capacities of 1,500,000 kva at 24,000 volts, and giving satisfactory service.

In another installation they are handling satisfactorily contact line circuits in 11,000 volt, 25-cycle, single-phase railway service in which short circuits up to 50,000 amperes are possible and these faults are cleared from the system in less than one cycle.

In view of the gratifying performance of breakers of this type in 15-kv powerhouse service up to 500,000 kva, as exemplified in the hundreds of units in use, a large number of which have been in service for several years, development directed toward extension of the fundamental principles to higher interrupting ratings for powerhouse duty followed as a logical sequence. Such extension involved a new conception of contact arrangement not only to provide the higher continuous-current capacity, but to incorporate as well the ability to close in air upon fault currents up to 100,000 amperes crest, to carry these currents until called upon by relay action to interrupt and, finally, to transfer the heavy currents involved to the deionizing chamber without damage to the main or auxiliary contact members. Development into new ground to obtain higher interrupting capacity was necessary, involving means for moving the arc promptly from the contacts to the deionizing chamber, introducing voltage into the arc in steadily increasing steps and assuring speedy, positive interruption. The results of this development are incorporated in a design, set forth in this paper, capable of greater interrupting duty than any circuit breaker available today not requiring liquid dielectric or other form of stored interrupting medium.

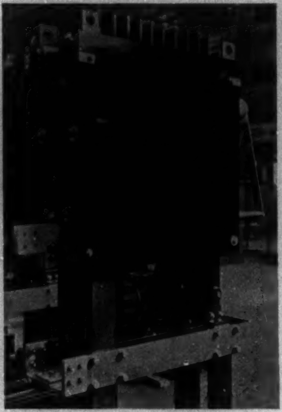

Figure 1. Pole unit of new De-ion air circuit breaker for generation-station service

Description

Figure 1 shows a completely assembled pole unit with a continuous current rating of 2,000 amperes, placed in service last year. Three thousand-ampere breakers are now being installed and a 4,000-ampere design is available for applications that may require it. All of these current ratings have the same general form except for the current-carrying members. General features of the breaker follow closely the vertical arrangement of elements proved successful by several years' experience with smaller breakers. Mounted on insulated posts of high mechanical strength, the contact linkage is operated from below through an insulating rod pushing upward to close the contacts, with the deionizing chamber mounted at the top of the structure immediately above the parting contact surfaces. The particular structure shown permits a wide range of line connections. Inherently, the breaker is adapted to rear connection, entering either horizontally or vertically, but an additional terminal is supplied to permit one vertical front connection in place of either of the other two. All terminals are arranged for standard bus-bar connections.

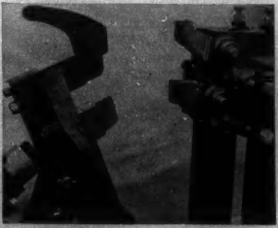

Figure 2. Finger contacts are a radical departure from conventional air-circuit-breaker design. Peak currents in excess of 100,000 amperes were closed with this type of contact during "CO" tests

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Paper number 39-35, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 29, 1938.

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Table I. Single-Pole Interrupting Tests With Separate Closing Breaker, 15-Kv De-ion Air Circuit Breaker

Test Number	Root-Mean-Square Amperes Interrupted	Breaker Time	Root-Mean-Square Volts Restored
1	4,600	4.2	12,700
2	20,500	4.0	11,800
3	25,000	3.8	11,700
4	30,000	3.9	11,800
5	30,400	3.9	12,000
6	32,200	4.1	11,800
7	32,200	3.8	11,600
8	34,000	4.1	12,000
9	34,600	3.9	11,600
10	41,500	3.8	12,200

As indicated by figure 2, the contact elements of this breaker depart from previous air-circuit-breaker design to permit extending their function as outlined previously. The finger type of contact here shown carries all the advantage accruing from their use in conventional breakers together with the incorporation of features particularly adapted to air-breaker construction. These contact fingers are arranged in two individual pairs, each with its own entering member. The lower set comprises the main current path while the upper set is designed for arc-drawing purposes. Both pairs of fingers consist essentially of parallel bars so arranged as to permit of ample flexible shunts being extended to the line terminal in a straight line thus avoiding local loops with their accompanying current forces in the current path. The fingers are biased toward each other by heavy compression springs with a definite stop maintaining them at proper separation for entry of the moving member. In addition to the self-adjustment permitted by the flexible shunts, the fingers are in effect pivoted in the contact-supporting casting so as to be further self-aligning within the limits necessary to prevent stubbing of contacts upon closing. The main fingers are wide and rugged with sufficient overlap of the entering member to provide the liberal contact surface necessary for high continuous and momentary current duty. The mechanical and thermal sufficiency of this design was well proved by numerous tests in which the fault currents were as high as 104,000 amperes.

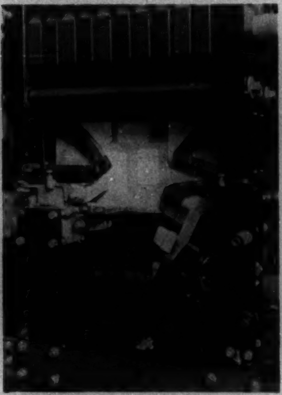


Figure 3. The shape of the arcing contact finger assists in movement of the arc upward to the arc horn without detracting from the ability to close high currents. The arc horns are spaced laminations for free venting and the contour gives rapid motion of the arc terminals

New features are incorporated in the arc-drawing pair of fingers. The parallel conducting bars forming the fingers are of high-strength high-conductivity alloy specially heat-treated and only recently made available in this form. An important feature of this pair of fingers is the shape of their contact surfaces. In the fully closed position these surfaces are such that the current path through them (in an elevational view) is substantially in a straight line with the parallel bars and shunts. The lower portion of this surface is cut away so that as the contacts open, the point of contact moves upward making the last point of contact well above the body of the fingers and introducing a sharp local loop in the current path at the instant of initiating the arc.

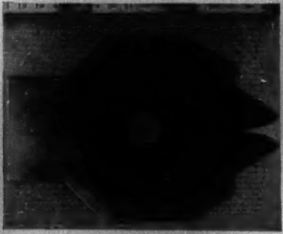


Figure 4. Copper and steel plate combination used in the new breaker

The magnetic force produced by this loop, not present in the fully closed position, assists materially in moving the arc rapidly from its point of origin on the contacts to arcing horns provided for the purpose and reduces to a minimum the burning on the contact surfaces. Both stationary and moving arcing contacts are faced with silver-tungsten, arc-resisting alloy. The moving element, essentially a plate, is formed with a horn to assist the arc in moving upward toward the deionizing chamber, even while the amount of contact separation is still very small. This horn also carries arc-resisting alloy on its upper surface.

This type of contact has several advantages for operation on high currents. Speed of contact separation is an important factor in air-breaker performance. The amount of contact movement afforded by finger-type contacts before actual parting occurs, decreases the accelerating force necessary for a given speed of separation and this decrease in accelerating force also decreases the closing effort, a vital factor in heavy-duty breakers. The finger type of contact also has the property of resisting the tendency to be biased toward the open position under short-circuit conditions which again is reflected in decreased closing effort.

Figure 5. Oscillograms were made of all interrupting tests. The above film shows typical performance at high current values. The short circuit was closed by a separate breaker

The contact-supporting casting is divided into two parts one of which supports both pairs of stationary fingers with their shunts. This contact supporting section may be removed from the main shell of the casting thus permitting maintenance work on the fingers without disassembling other parts of the breaker. As shown by figure 3, a side of the arc box may be removed for a more detailed inspection of this portion of the breaker while the contacts remain in their operating position. It will be noted that the space between the contacts and the lower

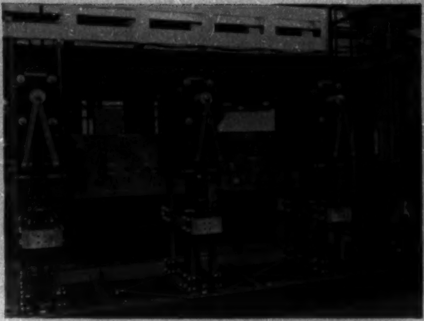


Figure 7. Designed to meet particular spacing requirements, this unit illustrates the adaptability of this breaker to meet special conditions

end of the deionizing chamber is relatively deep and that the horns provided for arc travel through this space are proportioned to provide a gradual lengthening of the arc, and with it a steadily increasing arc voltage, until it is at the point of entering the slotted plates of the deionizing chamber. These arcing horns are of spaced, laminated construction to provide venting as the arc moves along them.

The copper deionizing plates shown in figure 4 are designed from the point of view of continuing the work done as the arc passed upward along the horns. Extended to the full length of the chamber before entering the plates, the arc first encounters the long, gradually tapered entering slot. Passage along this slot tends to contract the core of the arc ("Extinction of an A-C Arc," J. Slepian, AIEE TRANSACTIONS, volume 48, April 1939), further increasing the arc voltage until at the end of the slot it is ready to transfer to the copper plate surfaces and change from a single long arc to a multiplicity of short arcs in series, each with its own cathode and anode drop. The steel plate surrounding each copper plate, also shown in figure 4, is more liberal in design than in previous structures to provide a stronger magnetic field for moving the arc core along this tapered slot onto the copper plates.

Figure 6. This film shows the performance of the breaker when closed against a peak current of 96,000 amperes and opened on 34,800 amperes

Thermal capacity of the deionizing chamber is a vital factor in interrupting heavy currents with the De-ion air circuit breaker since arc energy is transformed into heat in the process of interruption. Compared with earlier construction, two modifications appear in this design providing increased thermal capacity of the chamber: the thickness of the individual copper plate is increased, and the number of turns in the radial field coils is increased to give a higher speed of arc rotation around the annular path. That is, an increased cubic content of copper per gap provided while the time of exposure of the arc to any particular point on the plate is decreased. Improvement in electrostatic shielding produces more uniform distribution of voltage across the series of gaps to obtain a more efficient interrupter.

While these breakers are rated at 15 kv they are designed with 23-kv insulation in line with other powerhouse breaker practice. The 54-kv one-minute 60-cycle hold tests, and the 100-kv, $1^1/_2$x40 impulse wave tests were met without unduly increasing the size of the breaker as determined by its interrupting requirements. This applies to all the conventional tests, that is, across the open breaker as well as from the breaker terminals to ground, without the use of any supplementary disconnecting devices.

Three-pole breakers made up of single-pole units here described resemble, in general, earlier constructions of smaller breakers with the three units mounted on a single frame and operated by a conventional solenoid mechanism mounted at one side or underneath the breaker. Figure 7 shows a shaft-operated three-pole breaker with the closing solenoid at one end of a common frame and is representative of general construction in all except pole spacing which was here determined by special installation requirements. This breaker with its $52^1/_2$-inch pole spacing was a complete factory-assembled unit greatly simplifying installation. Figure 8 is a dimensioned drawing illustrating a more compact design with the same pole units spaced $27^1/_2$ inches apart and with the solenoid mechanism located underneath the breaker, operating through a common shaft. This arrangement is well adapted to metal-enclosed construction permitting the mechanism compartment to be completely isolated and accessible while high-voltage parts of the breaker are energized. Figures 9 and 10 illustrate respectively a steel cubicle lay and a horizontal drawout

Table II. Single-Pole Interrupting Tests, 15-Kv De-ion Air Circuit Breaker—"O" and "CO" Operations

Test Number	Peak Current Closed Against	Root-Mean-Square Current Interrupted	Breaker Time	Operation	Root-Mean-Square Voltage Restored
1		4,400	4.4	O	12,600
2	5,600	4,250	4.4	CO	12,600
3		30,300	4.0	O	11,800
4	92,500	31,800	3.8	CO	11,600
5		30,000	4.0	O	11,800
6	96,000	32,100	4.0	CO	11,600
7		35,000	3.9	O	11,800
8	56,500	28,200	4.0	CO	11,600
9		4,600	4.4	O	12,600
10	10,500	5,000	4.6	CO	12,600
11		4,800	4.4	O	12,600
12		34,500	4.0	O	11,800
13	95,000	35,250	4.0	CO	11,600

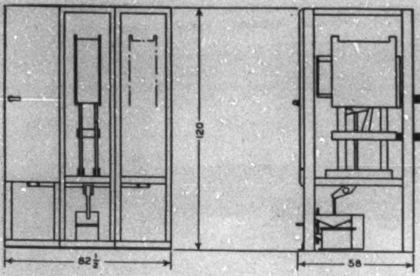


Figure 8. This breaker may be completely enclosed in steel if desired. The result is a comparatively compact assembly

metal-clad unit embodying modern features for this class of equipment.

Tests

In the development of this breaker, the usual insulation tests, operating tests, and current-carrying tests were made. Voltage distribution along the De-ion stack was studied to determine the best type of static shield. And finally, to prove the interrupting ability, several series of tests involving several hundred interruptions, were made, with full generator voltage across a single-pole unit, up to practically the full capacity of the high-power laboratory.

These tests were made on a single-pole unit operated by a conventional four-inch trip-free solenoid having a standard shunt trip magnet. For the sake of simplicity, the first interrupting tests were made without main contacts, the short circuit being initiated by a separate closing breaker. These tests were made with the test generator excited to 13.2 kv, 60 cycles. Figure 5 shows an oscillogram made while this breaker was interrupting 34,000 amperes. Table I shows data taken from typical tests made during this series. Outstanding facts presented by these figures are:

1. The time from energizing of the shunt trip coil to interruption of the circuit is from 3.3 cycles to 4.9 cycles, on a 60-cycle basis for currents from 4,000 amperes upward.

2. The interrupted currents range as high as 41,500 amperes.

3. The restored voltage averages more than 12,000 volts. This is significant in view of the fact that this is impressed on a single-pole single-break unit. Since almost all faults in this type of service involve ground, resulting in considerably less than line voltage being impressed on any one pole, this performance indicates a considerable factor of safety.

Table II shows data taken from a series of 13 single-phase tests made alternatively on the "O" and "CO" basis for customer representatives. These tests were made with the test generator excited to 13.2 kv. Of these tests, seven were at 30,000 amperes or more and the highest current interrupted was 35,250 amperes.

In these tests, the ability to close against high currents was demonstrated. The highest current closed against was 95,000 amperes crest. In other similar tests currents as high as 104,000 amperes crest were successfully closed.

Figure 6 shows a typical oscillogram made on a "CO" unit operation of the breakers.

Tests were then made on the breaker equipped with main contacts, rated at 2,000 amperes, 60 cycles. In these tests the operation was alternatively "O" to "CO." Table III shows results from these tests. The entire series of tests shown were made consecutively and without delay except for precautions to prevent overheating of the deionizing chamber due to the rapid operation at such high currents. By comparison with tables I and II it will be seen that the performance of the breaker under the three conditions is very uniform.

After these tests the breaker was in good operating condition and adequate for further service. The deionizing chamber, though dismantled for inspection, was reassembled and used for additional interrupting tests.

In making the interrupting tests on this breaker, its endurance under severe duty was forcibly demonstrated. In De-ion air-circuit-breaker testing it is quite common to make 20 or more tests without any maintenance on the breaker other than artificial cooling of the deionizing plates, which is required by the rapidity of the tests. For instance in referring to table III, the 21 tests were made in one continuous series. In eight of these tests the interrupted current ranged from 28,400 amperes to 34,800 amperes, six of them being above 30,000 amperes. Of the remaining 13 tests, 7 ranged from 11,800 amperes to 24,800 amperes. The remainder were at currents of from 1,300 amperes to 5,700 amperes. All tests were made with 13.2 kv on the generator. Nine tests were on the "CO" operating cycle and the highest current closed was 96,000 amperes crest. This series of tests was made without any maintenance or parts replacement, except the aforementioned occasional cooling of the deionizing plates. From the users' point of view this performance is impressive, as this one series

Figure 9. Cubicle layout for the new air circuit breaker

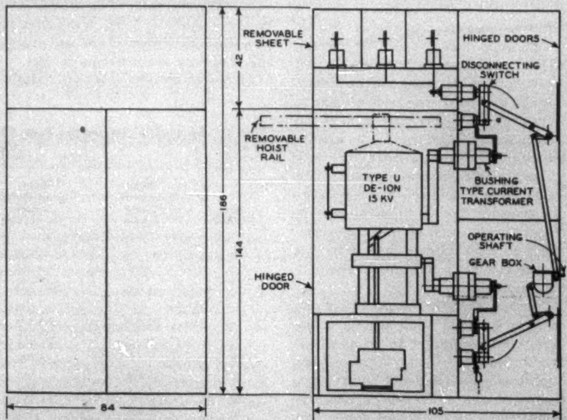


Table III. Single-Pole Interrupting Tests, 15-Kv De-ion Air Circuit Breaker—"O" and "CO" Operations

Test Number	Peak Current Closed Against	Root-Mean-Square Current Interrupted	Breaker Time	Operation	Root-Mean-Square Voltage Restored
1		1,300	5.8	O	12,700
2	2,500	1,400	4.3	CO	12,700
3		3,700	4.6	O	12,700
4	7,000	3,500	4.2	CO	12,700
5		5,700	4.5	O	12,500
6	8,200	4,600	3.6	CO	12,700
7		11,800	4.3	O	12,500
8	27,700	13,300	4.0	CO	12,500
9		13,400	4.3	O	12,700
10		10,200	4.0	O	12,700
11		17,500	4.6	O	11,900
12		24,800	4.0	O	11,900
13	42,500	22,000	4.5	CO	11,900
14		29,000	4.0	O	11,900
15	72,700	31,900	4.1	CO	12,400
16		32,000	4.3	O	11,500
17	49,700	28,400	5.1	CO	11,500
18		32,000	4.3	O	11,500
19	96,000	34,800	4.6	CO	11,600
20		31,300	4.4	O	11,500
21	74,000	31,200	4.3	CO	11,600

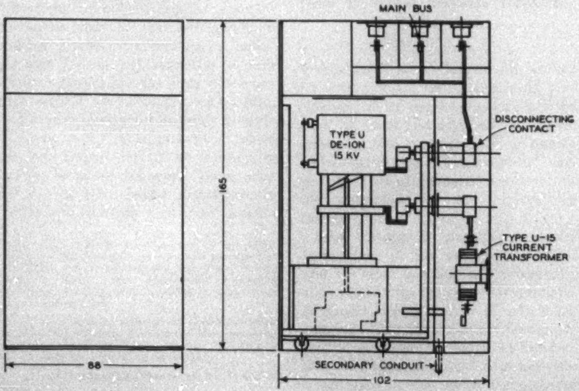
of tests is equivalent to many years of service.

Conclusions

Air circuit breakers incorporating fundamental De-ion principles of arc rupture have now been made available to a widening circle of application where, for individual reasons, the air breaker is preferred. Breakers of this type are available for general a-c switching applications at voltages from 2,500 to 15,000, the largest unit in the 15-kv class having been tested beyond 37,000 amperes, with some applications at 24 kv.

The basic De-ion characteristic of a dry breaker has been extended beyond the capacity of any other breaker except those operating in oil or other liquid dielectric. As with earlier forms of De-ion air circuit breakers, this larger breaker lends itself equally well to all forms of modern switchgear construction such as cell, metal enclosed, or truck. It also forms an important element in modernization programs.

Figure 10. Horizontal drawout metal-clad unit for the De-ion air circuit breaker including well-known features in use with conventional equipment



Discussion

D. C. Prince (General Electric Company, Philadelphia, Pa.): I am sure that the electrical-engineering profession has watched the development of the De-ion breaker with considerable interest since it was first announced at the winter convention of the AIEE in 1929. Since that time added importance has been attached to oilless-circuit-breaker development by a succession of station fires which have not however been attributed to faulty circuit-breaker operation.

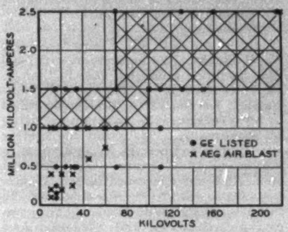


Figure 1. Circuit-breaker ratings

During the same period European manufacturers have been carrying along parallel development of oilless types. There is little doubt that the oil circuit breaker will be superceded by an oilless type if and when such a breaker is developed which offers the service, simplicity, performance, size, and cost of an oil breaker and has in addition no oil. Such a circuit breaker to have an important bearing on switchgear development should satisfy the foregoing criteria over as much of the field as possible.

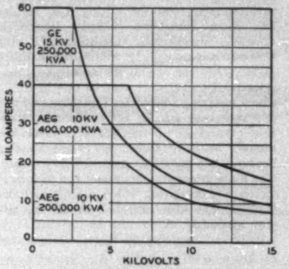


Figure 2. Comparison of interrupting rating in amperes

Figure 1 of this discussion shows the maximum listed rating of standard oil circuit breakers in the United States compared with corresponding ratings in Europe. One is at once struck by the fact that the maximum ratings of European breakers at all voltages are far below those listed and widely used in the United States.

Figure 2 shows the variation in interrupting current at reduced voltage as listed for a standard type of oil circuit breaker compared with corresponding data for European designs. It is again noted that the ability of the foreign designs to handle high currents is far below that of the standard domestic oil circuit breaker.

In the ten years since Mr. Dickinson first described this circuit breaker before this body, maximum interrupting capacity in current seems to have risen from 28,000 amperes to 41,500 amperes and in voltage from 15,000 to 24,000. The oil circuit breaker therefore still occupies the field as the best available means of interrupting all currents and all voltages.

M. H Hobbs (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It seems to me that one of the outstanding features of the breaker described by Mr. Dickinson is its uniform performance on all currents. This makes the breaker very flexible and permits its application to a wide variety of circuits. One additional application not mentioned in Mr. Dickinson's list is that to circuits supplying arc furnaces, where oil breakers had not proved satisfactory and the operations numbered 75 or more per day. As usual the De-ion breaker is giving a very good account of itself.

Briefly mentioned in the paper were metal-enclosed assemblies incorporating De-ion breakers. The unit is particularly well adapted to cubicles or metal-clad switchgear of the horizontal drawout type. As a matter of fact, if it were not, its value would be much reduced, for modern American practice almost universally requires metal enclosures.

For steel-mill service, requiring more or less frequent disconnecting for safety reasons, and to facilitate the relatively little maintenance required during the regular but short shutdown periods, horizontal drawout metal-clad mounting for the breaker is particularly well suited. Complete separation of the bus and connections from the breaker unit may be provided and phase isolation which this important service demands may also be accomplished. Routine inspection of the contacts after the breaker has been rolled out of the housing becomes a convenient and safe procedure. The same adequate clearances and dielectric strength of insulation which characterize the breaker, are also included in the metal-clad assembly.

Cubicle mounting is more common for powerhouse work involving the heavier breakers and with service which is not so severe as far as repetitive duty is concerned. Isolation of the individual parts of the circuit is no less important, however, and here again the breaker is well adapted to the requirements. To facilitate removal of the arcing chambers, a removable rail is provided, thus giving convenient access to the contacts.

It seems clear that the near future will see the application of this new development to a number of heavier industrial and power-station switchgear installations.

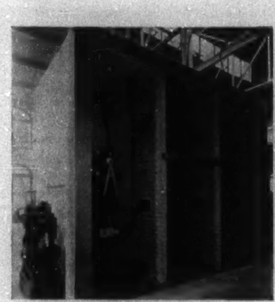


Figure 3

P. H. Adams (Public Service Electric and Gas Company, Newark, N. J.): Mr. Dickinson has presented in his paper a description of the latest development in the air De-ion circuit breaker designed for 15-kv service where interrupting duty does not exceed 800,000 kva. It is to be expected that with further experience and development, the range of this type of circuit breaker will be considerably extended.

In planning the rebuilding of the high-voltage bus at City Dock, one of our important substations, which supplies a low-voltage network, series street lighting, radially fed power, and rectifiers for street-transport service, a great deal of consideration was given to service protection and fire prevention.

Although the circuit breakers already in use in this substation for the high-voltage bus were of a type that contained only 12 or 13 gallons of oil each, they were obsolete and of insufficient rupturing capacity. The interrupting duty required of the circuit breakers in this substation had increased along with the increase in system generating capacity to a point beyond that for which the existing breakers could be rebuilt.

Since new breakers must be provided, it was decided to use oilless circuit breakers. Accordingly, in 1938, eight 1,200-ampere 15-kv De-ion air circuit breakers insulated for 23 kv and to have an interrupting capacity of not less than 750,000 kva, were ordered for use on the 13.2-kv bus.

A heavy steel base of reinforced channel iron construction was provided for each of these circuit breakers so that the operating mechanism and contact elements could be assembled on it and shipped from the factory ready to place on foundations and have the brick cell work built around it. Removable cover plates on the base give access to the operating shaft. A portable rail and trolley hoist can be attached to the top of any compartment to remove the grid element for inspection. The grid element weighs about 1,000 pounds.

Figure 3 of this discussion shows the circuit breaker in the cells with electrical connections ready for service. The cell and base are arranged for a change of the pole elements to 34.5-kv units in the future.

The series of tests given in table II in Mr. Dickinson's paper were witnessed by us in connection with the development of the pole units for the breakers on this order. From the showing made on these tests, it would appear that progress is being made in the development of the oilless type of circuit breaker. It is to be hoped that further developments will bring about the elimination of oil in circuit-opening devices for indoor use in the not-too-distant future.

There is also need for elimination of inflammable fluid in outdoor equipment. The air-blast circuit breaker gives promise in this field and its development for high-voltage service should be continued.

Multiple-Grid Breakers for High-Voltage Service

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Synopsis: There are occasional demands for high-voltage circuit breakers to interrupt in five cycles or less, and to meet this demand, a "De-ion grid" interrupter has been developed which may be used in multiple unit groups for apparatus rated 138 to 230 kv. Records of field tests up to 2,250,000 kva at 220,000 volts are given, showing interrupting times averaging 2.6 cycles (50-cycle frequency). Cathode-ray oscillograms indicate a comparatively small amount of transient overvoltage, less than would be expected from the usual magnetic oscillograph records. With the fast breaker operation, the apparent system voltage dip was small, and freedom from system disturbance and loss of synchronous equipment is noteworthy.

FOR some years the standard breaker interrupting time for high-voltage circuits has been 8 cycles on a 60-cycle basis or 0.133 second.[1] Individual cases have arisen where faster operation has been required, as for instance, the 3-cycle apparatus on the 287-kv Boulder Dam lines.[2] Three-cycle performance at very high voltage introduced special problems and increases the cost of the apparatus involved to a marked degree. On the other hand, it is now feasible through improved interruption methods, to considerably better the 8-cycle standard at a relatively small increase in cost. For the future, therefore, 5-cycle performance on a 60-cycle basis or 0.083 second breaker time from trip coil energization to arc interruption may be procured readily in voltage classes 138 to 230 kv. A multiple-unit De-ion grid assembly to meet these requirements has been developed and tested in the laboratory and the field.

The new interrupter assembly is made of units, each built from a design which has a previous record of satisfactory service at lower voltages. These units are arranged to operate simultaneously to give, at relatively high speed, a series of low-voltage arcs. This arrangement has several advantages: (*a*) each small unit can be tested to the limit of the laboratory power available, (*b*) damage to an individual unit does not impair the operation of the others, and (*c*) mechanical details are readily accessible for inspection and repair. The *CDH* grid after extensive high-power laboratory tests, both in this country and in Europe, was standardized upon two years ago for 46- and 69-kv service. At that time, investigation of voltage-interrupting ability for short grids of this type showed possibilities of its use in the high-voltage field. A small grid designed for 34.5-kv service proved actually able to handle 66-kv circuits and automatically suggested the use of this small unit in multiple assemblies for the high-voltage classes.[3]

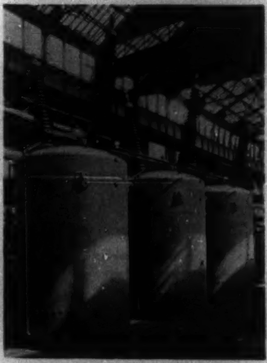

Figure 1. Typical three-pole 230-kv oil circuit breaker

Some changes in assembly naturally arose, since at low voltage, the *CDH* grid has been handling heavy currents, and at 220 kv the current-interrupting requirement is in the order of only 6,000 amperes. The finger contact previously used is no longer necessary for conductivity and a simple butt contact not only is adequate in that sense, but provides quicker parting time once the breaker is unlatched. The butt contact also is more economical in space which is particularly desirable when several units are built into a multiple assembly.

The venting of gases from the arc interruption also required consideration. The two vents near the top of the stack which had been found desirable with currents up to 40,000 amperes were retained. The lower part of the stack was entirely closed, except for a rectangular opening in the bottom plate into which the moving contact fits rather closely. This results in sufficient gas pressure within the stack at the lighter currents involved with 220-kv operation, without exceeding a pressure which can be handled safely by the enclosing structure. Each stack is built up of hard fiber plates, tied together with four wood-base Micarta studs. Iron plates are used for directing the arc as in the predecessors of this line of De-ion grid stacks. In general, the assembled unit has much of the appearance of a 34.5-kv stack except that metal top and bottom end plates are employed.

A group of three of these grid units per stationary contact (six per phase) comprises the interrupting element for 220-kv service as shown in figure 2. A similar group of two (four per phase) can be used for 138-kv apparatus, as test results discussed later will indicate. The mechanical linkage at the top of the tank is quite similar to the conventional breaker, except that special high-strength steel is used in places where a saving in mass makes its use justified. Light weight and speed of opening were prime considerations in the design. The usual form of lifting rod and cross bar (figure 3) is used, but the cross bar does not make and break the circuit as in the usual two-break switch. It is used to pick up (in closing) an insulating operating rod to which are attached, through a lever ratio, the three moving contacts of each assembly (figure 3). In opening, the three contacts are moved rapidly under the effect of the butt contact springs and also a substantial spring at the top of the operating rod, the lever ratio making it unnecessary for the lift rod and cross bar to be accelerated at such a high rate.

Considerable effort was put into the design of these fast-moving contact parts as the entire short-circuit interruption takes place within the travel of these small moving contacts and speed consequently is most important. Heat-treated aluminum contacts are used to provide the necessary conductivity and at the same time to secure the greatest speed

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Paper number 39-36, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 20, 1938.

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[1] For all numbered references, see list at end of paper.

(a)

1—Accelerating spring
2—Dash pot
3—Grid unit
4—Cover plate over stationary contact
5—Operating rod
6—Adjustment
7—Eccentric pin
8—Moving contact

(b)

1—Vents
2—"*CDH*" grid
3—Moving contact
4—Adjusting jacks
5—Stationary contact spring

Figure 2. Stationary contact assembly for six-grid 230-kv breaker

with least effort, through a reduction in mass. Long life of contacts is assured through the use of tungsten alloy tips to take the burning. The accelerating force of the butt contact springs acts through the first fraction of an inch of contact travel and serves to get the parts into motion as well as providing low-resistance point of contact. Another spring at the upper end of each operating rod acts through the entire stroke and insures that sufficient speed of motion will be maintained until the arc is extinguished. An oil-dashpot effect is incorporated into the upper part of the assembly, bringing the contacts to rest without excessive slam and without rebound. After leaving the interrupting elements of the stationary contact, the cross bar continues to move through a considerable body of clear oil, introducing an oil disconnect into the circuit in addition to the active break of the interrupters.

Alignment of the three grid units of each assembly is secured by adjustable tie rod connectors, providing for correct spacing of the units as well. Fine adjustment of each contact position is easily obtained by turning the eccentric hinge pin (figure 2).

A stationary contact is enclosed within the upper cast end plate associated with each stack. It is hinged at one end, backed up by a substantial contact spring, and protected against excessive burning by means of a face of tungsten alloy, silver soldered in place. For convenience in inspection and maintenance, this contact can easily be removed and replaced together with its spring and flexible shunt, without removing other material, or disturbing adjustments.

Preliminary testing in the high-power laboratory covered a wide range of short-circuit currents and voltages. One series of tests, closing and opening, made with 132 kv impressed across a single pole of a 220-kv breaker, showed uniform duration of arcing from 500 amperes up to 3,000 amperes. Other runs at 88, 66, and 44 kv up to 8,000 amperes gave similar performance, that is, uniform in time up to the highest current interrupted. These results are shown in curves 1–2 of figure 7.

The effect of high kilovolt-amperes per unit was also demonstrated by a series of tests in the laboratory using only two of the six grids. Here again, a very uniform curve of interrupting time against current was obtained up to currents well over 7,000 amperes at 66 kv and 44 kv (see curve 3 of figure 7).

In all these tests, the arc was extinguished within half the stroke of the moving contacts. The oil depreciation was so slight as to be not measurable.

Another series of tests shown in figure 8 was made at approximately 1,000 amperes, at voltages varying from 110,000 to 230,000 volts. All results, when plotted between applied voltage and interrupting time, fell within an envelope not more than 0.02 second wide. This shows the uniformity of operation over a wide voltage range. A similar series also shown in figure 8 with only four of the six grids operating was carried up to 176 kv before

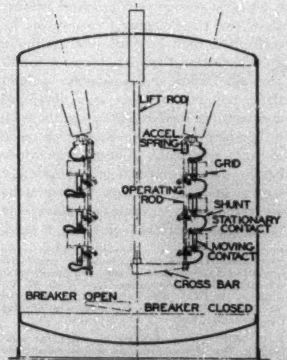


Figure 3. Schematic arrangement of contacts—220-kv, six-grid, in open and closed positions

the breaker failed to interrupt within the grids. The very satisfactory performance on these tests up to well over 132 kv, together with the testing of individual grids to 40,000 amperes at lower voltage, justifies the application of a four-grid assembly to 132-kv high-speed work.

As a result of this laboratory demonstration, arrangements were made for further tests on a three-pole breaker at the Laguna Bell substation of the Southern California Edison Company. This station is centrally located in the system and can produce short circuits of approximately 2,250,000 kva, at an operating voltage of 220,000 volts. A set of contacts, together with the necessary mechanical parts, was sent out from the factory and installed in a type *G-22-A* 187-kv three-pole breaker, which had been in service approximately ten years. Single-phase-to-ground faults were applied, sometimes by the test breaker, and sometimes by a separate closing breaker, to the third pole of the breaker further away from the mechanism. A protective relay operating from overcurrent in the ground lead, tripped the breaker in 0.005 to 0.010 second. A six-element magnetic oscillograph and a cathode-ray oscillograph were used simultaneously to record fault current, voltage across the breaker, relay time, trip coil current, and position of lift rod in one pole of the breaker. Changes in magnitude of fault current were secured by switching lines and transformers at various parts of the system.

Table I. Southern California Edison Company 220-Kv 50-Cycle Tests

Westinghouse Type G-22A 187-Kv Oil Circuit Breaker, October 16, 1938

Test	Shot*	Current Interrupted (Amperes)	Equivalent Three-Phase Kilovolt-Amperes	Duration of Short Circuit (Cycles)	Breaker Interrupting Time (Cycles)
1 *W*	O	620	225,000	3.7	3.4
1 *AW*	CO	565	205,000	3.9	3.6
2 *W*	O	1,600	590,000	3.1	2.7
2 *AW*	CO	1,520	553,000	3.0	2.6
3 *W*	O	2,780	1,011,000	3.4	3.0
3 *AW*	CO	2,960	1,135,000	1.5	1.1
4 *W*	O	5,220	1,900,000	3.1	2.6
4 *AW*	CO	5,330	1,938,000	2.5	2.2
5 *W*	O	6,000	2,250,000	3.1	2.6
5 *AW*	CO	6,000	2,250,000	2.6	2.1
GW	Opening	22.9 miles of line— 15 amperes			3.1
MW	Opening	118.1 miles of line— 77 amperes			4.8
BCW	Opening	246.4 miles of line—150 amperes			5.6

* "O" indicates "opening"; "CO" indicates "closing-opening."

Cathode-ray oscillograms recorded bus voltage to ground on the faulted phase, during the period of arcing, and for a few cycles thereafter. Each record shows a central zero line indicating a closed contact; another trace a small distance away indicating arc voltage, so small as to be scarcely noticeable; and the open-circuit recovery voltage which is sine wave after a short transient generally not exceeding one-half cycle. The maximum peak of recovery voltage obtained on the whole series of tests occurred on the lightest short-circuit values of approximately 200,000 to 225,000 kva. On one of these light short circuits an overvoltage of 170 per cent above normal crest voltage was obtained. For all heavier short circuits, however, the recovery voltage did not exceed 10 or 15 per cent above normal crest. On the lightest short circuits also the rate of rise of recovery voltage was greatest, being approximately 2,300 volts per microsecond. On the heavier short circuits this rate was reduced being approximately 500 volts per microsecond. It is interesting to note that these actual field-test cathode-ray data substantiate calculations made for similar conditions and

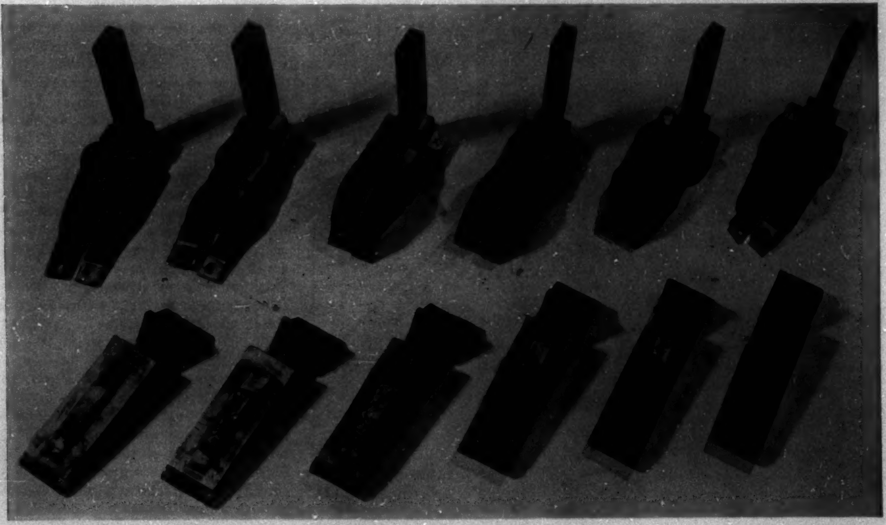
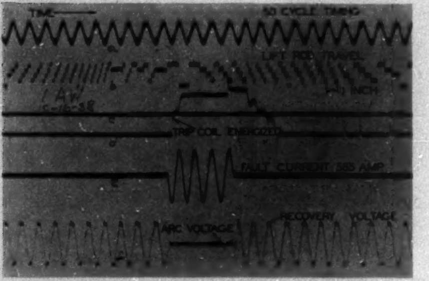


Figure 4. Contacts after field test—ten shots 565 to 6,000 amperes

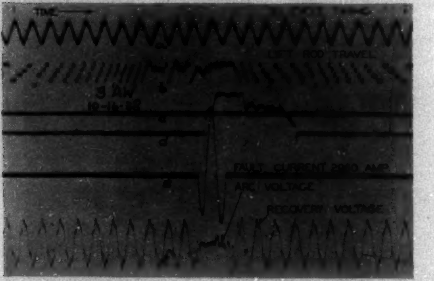
already presented before the AIEE.[4] Cathode-ray oscillograms of field conditions should be particularly useful in interpreting magnetic oscillograms and Hall recorder films—a more general use of cathode-ray records by operating companies is recommended.

Figure 5. Representative oscillograms (220 kv, 50 cycles)

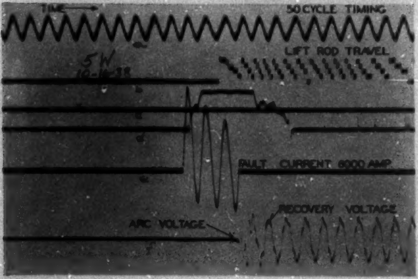
a—Timing wave
b—Lift rod travel
c—Not used
d—Relay and trip coil current
e—Fault current
f—Line-to-ground voltage



1 *AW*—A CO shot interrupting 565 amperes



3 *AW*—A CO shot interrupting 2,960 amperes



5 *W*—An O shot interrupting 6,000 amperes

Figure 6. Cathode-ray oscillograms—for tests illustrated in figure 5

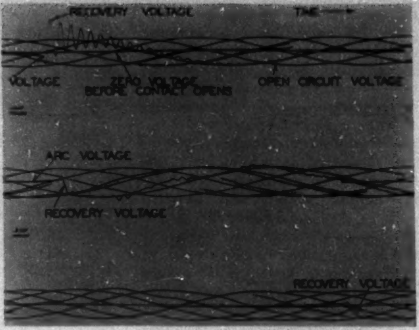


Figure 6 shows three typical cathode-ray oscillograms, with the several parts of each record identified so as to point out the transition from zero voltage before the contacts open, to arc voltage, to the transient at the instant recovery voltage appears and finally, the sine-wave open-circuit voltage. Since the film was rotated on a drum, each trace as it leaves the right-hand end of the oscillogram can be picked up again at the opposite end to form a continuous record.

The results of this series of field tests are shown in the tabulations (table I) and typical magnetic oscillograms are also shown (figure 5). Each oscillogram gives a time record of the position of the breaker contacts (trace *b*) before and after the fault current (trace *e*). The rise of current in the trip coil circuit is shown (trace *d*), lagging the start of fault current by approximately one-third cycle. Movement of the breaker mechanism follows directly thereafter, and arc voltage and recovery voltage (trace *f*) appear as the fault current is extinguished. Voltage records on these magnetic-oscillograph records are somewhat distorted, due to inaccuracies in the measuring circuits, and a more accurate record is obtained from the cathode-ray oscillograph. Values of current and equivalent kilovolt-amperes are given in table I, showing interrupting time averaging 2.6 cycles (50-cycle frequency, or 3.1 on a 60-cycle basis) and ranging from 1.1 cycles at 1,000,000 kva to 3.6 cycles at 200,000 kva.

Importance has been attached recently to the more prompt interruption of charging currents on high-voltage lines than has been practical with old forms of interrupter. As part of the field tests described hereinbefore, a group of charging-current interruptions were made, the results of which are shown in table I, indicating breaker time from energization of trip coil to arc extinction ranging from 3.3 to 5.6 cycles on a 50-cycle basis. The 246-mile stretch of 220-kv line used in these tests is, we understand, the longest 220-kv line on which charging-current interruption has been made under test conditions, the charging current being 150 amperes. Cathode-ray oscillograms of these charging tests show transient voltage phenomena during interruption up to 100 per cent above normal line-to-ground values.

Inspection of contacts showed no excessive burning of metallic parts or fiber plates, nor was there any noticeable oil depreciation. The dip in voltage on the system was so slight as to cause no trouble to connected synchronous apparatus. It is evident that the requirements of five-cycle operation for high-voltage equipment have been met with considerable margin.

Short-circuiting the heart of a 220-kv operating system under load conditions up to three-phase equivalents of 2,250,000 kva involves careful planning and some risk-taking even on a system as robust and well operated as that of the Southern California Edison Company. The difficulties are probably better appreciated by those skilled in system operation under fault conditions than they are by the apparatus designers. However, both have a keen interest in such field tests as they afford the only way of verifying the results obtained in high-power laboratories. We express only the general sentiment of the industry in acknowledging the contribution made to the switchgear art by the Southern California Edison Company in subjecting their system to the tests referred to in this paper.

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4. Power System Transients Caused by Switching and Faults, R. D. Evans, A. C. Monteith, and R. L. Witzke. AIEE Transactions, volume 58, 1939, pages 386–97 (August section).

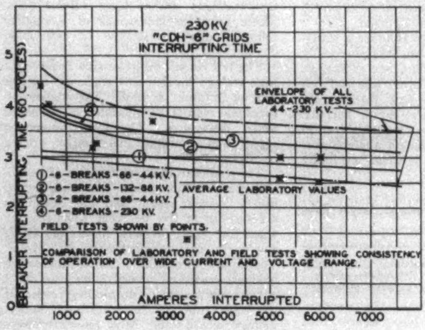

Figure 7. Interrupting performance with varying current

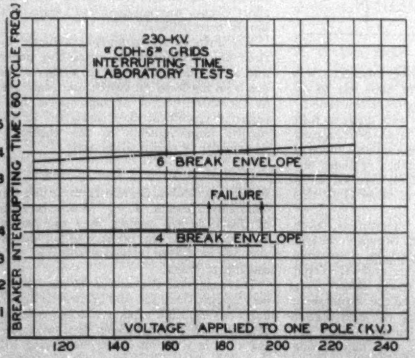

Figure 8. Interrupting performance with varying voltage

Discussion

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): Messrs. MacNeill and Hill have presented a very ingenious arrangement of De-ion grid contacts which will speed up and improve the performance of high-voltage breakers. I should like to present some remarks from the point of view of system stability, relay settings, line damage, and breaker rupturing ability using such an arrangement. The arrangement shown decreases the duration of short circuit from around six to eight cycles for the present standard breaker to something less than three cycles. All of these speeds are a great improvement over older types of breakers which have interrupting speeds around 20 to 30 cycles. Naturally there is a great reduction in damage to line conductors and insulators by decreasing the time of interruption to three or eight cycles. However, to obtain fully these benefits with high-speed breakers, it is desirable to use impedance step-type, carrier-current, or pilot-wire high-speed relays to keep the total interruption time as low as possible.

From a system stability standpoint when a system is short-circuited the reactive kilovolt-ampere demand on the system increases enormously and the kilowatt demand on the system may increase slightly or decrease. In any event the distribution of these loads among the generators on the system differs considerably from the load conditions which existed prior to the application of the short circuit to the system. As a consequence certain machines become unloaded and speed up while others tend to slow down. If a short circuit is not removed rapidly enough from the system the phase angles between the advancing and retarded machines will become so great that they will pull out of step. Studies show that great improvement is obtained in removing faults within one-fourth to one-half second but improvement below this point is questionable economically. Unquestionably if we could remove a fault from the system instantaneously we would not have a stability problem but it is doubtful if any such ideal could be approached from an economic standpoint. As a matter of fact we doubt whether there is any advantage of improving speeds below eight cycles.

The rapid removal of a fault from the system and reclosure of the line has been proposed as a means of improving service on our transmission systems. Sufficient experience has not been obtained with this arrangement to warrant such a policy. At this time I doubt very much whether it can be proved economical. In general it seems to me that this field is the only application open to a three-cycle breaker. There are so many improvements available which can be applied to transmission lines generally, such as ground wires, lowering tower-footing resistances, arcing devices, and high-speed relay systems, decreasing the interrupting speed to three cycles is more or less of a refinement for limited application.

Another factor to be considered in extremely high-speed fault interruption is the fact that as soon as you interrupt a fault at less than approximately six cycles the fault current which must be interrupted is asymmetrical and may be considerably above the symmetrical value. This is illustrated in the test oscillograms, figures 5*b* and 5*c*. In other words, within certain limits the faster the fault is removed the greater rupturing capacity it is necessary to provide in the breakers so that while it is advantageous to decrease the time of fault interruption in one respect it is disadvantageous in another. I do not wish to discourage the development of high-speed arc-interrupting devices but do wish to point out those factors which must be considered in their development. The opinions which I have expressed here apply not only to the developments which Messrs. MacNeill and Hill have described but to developments of other designers as well. I believe that if these developments give us a breaker of better interrupting ability it is of a great deal more advantage than the decreased time of arc interruptions obtained. Our sad experiences in the past of the manufacturers derating of breakers, many of which are too recent, emphasize the necessity of improvement along these lines rather than the necessity of further improvement in speed of fault interruption. Of course, if the improved speed in the breaker is inherent with its interrupting ability, that is another story and a welcome one because then we can control the time of arc interruption in relay settings.

D. C. Prince (General Electric Company, Philadelphia, Pa.): The authors are to be congratulated on the high degree of perfection achieved in the development of the De-ion grid. Examination of the successive papers describing the De-ion grid, which have appeared since this device first made its appearance in 1930, shows a steady evolution from the open slots of the earliest designs toward more and more complete enclosure of the arc in an explosion chamber. In the present paper this enclosure seems to be complete and one wonders whether the iron plates in the assembly need be longer retained. If it could be shown that operation is unimpaired by omitting the iron, which is very probable at these low currents, the question might be asked whether there is any distinction between the device of the paper and an explosion chamber.

H. A. Lott (Southern California Edison Company Ltd., Los Angeles): In October 1936, the first of a series of field tests at 220 kv were made on an oil circuit breaker at the Saugus substation of the Southern California Edison Company Ltd. During the ensuing year, 39 single-conductor-to-ground short circuits, varying in value between 0.3 and 1.7 million equivalent three-phase kilovolt-amperes were imposed on the 220-kv system at Saugus and cleared without inconvenience to consumers' service. The number of short circuits at each of the four duties which were used to test three different types of 220-kv breakers are shown in table I of this discussion.

An analysis of the Saugus tests led to the following conclusions:

1. If the over-all time of clearance is in the order of five to eight cycles, similar tests can be carried to the maximum values of 2,250,000 kva without inconvenience to consumers' service.

2. The final proof of a circuit breaker's performance occurs when the breaker is required to interrupt a short circuit at or near its rated capacity. The most practical way to apply such proof is through the medium of staged field tests on a high-capacity transmission system. While both the design and laboratory tests may be carried out with painstaking attention to every detail, such tests, because of limitations in the capacity of the laboratory equipment, fail to disclose occasional weaknesses which are only disclosed when a breaker is tested at or near its rated capacity. Usually such weaknesses are corrected by relatively minor changes in design.

In our discussion on "A New Multibreak Interrupter for Fast-Clearing Oil Circuit Breakers," AIEE TRANSACTIONS, December 1938, the procedure for conducting a short-circuit test at Saugus was outlined, together with some comments on the need for high-speed switching and the experience with high-speed switching on the Southern California Edison transmission system.

The operating results of the Saugus tests were so satisfactory and the interest was so keen that there was no hesitation in staging the next series of tests at Laguna Bell substation. Laguna Bell is at the load center of the 220-kv system, and the 2,250,000-kva duty is the greatest concentration of power at 220-kv available at any location.

The procedure used to conduct a short-circuit test at Laguna Bell was practically identical with the procedure used at Saugus. At Laguna Bell five different line and bus switching combinations were used to obtain five values of short-circuit current. However, since each test presents certain minor variations, a complete analysis was made, and a printed program outlining each step in the operating procedure was distributed in advance of each test to all station operators and engineers concerned.

Table I. Short-Circuit Tests at Saugus Substation

Number of Short Circuits	Equivalent Three-Phase Kilovolt-Amperes Interrupted (Millions)
8	0.3
6	0.5
6	1.0
19	1.7
—	
39	

During 1938 field tests were made on four different types of 220-kv breakers at Laguna Bell up to the maximum available duty of 2,250,000 kva. The number of short circuits cleared for each of the five values of fault current are given in table II of this discussion.

In one test, three interrupters, each slightly different from the others in design, were installed, one design in each of the three separate tanks of the breaker, and were tested the same day. All three tanks were tested at the same duty and the results were compared before imposing the next higher value of short-circuit current. One design proved superior, and was the only one tested at the maximum value of 2,500,000 kva.

The system performance while the maximum duties were being cleared at Laguna Bell confirmed in every detail the conclusions derived from the Saugus tests. There were no dips in the system speed and no consumer complaints when the short circuits were cleared in eight cycles or less. In a few cases the over-all clearance time exceeded eight cycles. When the clearance time was more than eight cycles, the short circuits of both the 1,750,000- and 2,250,000-kva values caused sufficient voltage dip to drop some motors which were equipped with instantaneous undervoltage release coils. There were no indications of instability following any one of the 77 test short circuits.

Table II. Short Circuit Tests at Laguna Bell Substation

Number of Short Circuits	Equivalent Three-Phase Kilovolt-Amperes Interrupted (Millions)
22	$^1/_4$
16	$^1/_2$
18	1
13	$1^3/_4$
8	$2^1/_4$
—	
77	

The test on the multiple-grid breaker for high-voltage service was made at Laguna Bell on October 16, 1938. The design features and results are presented in the paper under discussion. The tests progressed rapidly and as outlined in a program written in advance.

The results from the standpoint of system operation were excellent. There was no dip in the system speed, and the maximum voltage dip of five per cent was recorded when the 2,250,000-kva fault was being cleared in an over-all time of 3.1 cycles. There were no consumer complaints and no inconveniences to service since the short circuits were cleared too rapidly to permit the operation of instantaneous undervoltage release coils. Most consumers were not aware that tests were in progress as the flicker of lights which indicated when the short circuit was imposed and cleared was barely perceptible.

The authors are to be congratulated for their presentation of an interesting paper, and we are pleased to note the progress that has been made by all switch manufacturers in the development of high-speed, high-capacity circuit breakers designed to add further improvements to the continuity and quality of electrical service.

F. W. Gay (Public Service Electric and Gas Company, Newark, N. J.): Where breakers of this type are needed, they are needed very badly and the breaker under discussion seems to be a happy solution to a difficult problem.

It is suggested that one of the most necessary applications for this breaker is on circuits having high capacity, as for instance 132-kv cables. If a breaker of slow rupturing time is used on cables, restriking will occur. It is believed that multiple breaks, combined with high speed, will greatly reduce restriking.

The advantages of a super-high-speed breaker for ordinary duty have been greatly exaggerated. It is believed that there are a comparatively small number of applications where there is any great advantage from a stability standpoint in clearing a faulted circuit in much less than 20 cycles. If the total clearing time is much under 15 cycles, the rupturing occurs on the subtransient portion of the decrement curve and the current that must be ruptured increases rapidly when times of clearing are much less than ten cycles. If the current to be ruptured is increased, say ten per cent, by shortening the time of clearing the circuit, then the rupturing capacity of the breaker must be increased approximately ten per cent. If no credit is to be given the breaker because of less destruction at the point of fault, due to this shorter time of clearing, and if the high speed is not required for stability, then a fast-clearing breaker of given rupturing capacity must sell for less money than the standard eight-cycle breaker.

Every company should have swing curves for faults on various parts of the system. It is believed that when these swing curves are examined it will be found that there are very few locations, barring generating stations far from their load, where loss of stability or scrambling will occur in much less than three-quarters of a second for a double-line-to-ground fault. Certainly in this case a 20-cycle clearing time is short enough from a stability standpoint.

Better and faster relaying offers a more promising field for development.

W. A. Lewis (Cornell University, Ithaca, N. Y.): The discussion this afternoon has been primarily a description of the mechanical details of the new breaker design, and the performance of these circuit breakers under test conditions. It is interesting to consider the benefits offered to utility system operating engineers by the improvements in the circuit breakers. A large number of tests have been made by the Southern California Edison Company on circuit breakers of various designs. In some of the previous tests, existing breakers having a considerably longer operating time were used in the test. It was found that when the total time of fault clearing exceeded 12 or 14 cycles, a short circuit could be felt by customers on the system, and some motors having instantaneous undervoltage release devices would be disconnected. On the other hand, in tests involving faster operation no trouble at all was experienced by any of the customers on the system. In considering these tests, it should be remembered that all of the faults were single line to ground, and it can readily be seen that for the more severe faults, such as two line to ground, the high speed of operation is essential to avoid customer disturbance when the fault is at a location producing the maximum reduction in voltage. Computations on this and similar systems have, of course, shown that such speeds of fault clearing are necessary in order to maintain system stability. On systems having a large percentage of hydroelectric generation and long transmission distances, rapid fault isolation is much more essential than it is for those systems having all generation by steam, and with shorter transmission distances. However, as system interconnections are increased, rapid fault isolation will become more important even on those systems generating entirely by steam.

Another feature of the paper which is of considerable interest is the fact that the recovery voltage rates indicated by the cathode-ray oscillograms agree fairly well with the calculated recovery rates. A great deal has been done on developing methods of calculation, particularly those using the a-c network calculator, but there are relatively few instances where a direct comparison between calculation and test have been made. It is gratifying to note the satisfactory agreement contained in this case.

The third feature of interest to utility engineers is the fact that these new interrupting devices can be satisfactorily applied to many existing circuit-breaker designs, so that when the improvement in speed of fault clearing becomes necessary, it can be obtained without complete replacement of existing circuit breakers.

W. F. Skeats (General Electric Company, Philadelphia, Pa.): The reference to agreement of the test cathode-ray data with calculations presented in the paper by Messrs. Evans, Monteith, and Witzke is somewhat puzzling in view of the fact that the cathode-ray oscillograms presented in this paper are for short-circuit interruptions, whereas the calculations of Messrs. Evans, Monteith, and Witzke apply to the disconnection of an open line.

It is true in general, as has been pointed out many times in the literature, that an increase in the number of transmission lines connected to a bus tends both to increase the current available and to decrease the recovery voltage rate, so that there does tend to be in many cases a reduction of recovery rate associated with an increase in short-circuit current. Nevertheless, the tests made at the Philo plant of the American Gas and Electric Company as early as 1930 indicate that it is still possible to get high recovery rates with high values of short-circuit current. It would be interesting to know how the device described by Messrs. MacNeill and Hill would perform under such circumstances.

R. C. Van Sickle (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As an associate of Messrs. MacNeill and Hill, I have had the opportunity of studying in detail the cathode-ray oscillograms of the charging-current interruptions referred to in their paper. Such oscillograms are of considerable interest in showing the voltage existing during switching transients and I wish to call them to your attention.

Figure 1 of this discussion is one of the oscillograms taken during the disconnection of 246 miles of 220-kv line. This is the highest-capacity test of this type ever made. The original oscillogram is about 18 inches long and was made on a rotating drum by a single element recording the voltage to ground of the line being disconnected. The oscillograph was connected to the line close to the circuit breaker.

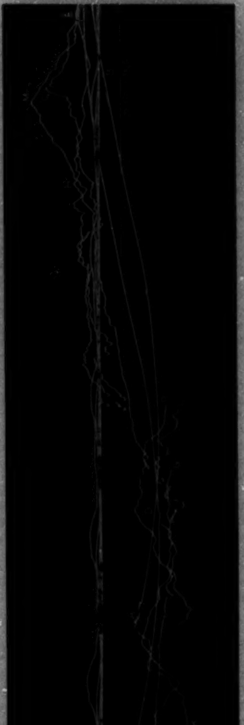

Figure 1

The sine waves show the normal system line-to-ground voltage and the zero line is located midway between crests.

The trace starts at *A* and moves to the right edge *B*, reappearing at the corresponding point *C* at the left edge. The first clearing occurs at *D*, probably with a very small contact separation since the arc restrikes after a very short interval. The trace goes off the film at *E* and reappears at the left. After a brief interruption, the arc restrikes at *F*. Both of these early interruptions are of such short duration that they do not appear as current interruptions on the magnetic oscillograph film and cause no appreciable voltage transient.

At the next crest of voltage the circuit is interrupted for about a tenth of a cycle. The transient following the restrike at *G* reaches at *H* a voltage which equals 130 per cent of the normal crest of line-to-ground voltage. The trace reappears at *I* and a restrike occurs at *J*. The following crest *K* reaches a value of 180 per cent. After the restrike *L* the maximum voltage obtained on the test, 250 per cent, is reached at *M*. The arc restrikes at *N* and the voltage reaches 215 per cent at *O*. The last restrike *P* produces a voltage of 210 per cent at *Q* and the transient dies out in an oscillation *R* leaving the line charged to about 60 per cent of the normal crest value.

The interruption was accomplished with two interruptions of negligible duration and five which were followed by restrikes which caused overvoltages. The overvoltages reached 120, 180, 250, 215, and 210 per cent of the normal crest of line-to-ground value on successive restrikes and caused no trouble on the system.

H. P. St. Clair (American Gas and Electric Service Corporation, New York, N. Y.): I should like to bring out briefly three or four points in connection with the paper by Messrs. MacNeill and Hill describing a new multiple-grid oil circuit breaker for high-voltage service.

While you are all aware that there has been an increasing interest displayed in this country in the development of oil-blast circuit breakers along lines which are a modification of European practice, at the same time the paper you have heard today, coupled with papers describing somewhat parallel developments presented in the last summer convention of the AIEE, gives evidence of the ingenuity of our American engineers in making notable advances in oil circuit breakers along conventional lines. Fortunately, in this country we are free from most of the restrictions—economic, military, or otherwise—which have forced European practice to a large extent away from oil, so that we are able to go ahead and develop the maximum inherent possibilities of oil circuit breakers.

As a result of this, I believe that the situation in this country is a particularly healthy one in that both of these developments, the air-blast and improvements in conventional oil circuit breakers, can go on in parallel without undue haste or pressure on either one. In this way, we are more likely to realize the maximum possibilities of each of these lines of developments.

When the Boulder Dam breakers were designed and built, a drastic step was taken in going from the standard eight-cycle breaker to the three-cycle breaker which that job required. This step was also a very costly one. I believe it is therefore a very sound and desirable procedure to develop a breaker which falls in between these two categories, namely, the five-cycle breaker, which apparently can be done with very little increase in cost over the present eight-cycle breaker. At the same time the gain in speed is a very substantial one and while its importance may not be fully apparent to all users at the present time, nevertheless we believe that it is certain to become more and more important as time goes on. On our own system, this increase in speed is already of considerable importance in connection with the application of ultrarapid reclosing of circuit breakers which has now passed beyond the pioneering stage and is taking its place as an important part of our system planning.

J. B. MacNeill: The field of switchgear may be divided into three principal parts as follows:

Low-voltage industrial

Medium-voltage powerhouse and substation

High-voltage transmission

The three papers presented as a group respectively by Ludwig and Grissinger (pages 414–20), Dickinson (pages 421–6), and MacNeill and Hill outline progress in each of these divisions.

The last-mentioned paper is timely in view of the discussion in engineering circles of the speeds which are necessary in high-power circuit breakers for modern system operation. The diversity of opinion on this subject seems remarkable until the variety of system operating conditions is fully considered. On some systems the present eight-cycle standard is more than adequate because of the solidity with which the system is tied together. There will be an increasing number of cases, however, where severe short circuits involving more than one phase wire will demand faster breaker operation than eight cycles. For such service five cycles seems a reasonable standard, as it can be obtained without marked increase in cost and with reliable equipment permitting numerous operations and providing high-speed reclosing.

A feature of this paper is the data from cathode-ray oscillograph records taken simultaneously with the magnetic oscillograph records under various conditions of circuit opening from charging currents up to a dead short circuit of 2,250,000 kva direct on Laguna Bell bus of the Southern California Edison Company. These records are of particular interest in view of the discussions now before the Institute on possible rates and magnitudes of transient voltages during switching operations. The values of switching transient voltages given in this paper are reassuring to American operating people using grounded neutrals. We are assured that these values are consistent with the results given by Evans, Monteith, and Witzke and Van Sickle. An increased use of the cathode-ray oscillograph under fault conditions on operating systems is desirable as it is apparent that some data taken by magnetic oscillographs is not accurate for high-frequency transient phenomena.

Mr. Skeats has asked if there is a co-relation between the recovery voltage data in the MacNeill-Hill paper and the results in the Evans, Monteith, and Witzke paper. We are assured that the two sets of data are in harmony for similar system conditions, and from this we can conclude that the average American grounded-neutral high-voltage system is not subjected ordinarily to extremely high switching voltages and that the more severe voltage transients referred to in the Evans, Monteith, and Witzke paper occur only infrequently and under unusual system conditions. Mr. Prince infers that the De-ion grid shown in this paper is approaching an oil-blast design. Fundamentally the De-ion grid theory is observed in this design; that is, the arrangement of oil pools and splitter plates is essentially the same as on all De-ion grids and the iron is still present to accelerate the arc, particularly on low currents, and to drive it into the restricted slots where deionization is carried out. Grids of this type have been made with and without iron and we feel that the presence of iron improves the grid operation.

Mr. Gay and Mr. Sels of Public Service Electric and Gas Company state that five cycles is not usually necessary for high-voltage circuit breakers, and to this we agree. Public Service of New Jersey, however, is fortunate in having a compact high-power system without marked stability problems. High speed of opening operation, however, particularly when coupled with high speed of reclosure is definitely showing great improvement in stable operation in several outstanding cases. In one such case the stable limit of a 138-kv line was multiplied by three by the installation of circuit breakers which opened and reclosed in 24 cycles. The alternative in that case to such high-speed operation was a parallel line.

Mr. Lott has presented an interesting discussion of Southern California Edison and has pointed out the large number of heavy short circuits which have been handled during their test program without any considerable system difficulty. This in itself is a tribute to the higher speed of breaker operation generally found during the tests, as well as to the ruggedness of the Southern California system and the care with which the system was arranged for the tests.

Mr. St. Clair of the American Gas and Electric Service Corporation has commented on the improvement in modern circuit breakers, both in interrupting ability and in reclosure speeds without departing from the underlying features of standard designs. There are considerable advantages to incorporating these modern features of operation in structures which are known to be adequate for American operating conditions. The operators are thus able to secure equipment familiar to their people and also in many cases to incorporate the high-speed features in old equipment with consequent saving in station construction costs. The general use of grounded neutral systems in America demands more frequent circuit-breaker operation than is the case with ungrounded neutrals, and our first requirement must be the adequacy of the circuit breaker for high-power duty, possibly repeated several times without maintenance, and with a growing demand for instantaneous reclosure.

Temperature Aging Characteristics of Class A Insulation

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Synopsis: This paper reviews the literature relating to the effect of temperature on the life of class *A* insulation. It presents test data on aging of built-up samples 100 mils thick, of varnished cloth, both black and yellow, over the temperature range 105 to 200 degrees centigrade.

The effect of temperature on the insulation was measured by three different methods—physical condition by visual observation, bending of the insulation to produce cracking, and the lowering of the breakdown voltage of samples which had been immersed in water. The final results are given in the form of temperature-time curves for several degrees of deterioration as measured by each of the tests.

THE materials used for insulation purposes in electric machines are subject to many factors which determine their useful life. They are subject to the effects of temperature, of mechanical stresses, vibration, electrical stresses, to the effect of oil in oil-filled apparatus, to moisture, dirt, and in some cases corrosive gases. Past experience shows that the life of a machine may be limited by any of these causes, and engineers must make proper allowance for each of them in design.

The importance of the effects of temperature was recognized early and definite limits were agreed upon for the maximum permissible operating temperatures which would constitute good practice for the various types of insulation used. These limiting values were based upon the knowledge of materials available at that time, and the performance of electric apparatus built to conform with these values has shown excellent results.

Since that time the electrical industry has made considerable progress. In addition, the chemical industry has been producing at a very rapid rate new materials which electrical engineers have been carefully studying as promising candidates to supplement or supplant the older forms of insulation.

It therefore is desirable to review briefly the previous work and compare the results in service with expectations on the basis of operating temperatures and with the results of later laboratory tests. Also, from these comparisons it is desirable to examine the latter and determine their significance.

Previous Work

In 1905 a paper by E. H. Rayner[1] in the *Journal* of the Institution of Electrical Engineers discussed the effect of temperature on insulation, pointing out the rapid effects of high temperatures on life. However, no very definite conclusions as to permissible temperatures were drawn.

In 1913 the Institute held a symposium[2] on the subject at which a number of papers were presented and much discussion ensued. The assumption was made that class *A* insulation had a ten-year useful service life at 100 degrees centigrade and one indefinitely long at 90 degrees centigrade, but one of only a few weeks at 125 degrees centigrade. Two ideas will be found in this discussion—one that as high a temperature as possible should be permitted, consistent with reasonable life of the machine, the other the importance of reliability in the operation of the machines and, therefore, the desirability of not departing greatly from the operating temperatures which were actually in use at that time. Following this discussion AIEE Standard No. 1 was evolved which recommended for purposes of standardization the following limiting "hot spot" temperatures for electrical machinery and apparatus:

Class *A* material (treated organic)	105 degrees centigrade
Class *B* material (inorganic, plus binder)	125 degrees centigrade
Class *C* material (pure inorganic)	Not set

In the intervening years laboratory confirmation was undertaken, and a large amount of testing has been carried out by various groups. Experience in the development, design, and performance of apparatus has been a most important factor.

Transformer engineers made aging tests on insulation under oil which have formed the basis of V. M. Montsinger's eight-degree rule.[7] This rule states that the rate of deterioration of oil-immersed varnished cloth doubles for every eight-degree-centigrade increase in temperature. In the same paper Mr. Montsinger states that the rate of deterioration is less for aging in air than in oil, and in his figure 9 gives two dotted curves for air which correspond in slope respectively to 13 degrees and 26 degrees centigrade in-

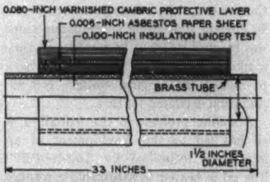
0.080-INCH VARNISHED CAMBRIC PROTECTIVE LAYER
0.006-INCH ASBESTOS PAPER SHEET
0.100-INCH INSULATION UNDER TEST
BRASS TUBE
1½ INCHES DIAMETER
33 INCHES

Figure 1. Test sample for aging class A insulation

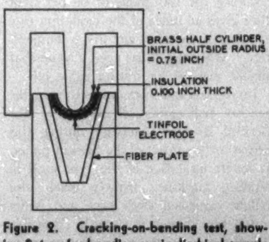
BRASS HALF CYLINDER, INITIAL OUTSIDE RADIUS = 0.75 INCH
INSULATION 0.100 INCH THICK
TINFOIL ELECTRODE
FIBER PLATE

Figure 2. Cracking-on-bending test, showing fixture for bending semicylindrical sample

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Paper number 39-7, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 21, 1938; made available for preprinting November 29, 1938.

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1. For all numbered references, see list at end of paper.

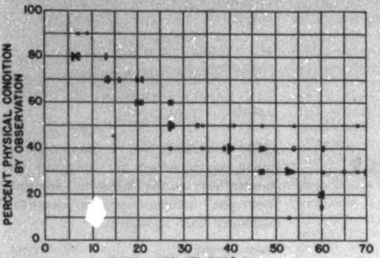

Figure 3

△—Black varnished cloth

⊙—Yellow varnished cloth

⊡—Cotton tape and black varnish

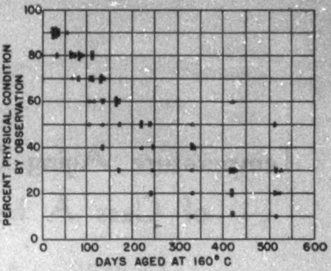

Figure 4

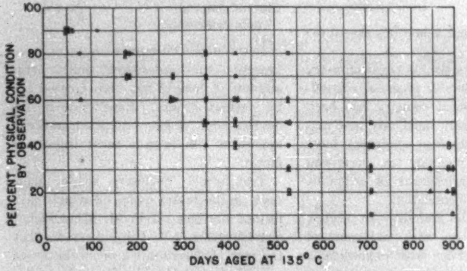

Figure 5

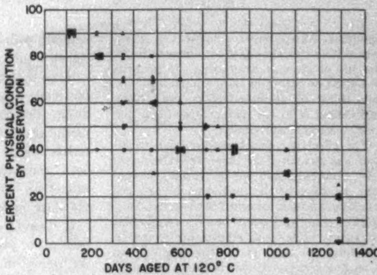

Figure 6

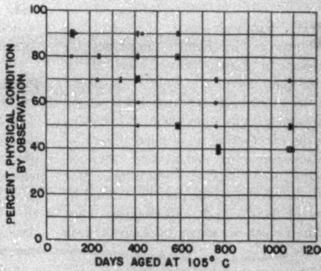

Figure 7

Figures 3–7. Physical condition by observation of class A insulation samples aged for various periods of time at 200, 160, 135, 120, and 105 degrees centigrade in ovens

crease in temperature for half the life.

At Massachusetts Institute of Technology three extensive series of tests[6] on oil-treated cable paper aged in oil as short lengths of cable showed that the mechanical properties deteriorated with time of test, but at temperatures below 90 degrees centigrade a minimum value was reached in 20 to 28 weeks and then a recovery brought the insulation back to nearly the original value. No explanation is offered for this.

A similar problem in the oxidation of oil has led to the development of an interesting technique by Dornte.[12,13,14] The oil is heated in a suitable container and oxygen bubbled through it. The number of cubic centimeters of oxygen absorbed per 100 grams of oil per hour then gives an index of the oxidation rate or aging of the oil at any given temperature. Whitehead[16] has used a somewhat similar method for testing cable oils.

Investigational work carried on in the general engineering laboratory of the General Electric Company during this period is the subject of the present paper. It covers insulation of the type used in rotating machines, namely black and yellow varnished cloth tape and white cotton tape dipped in varnish. Suitable structures with these insulations were aged in air at five temperatures between 100 degrees and 200 degrees centigrade.

Method of Measuring Aging

The criterion for the service life of the insulation of a machine is failure. If this is definitely caused by operation at uniform and long-continued high temperatures, it may safely be said that it gives a measure of the life of that insulation at that temperature for that kind of apparatus and service. In testing the life of insulation in the laboratory it has been impractical to test completed assemblies in the large numbers required to give reliable results. The total number of test specimens used in the present work was 600. Thus it becomes necessary to make tests on the material itself dissociated from the structure. In such tests some definite property of the material is selected and measured periodically after aging at several different temperatures, thus determining for these temperatures a characteristic aging curve for that property. In order to use such laboratory results to estimate the useful life of the insulation under service conditions it is necessary to select some definite deterioration in the measured property as its limit of serviceableness. It is assumed that when it has reached this condition, its useful life is ended.

Properties which may be measured include the following:

Insulation resistance
Electric breakdown
Power factor and dielectric power loss
Physical condition by visual observation
Cracking on bending (radius of bend to produce cracking)
Flexibility
Tensile strength
Folding endurance

The first three are electrical tests and the last five are mechanical tests.

In the present study, tests of the first five types were included. The observations of the physical condition of the insulation (figures 3 to 7) and cracking on bending (figures 8 to 12) show a continuous deterioration due to aging at all temperatures. The electrical properties improved in some cases with aging. For example, the insulation resistance increased as the test progressed, as might be expected due to the evaporation of moisture. The power factor of samples aged at 135 degrees centigrade decreased at first and then increased, as shown in figure 18. The breakdown voltage of the samples tested after immersion in water, given in figures 13 to 17, decreased fairly uniformly over the period of aging.

The criterion used by Montsinger[7] in his work on aging of paper in oil was tensile strength. The resulting curves given in his paper are quite consistent. Folding endurance tests were used in the studies[8] made at Massachusetts Institute of Technology. The results show the insulation aged progressively with time up to a certain point and then recovered. These two latter types of test were not adaptable to the test structure used in the present work, but it is interesting that different observers arrived independently at the greater usefulness of the mechanical test results.

Test Specimen and Aging Procedure

Since it was desired that the tests should typify deeply embedded class *A* insulation as used in motor and generator slots, the test specimen was designed in order to simulate as far as possible such insulation conditions.

Six-hundred samples were used consisting of a 100-mil thickness of the insulation wrapped on a brass tube 1½ inches in diameter and 33 inches long, as shown in figure 1. In order to exclude direct contact with the ambient air during the aging tests this insulation was protected by a 0.006-inch layer of asbestos paper and an additional 0.080-inch layer of varnish tape. The purpose of such protection was twofold. First, it was considered that in an actual machine the outer layers of insulation are the coolest and serve to protect the inner layers which, being the hottest, age most rapidly. Second, for laboratory tests it was desirable that the insulation age as uniformly as possible throughout its mass in order to get consistent results which might in turn be used with

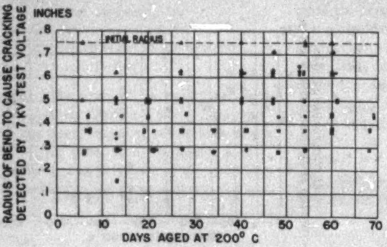

Figure 8

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

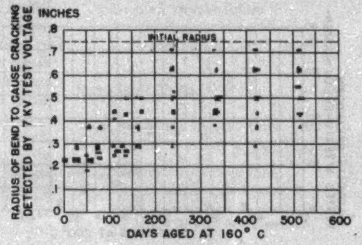

Figure 9

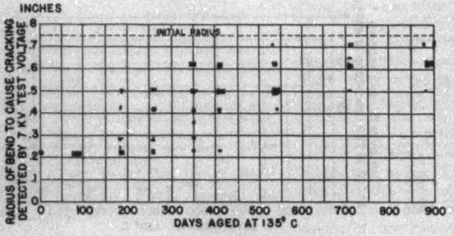

Figure 10

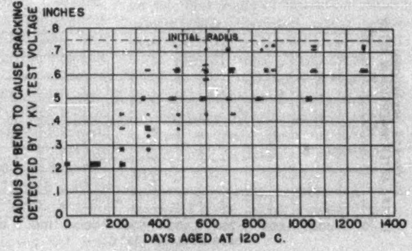

Figure 11

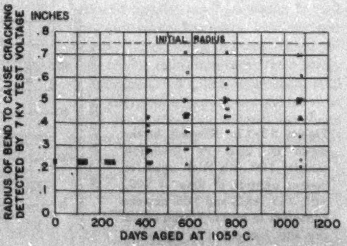

Figure 12

Figures 8–12. Radius of bend to cause cracking detected by seven-kv test voltage of class A insulation samples aged for various periods of time at 200, 165, 135, 120, and 105 degrees centigrade in ovens

other data to allow for the difference in temperature of the various layers or other conditions in an actual machine.

This choice of deeply embedded insulation and the fact that the tests were carried out in air restricted the type of test criterion. For example, tensile strength or other similar mechanical tests could not be made on the aged tapes since the entire thickness of insulation hardens during aging into a dry mass that will not permit the tapes to be unwound without tearing.

Three kinds of insulation were used in these tests:

A. Black varnished cloth tape

B. Yellow varnished cloth tape

C. White cotton tape treated by successively baked dips of black varnish

The tube samples were placed in ovens and held at the following temperatures: 200, 160, 135, 120, and 105 degrees centigrade. Once each week the heat was shut off for six hours and the oven doors opened to allow the samples to cool to room temperature and undergo the expansion and contraction that is encountered in actual operation in machines.

Samples were taken from the oven according to the schedule listed in table I. The 200-degree-centigrade tests were completed in about 2 months, the 160-degree-centigrade tests ran 18 months, while the tests conducted at 120 degrees centigrade extended almost 4 years.

Electrical Tests

The electrical tests of insulation resistance, breakdown voltage, and dielectric loss and power factor were made in the usual manner on (*a*) dry tubes as taken from the oven and cooled to room temperature, (*b*) wet tubes, soaked in tap water for 48 hours on removal from the oven.

Mechanical Tests

The mechanical tests were physical condition by visual observation, and cracking on bending.

The tests for physical condition were made by a personal observation of four samples at each test period. An expert can estimate the degree of deterioration very closely by the general appearance, breaking and rubbing small pieces with the fingers, scratching with thumb nail, etc. The chief objection to this method is the personal element involved. Where samples of different types are to be compared and classified, there is an unconscious tendency to favor those types which the examiner had previously believed to be the best. It is also very difficult to classify correctly a large number of samples unless some systematic method is followed.

With this in mind a system of classification was devised. It aims toward eliminating the personal element as far as possible and reducing the results to the simplest terms. Samples were not selected for examination in the order of

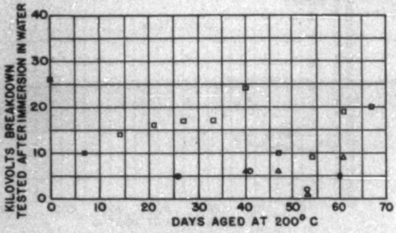

Figure 13

△—Black varnished cloth

⊙—Yellow varnished cloth

⊡—Cotton tape and black varnish

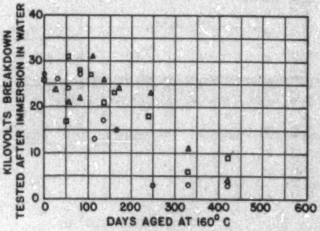

Figure 14

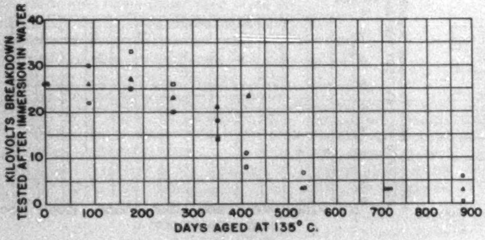

Figure 15

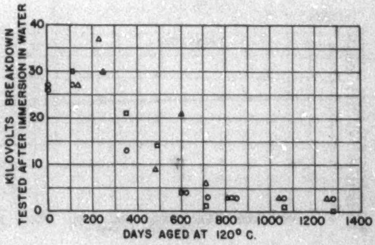

Figure 16

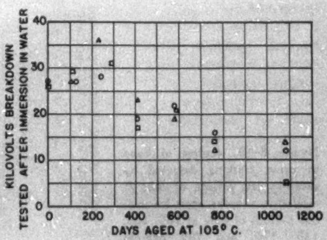

Figure 17

Figures 13–17. Kilovolt breakdown of class A insulation samples 100 mils thick aged for various periods of time at 200, 165, 135, 120, and 105 degrees centigrade in ovens. Tested wiped dry after immersion in tap water for 48 hours

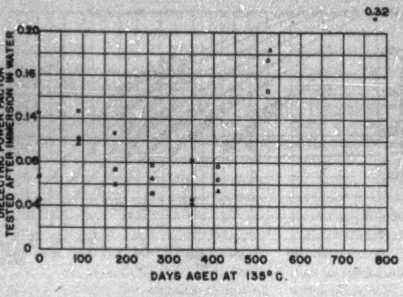

Figure 18. Dielectric power factor of class A insulation samples aged for various periods of time at 135 degrees centigrade in ovens. Tested wiped dry after immersion in tap water for 48 hours

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

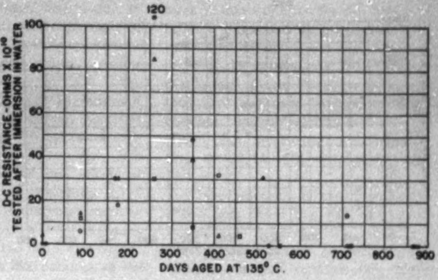

Figure 19. Insulation resistance of class A insulation samples aged for various periods of time at 135 degrees centigrade in ovens. Tested wiped dry after immersion in tap water for 48 hours

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

their aging period or type, but were selected at random. The "lining up" of the classification or rating with length of aging time then furnished a good check on the judgment of the examiner. The classification was as follows:

Per Cent Rating	Physical Condition by Visual Observation
100	Fresh, soft, and flexible (still tacky)
90	Dry but still soft and flexible
80	Dry but still flexible (hardening)
70	Dry and hard but still retaining flexibility
60	Hard and compact with very slight flexibility
50	Hard, compact, and inflexible but not exactly brittle
40	Brittle but compact and without checks
30	Brittle with checks forming
20	Brittle, checked, and partly cracked (slightly crumbly)
10	Badly cracked and crumbly (partly charred)
0	Completely cracked, charred, and crumbly

The above system of rating applies only to samples protected from exposure to air during the aging process by protective wrappings, which are removed before final tests are made. The total thickness of such insulation samples deteriorates uniformly and closely represents the inner wrappings of coil insulation where the "hot spot" occurs. When samples are exposed directly to air in an oven, the outer wrappings deteriorate faster than the inner, and a percentage rating would be very difficult to make.

Table I

Temperature at Which Samples Were Aged (Deg C)	Time in Days at Which Tests Were Made
200 (run 1)	7, 14, 20, 27, 40, 47, 54, 61
200 (run 2)	8, 14, 20, 27, 34, 41, 47, 54, 61, 68
160	27, 54, 81, 108, 136, 169, 243, 331, 419, 514
135	88, 176, 263, 350, 413, 531, 707, 877
120	114, 236, 350, 479, 600, 714, 825, 1,053, 1,283
105	114, 236, 413, 586, 761, 1,085

The test to determine the amount of cracking on bending was as follows: Six good half sections were cut from the tube samples for bending test. These were seven inches long and semicircular in cross section and included both the thickness of insulation to be tested and the section of brass tubing directly under it, having a normal radius of 0.75 inch. Using vaseline as a paste, a strip of metal foil was applied five inches long and three-quarters-inch wide in the center of the outer surface area. The section of brass tubing served as the ground electrode and the strip of foil as high-voltage electrode in the test which followed. The sample was placed in a bending machine (see figure 2) and bent from the normal tube radius of 0.75 inch until it coincided with a templet having a minimum radius of 0.71 inch. Holding the sample at this radius, seven kv* was applied for one minute. If the sample did not fail, it was bent to the shape of a second templet having a radius of 0.62 inch and seven kv again held for one minute. Continuing in steps with templets having minimum radii respectively of 0.50 inch, 0.43 inch, 0.37 inch, 0.29 inch, and 0.22 inch until sample failed at seven kv, a radius was determined which produced cracking. Five additional samples were tested and the results averaged. The radius at which this cracking occurred was used as the index of cracking on bending.

Results of Tests

As mentioned before, three kinds of insulation were tested:

A. Black varnished cloth tape
B. Yellow varnished cloth tape
C. White cotton tape treated by successively baked dips of black varnish

To secure a more reliable trend the data for all three kinds of insulation tested, namely black varnished cloth, yellow varnished cloth, and cotton tape treated with varnish, were consolidated. As these are all of the same general class there was more advantage to be gained by combining the data than to attempt separate analysis. However, in all the figures these materials are indicated by separate codes, triangles, circles, and squares respectively representing black varnished cloth, yellow varnished cloth, and cotton tape treated with black varnish.

The complete plots of physical condition are presented in graphic form as follows:

Figure 3. Physical condition–days aged at 200 degrees centigrade
Figure 4. Physical condition–days aged at 160 degrees centigrade
Figure 5. Physical condition–days aged at 135 degrees centigrade
Figure 6. Physical condition–days aged at 120 degrees centigrade
Figure 7. Physical condition–days aged at 105 degrees centigrade

These curves show a definite reduction in the physical condition by observation

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* The normal breakdown of these tubes when new would be 30 kv approximately.

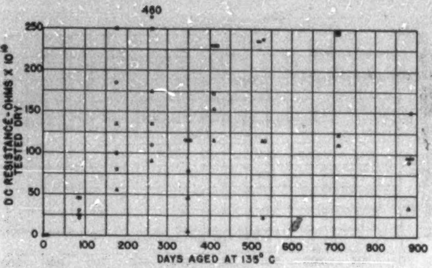

Figure 20. Insulation resistance of class A insulation samples aged for various periods of time at 135 degrees centigrade in ovens. Tested dry

△—Black varnished cloth
⊙—Yellow varnished cloth
⊡—Cotton tape and black varnish

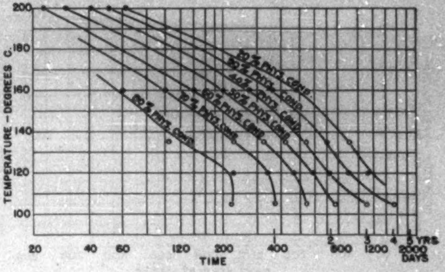

Figure 21. Curves showing the relation for class A insulation between temperature and time of aging to produce various physical conditions as determined by observation

as the aging progresses. Also, the higher the temperature the more rapid the deterioration to any given condition and hence the shorter the life. While the points are scattered, fairly representative average values can be obtained on account of the large number of samples used. The intersection of these average curves with the ordinate for any per cent physical condition gives the time required for the insulation to reach that condition at that temperature. It should be noted, of course, that the per cent physical condition represents the arbitrary evaluation previously given and there is not necessarily the definite relation between the conditions which the numbers would indicate. Nevertheless, on account of the number of conditions used, any resulting errors tend to be minimized as may be seen from the curves.

The complete plots of the results of the cracking on bending test are presented in graphic form as follows:

Figure 8. Radius of bend–days aged at 200 degrees centigrade

Figure 9. Radius of bend–days aged at 160 degrees centigrade

Figure 10. Radius of bend–days aged at 135 degrees centigrade

Figure 11. Radius of bend–days aged at 120 degrees centigrade

Figure 12. Radius of bend–days aged at 105 degrees centigrade

These curves show that when nearly new, the insulation wall may be bent from its initial radius of 0.75 inch to almost 0.2 inch before cracking occurs for an aging temperature of 105 degrees centigrade, but that when aged upward of 1,200 hours, it may crack when deformed only slightly to 0.71 inch. At 120 degrees centigrade the upward drift of the radius at which cracking occurs is more clearly shown as at this temperature the test time was long enough to show definitely the life by cracking. The other higher temperatures show similar trends. A comparison of the curves shows that the higher the temperature the shorter the time to cause cracking at any given radius of bend and thus the shorter the life. The points in these diagrams are also scattered, but again fairly representative values can be obtained due to the large number of samples used. The results at 200 degrees centigrade show quite a spread and emphasize some of the difficulties attendant on too highly accelerated tests carried out at extreme temperatures.

The complete plots of the results of kilovolt breakdown taken on samples previously immersed 48 hours in water and then wiped dry are presented in graphic form as follows:

Figure 13. Kilovolts breakdown–days aged at 200 degrees centigrade

Figure 14. Kilovolts breakdown–days aged at 160 degrees centigrade

Figure 15. Kilovolts breakdown–days aged at 135 degrees centigrade

Figure 16. Kilovolts breakdown–days aged at 120 degrees centigrade

Figure 17. Kilovolts breakdown–days aged at 105 degrees centigrade

This group of curves shows in general a gradual decrease in breakdown voltage as the period of aging is increased, especially in the case of the samples aged for the longer times at the lower temperatures.

Typical data on insulation resistance and power factor are shown in:

Figure 18. Power factor–days aging at 135 degrees centigrade

Figure 19. Insulation resistance–days aged at 135 degrees centigrade (after immersion in water)

Figure 20. Insulation resistance–days aged at 135 degrees centigrade (dry)

The insulation resistance and power-factor test data were of such a nature that the dependence of the properties on the aging period was not so clearly defined as in the tests for physical conditions, cracking on bending, and kilovolts breakdown after immersion in water. For the purposes of this presentation the discussion has been confined to the results obtained in these last three types of test. Thus, although a complete series of tests was made for each different temperature, only a set of curves taken at 135 degrees centigrade is presented to illustrate the general tendency of the insulation resistance and power factor.

Aging Curves at Various Temperatures

The data on physical condition, cracking on bending, and breakdown voltage after immersion in water were reworked into the form of aging curves at various temperatures.

In the case of physical condition the procedure was as follows: On figures 3 to 7 average curves were drawn through points of average physical condition for a given time of aging. From these average curves the times required for the insulation to reach selected values of physical condition were read. These times were then plotted against the temperature of aging, yielding the curves shown in figure 21.

The procedure for the cracking on bending data was similar. In figures 8 to 12 inclusive average curves were drawn and the time noted at each temperature to reduce the sample to a condition in which it would have cracked if distorted to a given radius. The resulting curves, figure 22, give the days aging for each temperature to produce cracking on bending for four radii of bend.

For the breakdown-voltage curves the measure of the aging was taken as the time for the breakdown voltage to decrease to arbitrarily selected values. In figures 13 to 17 inclusive, average curves of voltage were drawn and the time estimated at each aging temperature for the breakdown voltage to be reduced to 10 kv and 20 kv. The times required to cause the samples to fail at these voltages are plotted in figure 23.

Figures 21, 22, and 23 summarize the results of these aging tests. They represent the aging time at various temperatures to reach certain conditions of deterioration. They indicate that up to 200 degrees centigrade there is no critical temperature above which the insulation suddenly fails, but that for each temperature there is a definite time required for the insulation to reach a given condition due to heat aging, and that in general the lower the temperature the longer the life.

Comparison of Foregoing Results

For the purpose of comparing the results obtained by the three different methods, that is, physical condition by visual observation, cracking on bending, and kilovolts breakdown, figures 24 and 25 were prepared. The former shows, plotted together, the curves for 60-per-cent physical condition, 30-per-cent physical condition, 0.6-inch radius to produce cracking on bending, and 10-kv breakdown. Figure 25 shows the same data plotted on a logarithmic time scale. It will be noticed that, although each of these three curves has a different basis of selection, they have roughly similar shapes.

The underlying causes of the changes of shape in some of these curves, especially in the region between 110 and 140 degrees centigrade, present an interesting field for study. One of the first effects of heat is to drive off moisture. Continued application of heat may result in a direct chemical change in the molecules of the organic material itself, or a change due to oxidation in the presence of the atmosphere which might not occur in an inert atmosphere. Further changes may then result due to by-products of the oxidation process. To make a scientific study of the temperature aging of insulation these and other variables would require separate investigation. When the variables in the materials themselves are considered as well, it is evident that life-test results must be interpreted with caution.

Discussion of Results

After years of labor involving the most careful preparation of hundreds of test specimens, the maintenance of these at constant temperature in ovens provided with proper temperature distribution for the purpose, the testing of these after aging for various properties, the tabulation of thousands of results, we finally arrive at summary curves as shown in figures 21 to 23.

Some authors[7,11] have proposed rules for calculating the rate of deterioration of insulation as a function of the temperature, such as that the life of the insulation is reduced one-half by each ten-degree-centigrade rise in the operating temperature. The results of the present tests indicate that while such rules may furnish approximations for purposes of simplification in practical applications, due care must be taken to allow for the difference between the actual materials and structures used in the laboratory tests and those of the apparatus under operating conditions. In addition, since the curves show that the rate of deterioration is not constant, too great reliance should not be placed upon short-time tests alone, but they should be supplemented by long-time tests.

At the beginning of the investigation it had been hoped that the work of obtaining such a curve would result in a standard of reference so that as new materials became available they might be tested at higher temperatures to obtain their aging characteristics in a relatively short time, and if these appeared to be outstanding, further investigational work would be carried on toward a more complete evaluation of the material at the lower temperatures. To some extent this objective has been attained, although considerable work is still involved in such tests.

As soon as results are obtained from laboratory tests of any kind there is the immediate urge to correlate them with practical results, and so it has been with these tests. The first reaction is probably a surprise as to the shortness of life as compared with the long life of apparatus in service, but when it is considered that these values are the result of continuous aging at temperatures 105 degrees centigrade and higher, the results are not so startling. In fact, where opportunity has arisen to compare these results with the life of insulation in practice, which was known to have been operated substantially continuously at a given temperature, the correlation has

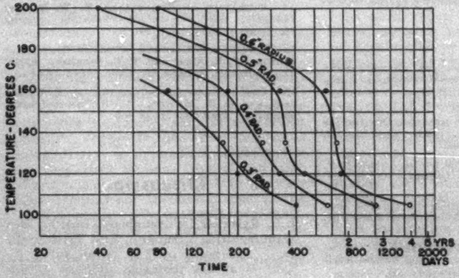

Figure 22. Curves showing the relation for class A insulation between temperature and time of aging to produce cracking by bending to various radii of curvature

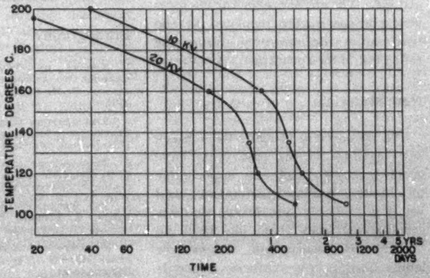

Figure 23. Curves showing the relation for class A insulation between temperature and time of aging to reduce the kilovolts breakdown tested after immersion in water for 48 hours to 10 kv and 20 kv for 100-mil thickness of insulation

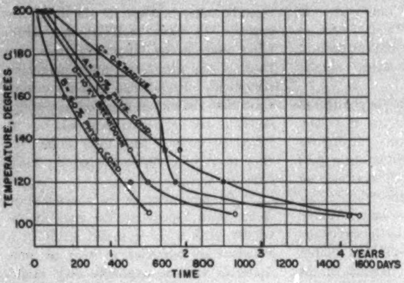

Figure 24. Aging curves of class A insulation, showing the relation between temperature and time of aging

A—Aged to 30-per-cent physical condition

B—Aged to 60-per-cent physical condition

C—Aged to crack on bending to 0.6-inch radius

D—Aged to ten-kv breakdown after immersion in water

Arithmetic scale

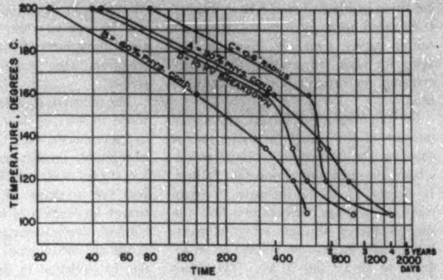

Figure 25. Aging curves of class A insulation, showing the relation between temperature and time of aging

A—Aged to 30-per-cent physical condition

B—Aged to 60-per-cent physical condition

C—Aged to crack on bending to 0.6-inch radius

D—Aged to ten-kv breakdown after immersion in water

Logarithmic scale

been such as to indicate reasonable agreement. Probably the greatest benefit of these aging tests has been to allow design engineers to see the effect of continuous operation at elevated temperatures upon the insulation and to observe the shortness of the resulting life.

Another result of this investigation has been to show the reasonableness of the 105-degrees-centigrade hot-spot temperature for purposes of standardization and for reference since it is quite plain that above this temperature deterioration increases rapidly under continuous operation, whereas below it the indications are from an extrapolation of the curves that the life is long. This value therefore represents the results of engineering judgment based on technical knowledge to obtain optimum service so that full use will be obtained of the insulation strength and life. The allowance for various service conditions is then regulated by the temperature rise measured during the rating test of the apparatus.

New Materials

The advances in bringing forth many new materials in the past years have given to design engineers opportunity for obtaining increased performance, and yet it is probably safe to say that for cellulose materials there has been no fundamental increase in temperature limit. Certain new synthetic materials give opportunity for some increase which can be used to advantage, and it may be that work should be undertaken to classify these materials more directly. It may be satisfactory for their temperature limit to be placed somewhat above that of class *A*, but they surely are not class *B*.

Conclusions

A method of testing the aging of insulation at various temperatures between 105 degrees centigrade and 200 degrees centigrade using carefully constructed laboratory samples has been described. A large number of samples was used. Three different methods of tests were applied. Each method was successful in indicating progressive deterioration of the samples during aging, and the agreement between the results obtained by the three different test methods is also reasonably good. Thus it appears that the methods used may be applied with confidence to the further evaluation of new materials.

The purpose of presenting this material at this time in connection with other papers bearing on the rating of motors run on interrupted duty is to give available data on the effect of temperature on life and to bring forth in discussion from others the data and experience they may have relating to this problem.

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Discussion

J. L. Rylander (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The paper by J. J. Smith and J. A. Scott is a very desirable addition to the subject of insulation. Little test data of this kind had been obtained and seldom has any of it been published.

These data are very useful in the consideration of the permissible operating temperatures of electrical machines with class *A* insulation. However, I wish to draw attention to some other features that must be taken into the consideration of safe operating temperatures of machines with class *A* insulation.

It should be noted that these are electrical and mechanical tests on the insulating materials themselves, but not on the motors or generators or even complete windings or coils themselves.

The matter of the design of the windings and all insulation details have a marked effect on the life of the windings at higher temperatures. The weakest detail of insulation determines when the winding fails.

The effect of any temperature above the boiling point of water has a decided effect whenever the insulation has absorbed moisture.

The shrinkage of insulation will often determine the life of many motors and generators. This is most noticeable where there are strong mechanical stresses in the windings due to either centrifugal force or to the stresses due to very heavy currents when starting or at any other time. Shrinkage is noticeable when all of the moisture content is removed from the insulation and this occurs when the boiling point of water is reached.

As stated in the paper there are no formulas for determining the approximate life of windings at various temperatures, but a couple empirical formulas are referred to as having been previously suggested by others. I developed the following formula for my own use from all data that has been available:

$$\text{life} = \frac{K}{(T - A)^2}$$

where K is practically a constant but depends on the general construction or type of winding and T is the operating temperature in degrees centigrade. A is practically a constant but depends upon the kinds and grades of the insulations used. The writer usually uses the value 83 for A. This formula indicates that if you want a motor with class A insulation to operate for a very long period such as a hundred years, the maximum temperature would be about 83 degrees centigrade.

P. L. Alger (General Electric Company, Schenectady, N. Y.): There are two highly significant trends in recent insulation developments.

First, there is the development of a multitude of new synthetic materials, whose composition and manufacture are carefully controlled from raw material to finished product. These include the chlorinated hydrocarbons, or Pyranols, for transformers and capacitors, Formex, Glyptal, and other new enamels and varnishes, fiber glass for high temperature insulation, and a vast number of new resins or plastic compounds.

Second, there is the growing use of controlled atmospheres for industrial operations and especially for electrical apparatus. These include hydrogen for rotating machines, nitrogen for transformers and cables, carbon dioxide for explosion-proof motors, sulphur dioxide and Freon for hermetic refrigerator motors, and other gases for high-voltage apparatus.

Both of these trends bring the chemist into the picture, and force the electrical engineer to study chemical reactions, rather than merely physical changes in insulation. There is a basic law of chemical reaction which requires that all chemical changes take place at an accelerated rate as the temperature increases, the rate being approximately double for every ten-degree increase in temperature.

It is, therefore, evident that our future progress will be largely determined by how effectively we bring chemistry to bear on our insulation problems. We must initiate a broad program for development of new insulating materials by chemists. We must ask the chemists to devise new methods of accelerated life tests for these new materials. And, we should ultimately revise our basic standards of temperature rating of apparatus to enable these new materials to be utilized to the fullest degree.

V. M. Montsinger (General Electric Company, Schenectady, N. Y.): I am very much interested in this paper as the authors' work indicates that the rate of change of aging is not constant over the entire range of temperatures from 105 to 200 degrees centigrade. In other words, instead of the rate doubling for a definite number of degrees increase in temperature, it requires an increase of some 20 to 30 degrees in the lower range, and some 10 to 15 degrees in the higher temperature range to double the rate of aging.

It can readily be seen that heavier overloads could be allowed by the curves given in this paper than would result from using the eight-degree rule which we have used in the past.

Until recently, so far as I know, with the exception of L. C. Nichol's work (reference 9), no attempt has been made to estimate to what extent short-time heavy overloads use up the life of the insulation. If a changing rate of aging versus temperature (as indicated in the paper) is used, it becomes practically impossible to integrate the time-temperature areas for short-time overloads. Even when using a simple rule like the eight-degree rule it is no easy task, for the reason that it is difficult to calculate the hottest spot temperature and then to resolve the time-temperature areas into forms suitable for integration.

In most types of rotating machinery, heavy overloads usually are not limited by temperature but by other factors like torque, stalling, commutation, etc. Where the amount of overload is limited for electrical reasons to moderate values, it appears that the effect of overload on the aging of the insulation can be closely approximated by using some simple method like the eight-degree rule.

Last year I had an opportunity to analyze short-time heating conditions applying to neutral grounding devices which are called upon only under line fault conditions to function under load, when fairly high temperatures may be reached for time periods ranging from one to ten minutes or more. By integrating the temperature-time area and using the eight-degree rule, temperature limits were approximated which were conservatively safe.

Where the aging of the insulation changes at a constant rate, that is, where it doubles for each 8, 10, or 12 degree increase in temperature, the temperature-time area can be integrated to obtain the amount of aging per cycle by the following equation:

$$A = t\left[\frac{\epsilon^{XT_2} - \epsilon^{XT_1}}{X(T_2 - T_1)}\right] \tag{1}$$

where

A may be designated as aging units.

t = time, per cycle
ϵ = 2.718
T_2 = maximum temperature
T_1 = initial temperature
X = constant
= 0.088 when aging doubles for each 8-degree increase
= 0.0695 when aging doubles for each 10-degree increase
= 0.059 when aging doubles for each 12-degree increase

It is usually more convenient to use a formula that gives the per cent of life used up per cycle of operation. This can be done by dividing the aging per cycle equation 1 by an assumed life at a given constant temperature.

For estimating the life of insulation as determined by tensile strength, I have used the following formula:

$$Y = 7.15 \times 10^4\epsilon^{0.088T} \tag{2}$$

where

Y = life in years
T = temperature in degrees centigrade

It is well to point out that the laboratory tests reported in my 1930 paper (reference 7) indicate that the constant in equation 2 should be 4×10^4 which would give a life of four years at 105 degrees centigrade. My experience, however, has been that we should depend mostly on laboratory tests on small samples of insulations to give us the relative "rates of aging" at different temperatures, but that the life of transformers can best be determined by life tests made on actual transformers. Both laboratory aging tests on transformers and field experience have shown that the actual life of transformers operating at approximately 105 degrees hot test spot is more nearly like seven years than four years.

According to equation 2 the life of insulation is gone at the end of approximately seven years operation at 105 degrees centigrade. This is in fair agreement with the figure 21 which shows that the insulation has from 20 to 30 per cent of its strength left after operating from four to five years at 105 degrees centigrade. It is at the higher temperatures that the agreement is not so good because the curves in figure 21 show that the temperature must be increased more than eight degrees to double the rate of aging.

While the eight-degree rule may not be rigidly correct, particularly for high temperatures, I prefer to use it since it is more conservative than the data shown in figure

21. Even when using the eight-degree rule, one will be surprised at the large number of times that insulation can be subjected to short-time overloads, without using up its life.

If the eight-degree rule is used, and if equation 1 is divided by the constant in equation 2, and the number of hours per year, the following equation results:

$$F = \frac{t(\epsilon^{0.088T_2} - \epsilon^{0.088T_1})}{7.15 \times 10^4 \times 24 \times 365 \times 0.088(T_2 - T_1)}$$

$$= \frac{t(\epsilon^{0.088T_2} - \epsilon^{0.088T_1}}{K(T_2 - T_1)} \qquad (3)$$

where

F = fractions of life used up per cycle
K = 55.1×10^6 for time t expressed in hours per cycle

If t is expressed in minutes per cycle $K = 330.6 \times 10^7$.

Charles F. Hill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The method of taking data by visual observation, introduced by Smith and Scott, is of considerable interest in the study of insulation deterioration. These observational data are quite consistent, even more so than the actual physical measurements, but are arbitrary in that the various six or eight stages of deterioration are established by mere definition. It would seem necessary to have simultaneous physical measurements before much confidence could be placed in such inspection data, but it is my opinion that such observations are quite worth while.

There is a considerable discrepancy between the physical data and observational data by Smith and Scott as is shown in figure 25 of their paper in the interval of 400 to 800 days operation where the mechanical data dips sharply. I am wondering if this dip is not due to some accidental phenomenon in the test.

Comparing the general rate of deterioration between the data by Smith and Scott and my own data ("Temperature Limits Set by Oil and Cellulose Insulation," AIEE TRANSACTIONS, volume 58, 1939, pages 484–91), it would appear that the rate in oil with a rather inert gas present is slightly faster than that for the test in restricted air by Smith and Scott. This is a surprising result, although not so impossible as our inert gas was not 100 per cent oxygen free. Small amounts of oxidation products of the oil might have a serious influence on the varnished cellulose materials.

In the discussion of results, Smith and Scott have reached a conclusion which, I believe, merits further emphasis. During the past few years, it has become customary for insulation engineers to speak of insulation deterioration rates doubling for each ten-degree-centigrade temperature rise. This idea has arisen because of the fact that in chemical reactions, rates of reaction do tend to change by a constant factor for constant temperature intervals. This is under the assumption, however, of constant materials and constant conditions of concentration, etc. Smith and Scott point out that such a rule does not hold for their life tests. We have also reached the same conclusion. The reason for the failure of this chemical law to hold for insulation life tests lies chiefly in the fact that in life tests, the material being tested is not the same material after a few weeks or months. The result is a very different rate of deterioration after some months than would be expected if the material remained a constant factor. Smith and Scott point out the danger from short-time tests. I would like to emphasize this particularly in that extrapolating rates from short-time tests may be very misleading.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper is extremely interesting and adds considerably to our knowledge. A few points that strike me are as follows:

1. Results of the tests tend to scatter considerably, as has been found by practically all investigators making any sort of comprehensive study.

2. A relatively short life will prevail for class *A* insulation operated continuously at the present "hot spot" temperature limit of 105 degrees centigrade of the AIEE rules. In my paper at this convention on "Load Ratings of Cable" (see 1939 annual TRANSACTIONS index for page numbers) I have indicated that for continuous operation it seems best from the standpoint of obtaining long life to have maximum temperatures not exceeding 85 or 95 degrees centigrade depending on how "continuous" the operation is at the higher temperatures.

3. If class *A* insulation is operated in normal service as is usually done, that is, at temperatures considerably below 105 degrees centigrade, then it becomes feasible to operate during emergencies at temperatures in excess of 105 degrees centigrade, presuming that such emergencies are infrequent and the temperature of the insulation is at the highest values for only a few hours during each emergency.

4. The operation during emergencies appears to do little more harm if the maximum temperature is around 140 than if it is around 120 degrees centigrade according to figures 22 and 23. In view of these unusual findings, it appears that the conclusions in my paper about operating low-voltage cables during emergencies at temperatures up to 120 degrees centigrade may be conservative.

In a series of six-month tests made at 100 and 125 degrees centigrade in Chicago on varnished cambric insulated cables, it was found that the rating of the quality of the various insulations as determined by mechanical and electrical tests on insulation removed from sealed cables was different from the rating found on the same insulations when tested in tape form in air. When heated as tapes exposed to air, the materials first hardened and improved in tensile and electrical strength but became more brittle. Some materials which appeared to be superior in such tests were found to become stuck together when heated in cable where air is excluded so that the cable could no longer withstand much bending without breaking the insulation.

J. J. Smith and J. A. Scott: We have endeavored to point out in the paper and it is again emphasized by Mr. Rylander that there are other factors in addition to the effect of temperature to be taken into consideration in determining the safe operating temperature of a machine. However, it is desirable to study these effects one at a time and therefore the present work was limited to the effect of temperature alone on the aging of insulation. We note Mr. Rylander's simple form of equations for life and it would be interesting if he added the value of *K* for some typical applications.

Mr. Alger points to the introduction of many new materials in recent years. The introduction of Formex wire is an example of such a development. As more of these materials become available, testing will be required to evaluate their performance compared with the older materials and the paper suggests a technique which may be used for this purpose.

Mr. Alger and Mr. Hill discuss the bearing of chemical changes on the aging problem and refer to a law the chemist often finds that the rate of change doubles for each 10-degree-centigrade increase in temperature. Such a law frequently holds for a single definite chemical reaction, and we would have been pleased if it had turned out that way in the present tests, but it did not. As pointed out in the paper, the consideration of the aging reactions from a physical and chemical standpoint as well as the effect of the structure itself is a subject worthy of more detailed study.

Mr. Montsinger's eight-degree rule is quite widely accepted in transformer practice and from all the evidence is conservative, as he points out when used to estimate the effect of increases in temperature. However, when the effect of decreases in temperature is considered as, for example, in estimating the service life of materials from short-time laboratory tests at elevated temperatures, the eight-degree rule may indicate a much longer life than is actually shown by these tests. In other words, the available evidence indicates that the actual rate of aging is not a simple law, but is dependent on many individual circumstances, so that the only safe basis for conclusions is actual operating experience.

Doctor Hill asks if some accidental phenomenon might account for the difference in the shape of the curves of physical and observational data in figure 25 between 400 and 800 days. These results were checked back to the original data and confirmed.

Doctor Hill points out that the aging rate he found in oil with inert atmospheres was faster than the rate in air given in our tests. Montsinger in his 1930 AIEE paper "Loading Transformers by Temperature" reports a similar behavior. This question of inert atmospheres is a fruitful field for future work, with the extended use of hydrogen cooling and other enclosed types of motors.

We are interested in Mr. Halperin's conclusion from work in connection with cable that the life of insulation operated continuously at the present "hot spot" temperature limit of 105 degrees centigrade is relatively short and his indication of a continuous temperature not in excess of 85 to 95 degrees centigrade. With regard to his discussion of higher operating temperatures in item 4 it should be remembered that the structure used in our tests is not the same as that in which he is interested and thus the results may not be directly applicable. His description of the results of aging tests on cable insulation both when sealed and aged in air indicate the necessity for care in comparing the results of aging tests made under different conditions.

Rating of General-Purpose Induction Motors

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Synopsis: This paper reviews principles of rating of general-purpose a-c motors, particularly in relation to overload torque and temperature limits.

The overload capacity of an electric motor is limited, first, by its stalling, or breakdown torque, and, second, by its operating temperature. The breakdown torque, analogous to the stalling torque of a gas engine, is roughly proportional to the motor size and the square of its magnetic flux density. The temperature limitation is exactly analogous to the temperature limit of a transformer, the useful life of the insulation being reduced exponentially as the temperature is raised.

The increased variety of motor uses in recent years, especially for automatic operation of refrigerator compressors, air conditioning, pumps, and other mechanical devices, has led to more exact methods of application, utilizing motor overload capacity and matching torque characteristics to the driven equipment. Under this economic pressure, motor overload capacities have been increased over the requirements of present standards, and small motors are now commonly used on intermittent overloads far beyond their continuous ratings.

It is proposed, therefore, that American standards be revised to provide for increased values of breakdown torque and service factor in the smaller motor ratings, and that permissible intermittent duty cycles be defined, enabling the full economic life of the motor to be utilized. It is also proposed that the starting current be recognized as a convenient and accurate measure of induction-motor breakdown torque, or momentary torque capacity, and that starting current values be established on a logical and consistent basis for both single-phase and polyphase motors.

Specific recommendations are given in the paper for rated characteristics and operating limits under this starting current-temperature system of rating, and the economic advantages to the industry to be gained by their adoption are pointed out, including safer wiring and control systems and a reduced variety of special motors.

THE importance of an adequate system of rating for industrial motors can hardly be overemphasized. The rated horsepower of a motor is a measure of its working ability, for whose integrity the entire electrical industry is responsible. The name-plate rating implies a host of different qualities built into the motor, including overload and starting ability, temperature endurance, high potential strength, and other matters covered by national standards. For the economic use of motors, the fair comparison of competitive designs, the maintenance of a proper and not excessive variety of types, the intelligent handling of power supply and control problems, and for many other reasons, it is essential that American standards of rating convey a definite guarantee of balanced characteristics and quality in motor design.

The essence of the rating problem is to find a simple test procedure that will uniquely define the output limitations of the apparatus in question. The outputs of gas engines, steam locomotives, pumps, turbines, and other mechanical apparatus are limited by mechanical considerations. Their continuous output ratings are, therefore, very little below their maximum momentary capacities, and users do not expect to load them appreciably beyond their ratings, even momentarily. On the other hand, the output of a transformer is limited almost entirely by thermal considerations, the theoretical point of maximum output with a constant voltage supply being far beyond the safe thermal limit. Hence, transformer users may permit high short-time overloads, so long as prescribed temperature limits are not exceeded.

Electric motors are subject to mechanical as well as thermal output limitations, both of which must be recognized in a practical rating system. The thermal limits are controlling in continuous operation, with present insulating materials, so that the close similarity between motor and transformer methods of rating that has always existed is entirely logical. In many cases, however, such as hermetic refrigerator motors, responsibility for cooling is entirely in the user's hands, so that usual temperature-rise guarantees will not be made by the motor manufacturer. Future trends will, therefore, assuredly require a rating system based on torque ability alone. The object of the present paper is to propose a transitional rating system, in which the temperature limits will be supplemented by other size-defining requirements.

History

A thorough discussion of the motor rating question before the Institute and by the entire industry some 12 to 18 years ago resulted in the adoption of the present American system of a single continuous rating with 40 degrees centigrade rise for general-purpose motors. The conclusions at that period were ably summed up by C. L. Collens in a paper published in the August 14, 1926, issue of *Electrical World*, from which the following statements are quoted:

1. Any basis of rating is at best merely an arbitrary designation of size. It is merely one of many that might be chosen.

2. Rating alone is insufficient and must always be supplemented by a clear definition of the service conditions for which the rating is chosen. In fact, determination of the usual service conditions must necessarily precede the determination of a suitable basis of rating.

3. Rating alone is an insufficient indication of the inherent ability of the motor to perform satisfactorily under service conditions and duty cycles differing from the usual. It is merely one indication of size and must be supplemented by other service information to permit of intelligent selection and economic application.

In concluding his paper, Mr. Collens made the following recommendations:

1. The division of industrial power motors into two classes with the dividing line at 200 horsepower, and in each class:

2. A normal continuous-duty single rating for the open-type motor as the standard designation of size.

3. Well-defined usual service conditions for the normal rating.

4. Service information showing permissible loadings under other duty cycles or other service conditions different from the usual service conditions.

5. Specialized motors with special ratings only where the performance characteristics required or the nature of the duty cycle do not permit of applying a service factor to the normal rating of the standard motor.

Four of these five recommendations were carried into effect in the AIEE and National Electrical Manufacturers Association standards more than ten years ago, and experience since has well justified this action. Mr. Collens' fourth suggestion, however, that information should be prepared, showing permissible loading of standard motors under other duty cycles or unusual service conditions, has never

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Paper number 39-2, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 20, 1938; made available for preprinting November 23, 1938.

P. L. ALGER is staff assistant to engineering vice-president and T. C. JOHNSON is electrical engineer, General Electric Company, Schenectady, N. Y.

been adequately carried out nor incorporated in the industry standards.

The progress of the art during the past ten years, including the development of the modern automatically controlled cyclic loads of air conditioning and refrigeration, has brought a tremendous increase in number and variety of motor applications. The recent trend has been to develop many special motors, each adapted to drive a particular piece of mechanical equipment, often with overload and starting abilities much in excess of those normally associated with their name-plate continuous ratings.

The objectives of revised standards should be to specify a standard type of motor adapted for the greatest variety of applications and to facilitate the economic use of the full capacity of the motor under all service conditions.

The Present Rating System

Present American standards[1] provide for two broad classes of continuous-rated motors.

General-purpose motors (200 horsepower or less and 450 rpm or more) have a single continuous rating, but must be suitable for carrying 115 per cent of rated load continuously under usual service conditions, with the ambient temperature 40 degrees centigrade or lower. These motors are offered in standard ratings for use without restriction to a particular application. They are required to meet the low limiting temperature rise of 40 degrees centigrade by thermometer at rated load, to allow a greater factor of safety where the service conditions are unknown.

Special-purpose motors, specifically designed for a particular power application where the load requirements and duty cycles are definitely known, have a single continuous rating of 50 degrees centigrade rise by thermometer, without any continuous overload requirements.

The standards also specify minimum values of starting, pull-up, and breakdown torques for each type and class of motor.

These provisions should undoubtedly be retained, but it appears desirable to add to or modify them in four respects:

First, the standards should include operating recommendations for general-purpose motors in intermittent or varying load service and in different ambient temperatures, so that the inherent overload capacity of the motor can be safely utilized. This is in accordance with the action already taken in the recently proposed American standards for transformers.

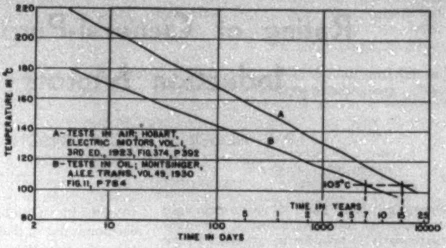

Figure 1. Life expectancy versus temperature for class A insulating materials in continuous service

Second, the general use of small motors in intermittent rather than continuous service should be recognized in the standards by requiring relatively greater starting and breakdown torques, and greater temporary overload capacities, than for larger motors. Unless such provisions are made, off-standard motors will be used to an increasing extent, and control problems will be complicated, to the detriment of the public as a whole.

Third, the more general use of various protected motor designs suggests that their allowed temperature rises be reviewed and their ratings be made more nearly comparable with general-purpose motors.

Fourth, the more complete utilization of motor overload capacities, implied by this program, should be accompanied by more exact determination of insulation temperatures. In many modern designs, especially of protected* motors, the windings are quite inaccessible, and thermometer readings on exposed parts do not accurately measure hot-spot temperatures. It appears desirable, therefore, for the standards to require that stator-winding temperatures be measured by resistance. This fourth question is the subject of a companion paper.[2]

Overload Capacity of Standard General-Purpose Motors

Assuming adequate mechanical strength, the measure of a motor's momentary overload capacity is the adequacy of its torques, giving assurance that the motor can bring the load to speed and carry it under low voltage, high friction, or other unforeseen temporary conditions. American standards now require that general-purpose polyphase induction motors shall have a breakdown torque of not less than 200 per cent. Allowing for ten per cent reduction in voltage and 20 per cent margin for variations in individual conditions of loading, this 200 per cent breakdown torque will enable loads not over 135 per cent of the rating to be carried successfully, subject to heating limitations.

Therefore, under the present standards, motors cannot be relied upon to carry momentary overloads of more than 35 per cent in excess of the rating, under a reasonable variety of service conditions. In practice, designers normally provide more breakdown torque than required by the standards, especially for the smaller and higher-speed motors, so that many present designs can carry considerably greater short-time overloads.

It should be remembered that the starting current of a large polyphase induction motor is almost directly proportional to the maximum or breakdown torque. The starting current is, therefore, an excellent measure of short-time overload ability.

With adequate torque margins, the remaining important factor in overload capacity is the temperature rise. This must be low enough to ensure adequate service life under the expected overloads. While there may be other objections to high temperatures in special cases, the chief purpose of limiting the standard temperature rise is to protect the public from the inconvenience and loss that would be occasioned by motors with a short insulation life. As the actual life that a motor will have under a given temperature cannot be determined by acceptance tests alone, it is peculiarly important that the standards provide for safety in this respect.

When and if insulating materials of greater temperature endurance come into use, they may be utilized to permit reduction in motor size, with a higher continuous rise. It seems desirable, however, to use such higher temperature materials and limits first on totally enclosed machines, permitting interchangeable dimensions with open-type motors of present temperature limits. For open-type motors with liberal overload torques, and reasonable efficiencies, it costs very little to provide ventilation adequate to meet present temperature limits. The only apparent gain from higher temperature limits on open-type general-purpose motors, therefore, is a small saving in the cost and losses expended in the ventilating system. For enclosed machines, however, a definite size reduction with increased temperature can usually be made without impairing operating characteristics, and this benefit may justify developing new higher-temperature insulation systems.

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[1] For all numbered references, see list at end of paper.

* Protected is used here to describe partially or fully enclosed motors as a class.

It is evident that the increase of copper resistance with temperature causes a decrease in efficiency with every increase in the operating temperature. In a typical case of a motor with 90 per cent full-load efficiency at 75 degrees centigrade, the efficiency will be reduced 0.5 per cent by an increase to 105 degrees centigrade copper temperature, or by 1.25 per cent if the temperature is raised to 150 degrees centigrade. Such an increase in the losses further raises the temperature, causing cumulative heating, or "temperature creep," as indicated in figure 4. This forms an effective limitation on high normal temperature rise, or continuous overloads. Rapid oxidation of oil and consequent need for separately cooled bearings and frequent oil renewal at temperatures above 100 degrees centigrade are additional reasons for limiting temperatures in continuous service.

Temperature-Life Characteristics of Insulation

In this paper, we shall assume that the allowable temperature of the motor should be so specified that a motor in continuous service at rated load and maximum ambient temperature will have adequate life, with present insulating materials. Available information on temperature life of cellulose insulation was summarized by V. M. Montsinger[3] some years ago, in connection with studies of transformer insulation under oil, that have since formed the basis for the recently published American standards for transformers.

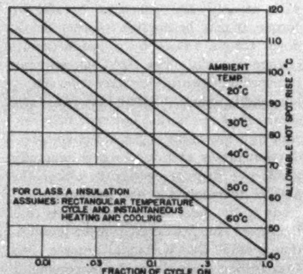

Figure 2. Permissible increase in hot-spot temperature rise to secure same life expectancy in intermittent service as in continuous service at 102 degrees centigrade

Although these test data indicate a materially longer life of cellulose materials in air than under oil, some information from longer test periods[4] that is available suggests that over long periods of time the life in air and oil will not be materially different. For the present paper, therefore, the same temperature life curve as generally used for transformers will be employed, as shown in figure 1*B*. This indicates that the life of class *A* insulation will be halved for each 8 degrees centigrade increase of temperature. The curve gives a materially shorter life than the *A* curve, which represents the best information available in 1925, when the present AIEE standards of temperature limits were established. If steadily maintained at a temperature of 105 degrees centigrade, the curves indicate the insulation will theoretically reach the end of its useful life in seven years, or will then be subject to immediate failure under any mechanical or electrical shock.

General-purpose motors with a 40 degrees centigrade ambient and a 40 degrees centigrade rise at full load by thermometer will normally have an actual hot-spot temperature of not over 102 degrees centigrade, when operated continuously at 115 per cent of the rating, in accordance with the standard service factor; indicating about ten years' useful life. Since, in practice, the average ambient temperature in the United States is generally below 30 degrees centigrade, the typical motor operating continuously at 115 per cent of its rating will have an average hot-spot temperature of about 90 degrees centigrade, giving an indicated useful life of roughly 25 years. When motors are operated below their ratings, considerably longer insulation life may be expected. This accords well with experience and indicates that our present standard basis of rating is satisfactory, for fully continuous service in normal ambients.

If, however, a motor is employed on intermittent service, with short-time overload periods repeated at intervals of hours, days, or longer, and periods of complete idleness between, the actual life of the motor at the same loading may be considerably longer, temperature alone considered. Therefore, reasonably higher temperatures may be permitted on intermittent service.

We shall assume that whether a motor is operated at a given high temperature one month in every ten, or on any other cycle with the motor idle nine-tenths of the time, the insulation will deteriorate at the same average rate, giving the same total years of life. We shall assume also that the temperature life curve is a constant exponential curve as indicated by figure 1, and that temperature variations are not accompanied by other deteriorating conditions, such as variable dirt or moisture exposure. In practice, these conditions are not strictly true, but over the moderate range of temperature variation considered, the assumptions appear justified.

Consideration of figure 1 readily enables the actual temperature to be determined that will give the same life in years on any intermittent service. Figure 2 shows, for example, that, if the motor is idle nine-tenths of the time, it may be permitted to have a hot-spot temperature rise 26.5 degrees centigrade greater than normal, and still have the same life in years as the standard motor operated continuously at 115 per cent of rating. To find the permissible overload on the motor, corresponding to this additional temperature rise, reference must be made to the characteristics of a typical general-purpose motor.

Normal Induction-Motor Characteristics

If a motor is designed solely for continuous operation at rated load, it will normally have its maximum efficiency point near 75 per cent load, giving the most favorable over-all performance. If the maximum efficiency point always occurs at the same fraction of full load, the no-load losses will bear about the same proportion to the full-load losses for all motors in the line. Keeping the same percentage of breakdown torque for all sizes of motor, in accordance with the present standards, and keeping the same balance of losses as indicated, the performance curves and equivalent circuit of the typical general-purpose induction motor can immediately be determined. The chief differences between motors of different speeds and horsepower will be that the percentages of no-load current, full-load power factor, and total losses will vary. Figure 3 shows the performance curves and the equivalent circuit for such a typical polyphase motor, which closely represents an average 4-pole 25-

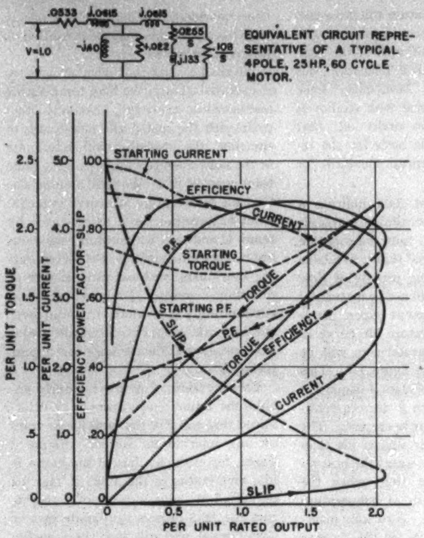

Figure 3. Operating characteristics of a typical four-pole general-purpose polyphase motor

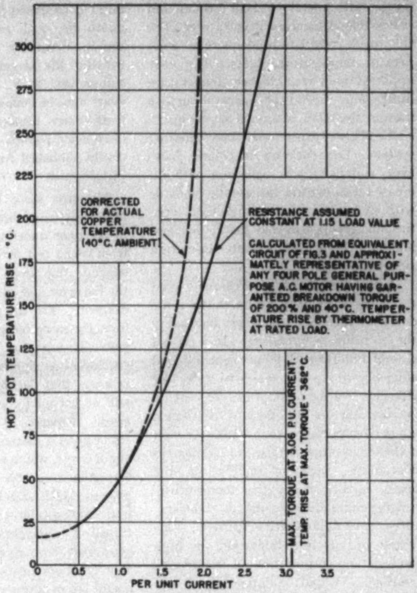

Figure 4. Hot-spot temperature rise versus current for typical four-pole general-purpose motor

horsepower 220-volt design. The circuit was chosen to give a maximum torque at normal voltage of 220 per cent (10 per cent greater than the 200 per cent value required by the standards), providing the usual margin necessary to be sure of meeting guarantees.

Many polyphase induction motors now have double squirrel-cage or deep-bar rotors, to get increased starting torque without impairing efficiency in normal operation, and the circuit constants shown in figure 3 are representative of this type. Light and heavy broken-line curves indicate the starting characteristics of the double and single squirrel-cage designs, respectively. The curves indicate that a starting (locked rotor) current of at least 500 per cent of full load must be allowed, if 200 per cent breakdown torque is required. The actual value of starting current will vary for different sizes and speeds of motor, depending on their efficiency, power factor, and starting torque values, but a figure of 14 amperes per rated horsepower for 220-volt 3-phase 60-cycle motors larger than 15 horsepower represents the lowest that can be expected. Any increase in breakdown torque will require a proportional increase in starting current, 250 per cent breakdown torque requiring 17.5 and 300 per cent requiring 21 amperes per rated horsepower on the same basis. The smaller motors also will have greater starting currents for a given breakdown torque than large motors, due to their lower power factors and torque efficiencies, and higher starting-torque requirements.

Many temperature tests on different motors indicate that for low-voltage designs the temperature rise of the winding is closely proportional to the total losses in the motor, regardless of where they occur.[2] For overload conditions especially, the hot-spot temperature rise may be taken as directly proportional to the total losses without important error.

If, therefore, we assume that all motors are provided with ventilation just adequate to hold the temperature down to the rated value of 40 degrees centigrade rise by thermometer at full load, a single temperature-rise-versus-current curve may be drawn, that will be closely representative of a wide range of motor sizes and speeds. Such a curve is shown in figure 4, the temperature rise being in direct proportion to the losses given by the equivalent circuit of figure 3. Degrees are plotted against percentage of full-load current, rather than against horsepower or torque, to allow for the variations in the no-load current and no-load temperature rise for motors of different sizes and speeds. The dotted curve indicates the additional temperature rise due to increase of copper resistance with temperature,* showing the cumulative heating effect of prolonged overloads.

Although figure 4 is drawn for a motor with exactly 220 per cent breakdown torque, it may be applied to other breakdown torque values by a proportionate change in scale. The curve shown gives hot-spot temperatures, rather than simply observable values. Many heat runs have shown that the actual hot-spot temperature in a low-voltage general-purpose induction motor at rated load is generally about five degrees centigrade above the highest thermometer rise, and rarely exceeds it by more than ten degrees centigrade. Thus, the conservative assumption is made that a standard motor with 40 degrees centigrade rise by thermometer will have 50 degrees centigrade rise at the hottest spot on the insulation.

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* Neglecting the increase in watts dissipated per degree of rise at elevated temperatures.

At the service factor rating of 115 per cent load, the motor losses are 123 per cent of full-load losses (figure 3), making the temperature rise 49 degrees by thermometer or 62 degrees at the hottest spot. Thus, the service factor rating corresponds very closely to the 50 degrees centigrade rise permitted by the standards for special-purpose motors, of which the load conditions are definitely known in advance. The 13 degrees centigrade rise at the hot spot above the test value of temperature rise assumed for this typical low-voltage induction motor is a little less than the 15-degree allowance in the standards.

Permissible Intermittent Overloads

Comparison of figures 2 and 4 enables us to determine the permissible overload current on a standard motor for any degree of intermittency of loading. The full-line curves of overload current versus fractional operating time for different ambient temperatures, shown in figure 5, all correspond to the same ten-year life expectancy. The lower of the two 40-degree-centigrade ambient curves indicates the effect of temperature creep, corresponding to the dotted curve of figure 4.

Inspection of this figure shows that short-time overloads well in excess of 135 per cent of rated current are permissible in usual 20 degrees centigrade to 30 degrees centigrade ambients, without exceeding economic heating limits. As previously indicated, however, this 135 per cent value is roughly the highest load the present standard motor can carry without risk of breakdown under ten per cent low voltage and other varying conditions of service. Hence, to utilize fully the economic life of motors in intermittent service, higher breakdown torques than 200 per cent are required.

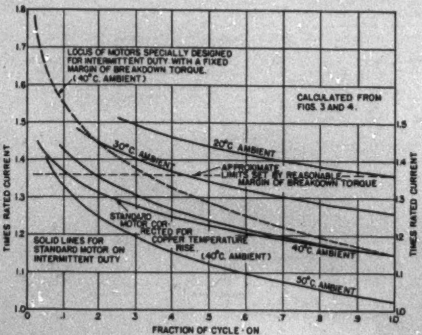

Figure 5. Permissible current loading of four-pole general-purpose motors in intermittent service, for the same insulation life expectancy

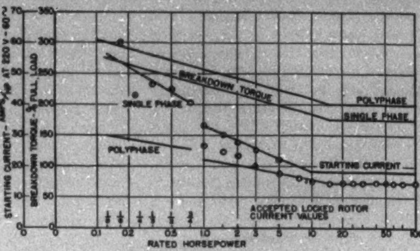

Figure 6. Recommended breakdown torques and starting currents (locked rotor) for four-pole general-purpose a-c motors of different horsepower ratings

It should be noted that figure 5 is derived on the basis of no lag of temperature behind the applied load, or instantaneous heating and cooling. For duty cycles involving many hours of continuous running interspersed with long idle periods, this is satisfactory, but for running periods of two hours or less, it gives very conservative values. At the limit, with very short cycles, the average loss can be assumed constant over the entire period, and the aging will correspond to the temperature rise due to $1/n$th of the operating plus starting losses, if the idle time is $(n - 1)$ times the operating time.

Knowing the heating and cooling curves, or thermal time constants, of any motor, it is readily possible to determine the temperature-time curve, and hence the aging effect, of any duty cycle. In appendix II of this paper, such calculations have been carried out, and the curves of figure 11 have been derived, enabling the permissible temperature rise above normal to be determined, and hence, from figure 3, the permissible overload current to be found, for any actual cycle, and for motors of different sizes.

Everything considered, therefore, it seems proper to use the ideal zero time constant curves of figures 2 and 5 for operating recommendations, recognizing that the extra heating due to acceleration losses in starting, and temperature creep, on higher overloads, offset a large part of, but not all of, the beneficial effects of time lag in temperature rise on short cycles.

Characteristics of Motors Designed for Intermittent Service

If a continuous-rated motor is applied on an intermittent load, therefore, and the insulation is to be used to the full extent of its economic life, the motor can evidently be operated at an output greater than the continuous rating, and it should be designed with a higher breakdown and starting torque ability than required for continuous service. Knowing the fractional operating time and the desired motor life, the permissible temperature rise is found from figure 2. For greatest economy, the motor should then be designed to obtain the maximum possible horsepower output at this temperature rise. The designer does this by first varying the number of turns in the motor winding, and finding for each case the current loading with the given supply voltage that gives a total power loss corresponding to the permitted temperature rise. The maximum power output for these conditions is obtained when the maximum point on the efficiency characteristic coincides with the loading that gives the permitted total losses.

In practice, the maximum efficiency point should occur a little below the operating point to secure better light-load efficiency and power factor and lower starting current. We shall assume, therefore, that the winding turns will finally be chosen to make the maximum permissible load for the desired intermittency always occur at the 115 per cent load point on the standard-motor characteristic curves, giving the same torque characteristics as in continuous operation at the original 115 per cent service-factor rating.

If the equivalent circuit constants of the motor do not change from increased magnetic saturation or other cause, and all fixed losses increase as the square of the volts per turn,* all the characteristic curves of the motor will retain the same shapes, and figure 3 will still represent the motors redesigned for higher torques.

The per unit output of the redesigned motor is obtained by multiplying the output scale for the original motor, figure 3, by $1/a^2$, where a is the ratio of the new to the old number of winding turns, the new full load characteristics being the same as at a load a^2 on the original motor. The rated load in horsepower is assumed to remain the same in all cases.

By this process, the efficiency and, therefore, the total losses and temperature rise at rated load will be slightly changed from their original values. A limit is set on this stepping up of the torque with reduced winding turns by the excessive increase of no-load current when the magnetic flux density is increased beyond the saturation point. For low-speed motors particularly, there is a definite value of volts per turn of winding beyond which a further increase will reduce instead of increasing the breakdown torque, for fixed magnetic dimensions. A further limitation is set by the rapid increase in full-load current and temperature as the maximum efficiency point is brought beyond the point of rated load.

For example, taking 40 degrees centigrade ambient temperature and an ideal load cycle with only 0.06 of the time operating, the rest idle, the permissible motor temperature rise at the hottest spot is 94 degrees centigrade, as compared to 62 degrees centigrade for the standard motor operating at 1.15 times rated load, from figure 2. Hence, the allowable total losses can be increased to 1.52 times, and the volts per turn should be raised by a factor of 1.23. At this increased magnetic density (neglecting saturation), the motor will have an output at 94 degrees centigrade hot-spot temperature rise of 1.52 × 1.15 or 1.75 times the original horsepower rating, and the breakdown torque will be 1.52 times larger, or will still be 174 per cent of the output (190 per cent including the 10 per cent margin of the average motor of figure 3), the same as for the original motor operating at the 115 per cent service-factor rating.

Figure 7 compares the calculated temperature rise-current curves for the motor of figure 3 before and after rewinding with 1/1.23, or 0.81 as many turns, with the same supply voltage, the same ventilation, and the same continuous horsepower rating. Rated load efficiency and power factor on the new characteristic curves are the same as at $(0.81)^2$, or 0.66 load for the original winding, or 89.3 per cent and 79 per cent, respectively, from figure 3. These compare with 88.9 per cent efficiency and 86.5 per cent power factor for rated load on the original motor, giving a new rated load current 108 per cent of the original value, in amperes.

The total losses at rated load being 10.7 per cent instead of 11.1 per cent of input, the temperature rise at rated load will be 38.5 degrees centigrade by thermometer, or 48 degrees centigrade at the hot spot, a little lower than before. The efficiency at 115 per cent load on the rewound motor is 89.8 per cent as compared to 88.2 per cent originally, so the temperature at this load is $1.15 \times 10.2/10.7 \times 89.3/89.8 \times 48 = 52.5$ instead of 62 degrees centigrade. From figure 7, 62 degrees centigrade rise is reached at 122 per cent current on the new motor, which, from figure 3, corresponds to 132 per cent load. Hence, the redesigned motor has a service factor of 1.32 in place of 1.15. If, however, the 40 degrees centigrade rise at rated load had been adhered to, 62 degrees centigrade rise would be reached at 119 per cent current, corresponding to 125 per cent load.

The overload current-temperature curves of figures 5 and 7 apply fairly closely to a wide range of motor sizes, but differences in power factor and efficiency curves will cause considerable differences in the corresponding output curves for different sizes. In general, the lower full-load power factor and higher no-load losses of the smaller motors will cause their overload currents to increase less rapidly than their outputs, so giving them higher service-factor ratings than large motors.

Recommended Characteristics

Since small motors are usually applied on intermittent loads, it is obviously desirable to design them for a moderate degree of intermittency rather than for continuous service. Experience has shown that motors with minimum breakdown torque values varying from 200 per cent for 15 horsepower and larger, up to 300 per cent for 1/8 horsepower, are well suited for average starting and load requirements, and it is proposed that these values be adopted as standard for polyphase motors, in place of the flat value of 200 per cent now standard. On small motors with high starting torque, which have more than 20 per cent slip at maximum torque, the torque at 80 per cent speed is taken as the breakdown value. This 80 per cent speed point is chosen as the measure of breakdown torque in recognition of the fact that higher torques which may be available at lower speeds are only useful in starting, rather than in overload operation. Integral-horsepower normal-torque motors usually have less than 20 per cent slip at breakdown. Figure 6 shows the proposed variation of torque with horsepower rating.

In comparing single and polyphase motors for the same service, it is found that the single-phase motor (of either repulsion or capacitor type) has normally greater starting torque, greater service factor, and lower slip for the same value

Table I. Characteristics of Proposed Standard General-Purpose A-C Motors With 40 Degrees Centigrade Rise by Thermometer at Rated Load

	Breakdown Torque* in Per Cent of Full-Load Torque				Locked Rotor Current Amperes at 220 Volts, 60 Cycles				
	Single Phase		Polyphase		Single Phase		Polyphase		
Horsepower	Present	Proposed	Present	Proposed	Present	Proposed	Present	Proposed	Service Factor in 40 Degrees Centigrade Ambient
1/8	175	275	200	300	10	7		3.75	1.50
1/6	175	270	200	295	10	9		5	1.45
1/4	175	260	200	285	10.75	12.5		7	1.40
1/3	175	255	200	280	15.5	16		9	1.35
1/2	175	245	200	270	22.5	22		13	1.30
3/4	175	235	200	260	30.5	30.5		19	1.30
1	175	230	200	255	33	33	26.6	22	1.25
1 1/2	175	225	200	250	45	45	36.6	31.5	1.25
2	175	215	200	240	55	57	46.6	40	1.20
3	175	210	200	235	75	77	60	57	1.20
5	175	200	200	225	110	110	89	90	1.15
7 1/2	175	190	200	215		145	120	120	1.15
10	175	185	200	210		180	150	155	1.15
15 and up	175	175	200	200		18× horsepower		14.5× horsepower	1.15

* When the maximum torque occurs at more than 20 per cent slip, the torque at 80 per cent speed is taken as the breakdown value.

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* Actually, the friction and windage will not increase, unless larger bearings are required for the higher torques, but the core and exciting current losses will increase somewhat faster than the volts per turn squared. The two effects will counteract each other so completely that their combined effect may be neglected for our purposes.

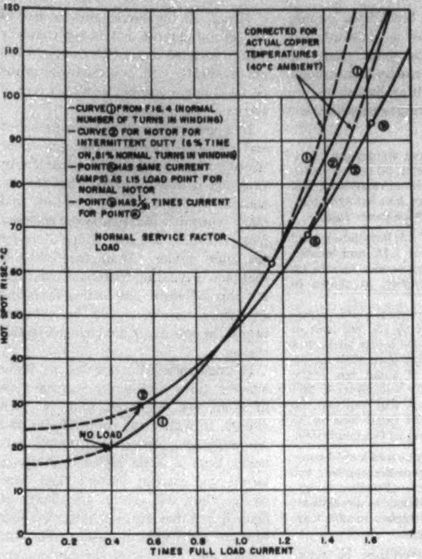


Figure 7. Hot-spot temperature rises of standard and intermittent-duty types of motors

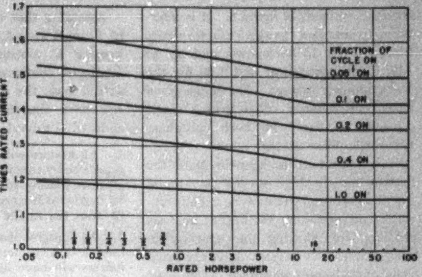


Figure 8. Permissible current loading, in intermittent service, with 40 degrees centigrade ambient, of four-pole general-purpose a-c motors, having breakdown torques in accordance with figure 6

of breakdown torque. A repulsion-start motor, for example, usually has a starting torque of over 400 per cent, and a pull-up torque considerably less than the breakdown value, while a small polyphase motor has a starting torque only a little greater than the 80 per cent speed value, and a pull-up torque fully equal to the breakdown value. Capacitor-start single-phase motors have intermediate characteristics. Experience has shown that the characteristics of the two types are best matched when the breakdown torque of the single-phase motor is equal to about 90 per cent of the polyphase value. Present standards, recognizing this, require only 175 per cent breakdown torque for single-phase motors. It is proposed, therefore, that single-phase-motor breakdown-torque values be standardized 25 points lower than for polyphase, varying from 275 per cent for $^1/_8$ horsepower to 175 per cent for 15 horsepower, figure 6.

In figure 6 also are shown proposed limiting starting-current (locked rotor) values for single and polyphase motors, chosen to enable the specified breakdown torque values to be obtained with reasonable margin. Circled points show present accepted, but inconsistent, values of starting current. The amperes per horsepower increase in the smaller sizes in approximately direct proportion to the increase of breakdown torque, and in inverse ratio to the apparent efficiency at rated load. The break in the current curves between fractional and integral horsepower ratings is due to the higher starting-torque requirements in the fractional sizes, to provide for relatively higher friction and greater variations in service conditions. The general use of repulsion motors, which inherently have a higher ratio of starting to pull-up torque, and lower starting currents, than capacitor-start motors, has made it customary to specify much higher starting torques for fractional-horsepower motors than required by the load in most cases. It may be, therefore, that future trends will reduce these starting torque requirements below the present levels of about 400 per cent of rated value, and in this case the fractional-horsepower starting-current levels could be reduced also.

The single-phase locked-rotor currents given are proposed to cover capacitor-start motors, and are appreciably higher than necessary for repulsion-start motors. The proportional relation between starting current and breakdown torque also does not hold as accurately for repulsion-start motors as for pure induction motors, due to possible changes in brush position on the repulsion types.

These starting-current values are especially important to control engineers, underwriters, and power-supply authorities, because they determine the necessary fusing, overload control, wiring, and voltage-regulation requirements. From every viewpoint of safety and convenience, it is desirable to have a definite, one-to-one correspondence between locked-rotor current and name-plate horsepower for a given supply voltage. If the values proposed in figure 6 are adhered to, they will permit economic motor use with uniform safety and control practice, and give reasonable assurance that a motor of the next lower or higher rating capacity is not masquerading under a false name plate.

The dotted line in figure 5 shows the permissible overload current capacities without reduction of breakdown-torque margin, obtained by using a different winding for each load cycle, with 81 per cent turns for 0.06 operating time, 84 per cent for 0.10 time, etc. By designing each size of the line of motors to have the breakdown-torque value indicated in figure 6, and using these modified motors for all duty cycles, reasonably increased overload capacity can be obtained with normal efficiency, at the expense of reduced full-load power factor and increased starting current, but with starting torque higher in proportion to the increased breakdown torque.

Figure 8 shows the permissible current overloads in intermittent service, for different sizes of motor, each designed to meet the breakdown torque requirements of figure 6, and to have 40 degrees centigrade rise by thermometer at rated load. Figure 9 shows typical current-output curves for these same motors. From these two sets of curves, figure 10 is derived, giving the permissible torque overloads, or power outputs, corresponding to the current overloads of figure 8. It is interesting to note that the service factor varies from 1.15 for 15 horsepower and up, to 1.25 for 1 horsepower and 1.50 for the 1/8-horsepower ratings. The values given have been rounded off to give a single conservative figure for both polyphase and single-phase motors.

It is important to keep in mind the reasons for the high service factors on fractional-horsepower motors. The service factor represents the output increase obtained by allowing the temperature rise to increase 10 degrees above the rated value of 40 degrees centigrade. This gives 1.15 to 1.19 times rated current over the range of 1/8 to 100 horsepower 4- and 6-pole motors, figure 8. Fractional-horsepower motors, having inherently lower power factor and efficiency, and being called on to deliver higher starting and breakdown torques than large motors, inevitably have no-load current values only slightly lower than at full load. Hence, their current-output curves, figure 9, are much flatter than for large motors. Small motors have also inherently large mechanical factors of safety, as their shaft and bearing sizes are fixed by stiffness rather than torque requirements. Finally their large surface areas per unit of power loss, and their thin insulation give them relatively much better heat dissipating characteristics than large motors. Electrical, mechanical, and thermal characteristics all combine, therefore, to make lower service factors appropriate for large than for small motors.

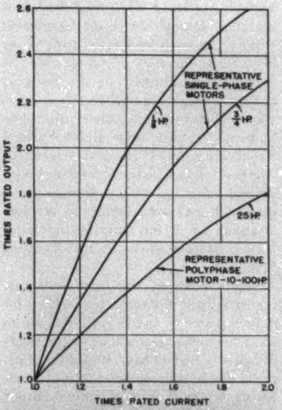


Figure 9. Typical current versus horsepower curves for four-pole general-purpose motors, having breakdown torques in accordance with figure 6

Assuming that the breakdown torques of figure 6 are adopted as standard, and keeping a continuous single rating of 40 degrees centigrade rise by thermometer at full load, the resulting line of motors will have the following characteristics, as listed in table I:

1. All motors will have 40 degrees centigrade rise by thermometer, 50 degrees centigrade or less hot-spot rise at rated load, and 62 degrees centigrade or less hot-spot temperature rise at the service-factor load.

2. All motors above 15 horsepower will have a service factor of 1.15, and smaller motors will have higher service factors up to 1.50 for 1/8 horsepower, as shown in figure 10.

Thus, continuous operation could be permitted at 115 per cent to 150 per cent of rated load for the various motor sizes, in a 40 degrees centigrade ambient, with an expected insulation life of about ten years. Lower ambients usually experienced, and noncontinuous operation, will lengthen the normal insulation life at rated load to 25 years or more under usual service conditions.

3. The breakdown torque and locked-rotor current values for different horsepowers will be as shown in figure 6. Starting torque values will be proportional to breakdown torques, but relatively higher in the fractional-horsepower ratings.

4. The permissible overloads, or intermittent service factors, in percentages of rated current and rated torque are shown in figures 8 and 10 for several ratios of operating to elapsed time. Limits set by a breakdown torque margin of 150 per cent are indicated.

These values are based on the conservative assumption of zero time lag of temperature. More exact values, taking into account the actual heating and cooling time constants, can be determined by the methods outlined in appendix II.

Rating of Protected Motors

The general use of protected motors with different degrees of enclosure suggests that their permissible overloads should also be determined. Present standards allow 50 degrees centigrade rise by thermometer, for splashproof motors, and 55 degrees centigrade for totally enclosed and fan-cooled designs, with a service factor of 1 instead of 1.15. The extra 5 degrees centigrade for enclosed motors is generally understood to be allowed because of the smaller difference between the hot test spot and the measured temperature than in open motors. The standards imply, therefore, that all fully protected motors will have a hot-spot temperature rise at rated load of 65 degrees centigrade, or practically the same as that of the standard general-purpose motors of the open type at 115 per cent load.

Hence, all the curves already derived for general-purpose motors apply equally well to enclosed motors if the actual loads are divided by 1.15, or if the rating of the enclosed motor is taken as 87 per cent of the name-plate value.

It is very desirable to build protected and open-type motors in the same frame size and with interchangeable characteristics, and it may be urged that the exclusion of dirt, excessive moisture, and other protection to insulation in enclosed motors justifies a higher temperature for the same service. It appears probable that new insulating materials may permit this in future, but further operating experience records should be obtained before the standards are changed in this respect.

In usual enclosed motor designs, fewer winding turns and larger magnetic dimensions are employed than in open motors, to reduce the copper losses and temperatures. Hence, such motors normally have a little higher breakdown torque and starting current than open motors, but still within the limits of figure 6, and they are even better adapted to carry short-time overloads. For this reason, and in view of the presumably longer insulation life at a given temperature because of moisture and dust exclusion, it is suggested that service factors be applied to enclosed as well as open motors. While no specific recommendations are now offered, it is clear that the inherent ability of an enclosed motor to deliver short-time overloads will be utilized in the long run, and standards or operating recommendations should be prepared to facilitate this.

The cooling of motors for hermetically sealed refrigerators, enclosed gas pumps, and other built-in applications, is entirely in the control of the user. Temperature rating standards do not apply to such motors, therefore, and the operating temperature may be anything that the user's experience justifies. By adhering to the proposed breakdown-torque and starting-current rules, and normal efficiency values, however, assurance is given that the motor will have the torque ability represented by the name-plate horsepower. With improvements in insulating materials, such torque and starting-current rating methods may be expected to supersede the temperature system to an increasing extent.

Conclusions

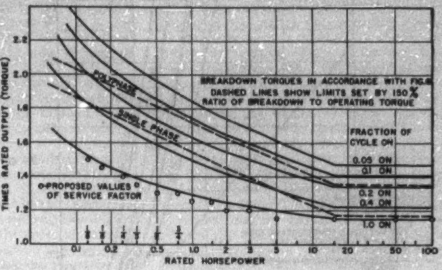
The whole object of this discussion is to make the standard motor fit as many uses as possible. It should have a simple, generally understood name plate that guarantees its satisfactory performance in continuous duty, and there should be supplementary information describing its proper application in all sorts of different duties.

The recommendations for breakdown torque and starting current values for standard general-purpose single-phase and polyphase induction motors given in table I, and the associated permissible overloads in intermittent service, figure 10, are believed to provide a logical and comprehensive system of rating. The information on permissible overloads is proposed in the form of operating recommendations, rather than as hard and fast requirements of standards. This system provides for motors designed to fit the maximum variety of applications, and it permits the user to apply them intelligently up to the limits of their economic life. It ties together the present differing practices on fractional- and integral-horsepower motors and permits merging the important requirements for automatically controlled refrigeration and air-conditioning motors with the standard general-purpose designs.

The plan does not disturb the basic principles of motor rating so well established some 15 years ago, but carries them forward along proved lines. It divorces the operating recommendations from the rating standards, and lays a foundation for the more flexible use of motors in new fields and their more ready adaptation to future developments.

The numerical values given in the paper have been presented as illustrative of the principles involved, rather than as final values for standards. Necessarily, the characteristics of motors made by different manufacturers will vary from those used for illustrations in the paper, and an industry-wide review of experience must be made before any action on standards can be taken. It is also desirable to extend the analysis to cover motors of other speeds besides the four-pole designs which have alone been considered in the paper. And, finally, the temperature-life curve of figure 1, forming the basis of the values proposed, should be reviewed on the basis of new information available, before final values for standards are accepted.

Figure 10. Permissible overloads of typical four-pole general-purpose a-c motors in intermittent service, in 40 degrees centigrade ambient



Appendix I. European Standards of Motor Rating

Comparison of French,[6] British,[7] and German[8] with American standards reveals considerable differences in both torque and temperature limits for general-purpose a-c motors. The quantity of work-performing ability measured by one continuous horsepower of motor rating cannot be accepted as an international standard, therefore, but must be evaluated by reference to the customs in the country of origin.

All four countries give general-purpose industrial motors a single continuous rating, limited by temperature rise. The first three countries allow a maximum ambient temperature of 40 degrees centigrade, but the Germans allow 35 degrees centigrade ambient. American and British rules for open-type motors specify 40 degrees centigrade rise by thermometer; while French rules specify 55 degrees centigrade by resistance, or 50 degrees centigrade by thermometer if resistance measurements are not practicable, and German rules specify 60 degrees centigrade by resistance or thermometer, whichever gives the higher reading. In all cases, temperatures are measured before and after shutdown, the highest reading being taken and no tolerances from the guaranteed values being allowed.

For these open-type low-voltage motors, it is fair to say that the hot-spot temperature rise is not more than 25 per cent above the thermometer reading, or 12 per cent above resistance measurements. On this basis, the hot-spot temperature rises of the four motors at rated load will be 50 degrees, 50 degrees, 62 degrees, and 67 degrees centigrade, respectively. In continuous operation at full load in a 40-degree-centigrade ambient, therefore, the American and British motors will have a life expectancy of 25 years, the French motor 10 years, and the German motor 6 years, from figure 1, assuming the same design margins over guarantees. American special-purpose motors, or general-purpose motors operated at their 115 per cent service-factor rating, however, have the same temperature rise, and the ten-year life expectancy, of the French motor. In a 35-degree-centigrade ambient, these periods would all be increased about 50 per cent.

For totally enclosed motors, the American rules allow 55 degrees centigrade rise by thermometer without any service factor, and the British allow 50 degrees centigrade, while the French and Germans keep the same 55- and 60-degree rises by resistance allowed for open motors. In a 40-degree-centigrade ambient with continuous operation at rated load, these correspond to life expectancies of five, ten, ten, and six years, respectively, the American having the shortest life in this case.

The five-degree spread between open and closed motors under the American rules may be justified technically on the grounds of a lower hot-spot differential, slower deterioration of insulation from other causes than temperature in enclosed motors, and the frequent use of these motors in outdoor installations at lower ambients. It is economically sound, because it is relatively a great deal more expensive to lower the temperature of a fully enclosed motor than of an open motor, and because it facilitates interchangeable mounting dimensions for open and closed motors.

The inherent, or short time, capacity of a motor is determined by its maximum torque, which is quite independent of the temperature rise. For a true comparison of an induction motor's ability to handle all sorts of loads, therefore, it is necessary to know its breakdown torque.

The American and British rules both specify a minimum of 200 per cent breakdown torque, or 100 per cent overload torque, for standard general-purpose industrial motors. The French rules require only 150 per cent, and the German rules 160 per cent breakdown torque, on the basis of continuous ratings. The French rules recognize seven, the British two, and the German four kinds of intermittent or short-time ratings, for which 200 per cent breakdown torque is required by all except the French rules. None of the rules prescribe any starting-current limits for industrial motors.

The breakdown-torque values, which are the best measure of magnetic dimensions and mechanical ability, indicate that the standard American, British, French, and German continuous-rated motors have relative sizes of 100, 100, 75, and 80, respectively.

The American, British, and French all have special rules for fractional-horsepower motors, which cover motors smaller than one horsepower at 1,500 rpm, one horsepower at 1,000 rpm, and 600 watts, respectively. American rules prescribe the same breakdown torque and temperature limits as for larger motors, except that 175 per cent breakdown torque is required for single-phase motors. British rules require only 125 per cent breakdown torque for polyphase and 100 per cent for single-phase motors. They also extend the 50-degree temperature-rise limit to include drip-proof as well as totally enclosed motors, keeping 40 degrees centigrade for open motors. French rules provide a special "domestic" service rating for fractional motors, which is defined as equivalent to continuous service

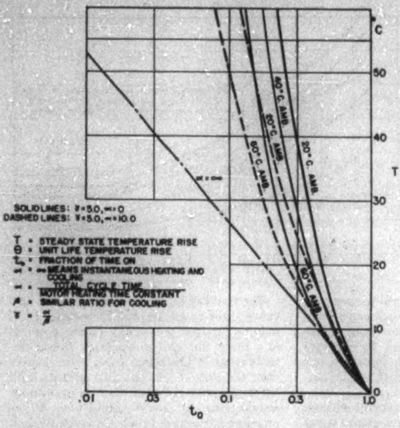


Figure 11. Permissible excess temperature rise in intermittent service, for fixed total insulation life (showing effect of cycle time compared to heating time constant)

at two-thirds of the name-plate horsepower, implying a breakdown torque 225 per cent of the continuous capacity. The French also provide a higher temperature rise for all fractional-horsepower motors, 65 degrees centigrade for continuous-rated motors, and 85 degrees centigrade for domestic motors for temperate climates, the latter on the basis of a maximum ambient temperature of only 20 degrees centigrade.

It is thus evident that continental standards call for materially smaller motors for a given continuous rating than American and British standards. R. Langlois-Berthelot has recently written a comprehensive article[3] on the temperature life of electrical machines, including an historical summary of the subject. His views on the normal life of insulation are summarized in the following statements:

> We shall take . . . the (hot spot) temperature θ (for rating purposes) as that permissible in a machine to assure in practice a normal life of 15 to 20 years, and we shall assume as a fact of experience that this normal life is equivalent to continuous operation for two years, or 17,000 hours, at the maximum temperature; so that . . . θ will correspond on the insulation-life curve to a life of two years. These are the average normal conditions, which take account of the usual variations of ambient temperature and of load. This interpretation of the temperature θ conforms to experience with machines and to the opinion of known experts who have been willing to express their views.

This point of view that the standards ought to set temperature limits to give long life under average conditions of reduced load and reduced ambient appears fundamental in continental standards, in strong contrast to the American viewpoint that the rated temperature should be low enough to assure a long life in rated load operation at the maximum ambient temperature.

Appendix II. Determination of Permissible Insulation Temperatures

In accordance with the experimental evidence that insulation deteriorates at a rate that is greater the higher the temperature, it is evident that, for the same useful life, a motor which is to be in operation only a part of the total time may be allowed to run hotter than a motor which is to be used continuously. In the body of the paper permissible overload durations were established, to secure the same total insulation life as at continuous full load, under the assumption of instantaneous heating and cooling. In practice, the motor takes an appreciable time to heat up, thus lowering the temperature in the initial part of the overload period, so that the actual temperature aging for the duty cycle is less than that assumed. In this appendix, the effect of this time lag of the temperature on the permissible duration of overloads will be considered.

It is assumed that the mode of variation of class *A* insulation life with temperature is that shown in figure 1*B*, which represents generally accepted data. According to this curve, the effective insulation life is given by:

$$L = L_0\epsilon^{-a(T-\theta)} \qquad (1)$$

where

L_0 = years of useful life at some reference temperature
= seven years at 105 degrees centigrade continuously
$T - \theta$ = temperature of insulation above reference temperature
a = a constant, here equal to 0.0866 (degrees centigrade)$^{-1}$

Then the *rate of deterioration* of insulation must be of a reciprocal form.

$$R = A\epsilon^{+a(T-\theta)} \text{ (where } A \text{ is a constant)} \qquad (2)$$

For the economic use of the temperature-life characteristic of the insulation, it is necessary to choose a temperature which, if maintained continuously, will allow a reasonable insulation life, henceforth to be called *unit life,* and then so to order the temperature duty cycles as to arrive at this unit life, no more, no less. This unit-life temperature is taken as 102 degrees centigrade (giving a unit life of about nine years), since this is closely the temperature attained by a standard general-purpose polyphase open induction motor having 40 degrees centigrade rise by thermometer at full load (50 degrees centigrade rise at hot spot), when running at the 115 per cent service factor load in a 40-degree-centigrade ambient temperature. The difference between this unit-life temperature (102 degrees centigrade) and the ambient temperature is called the *unit life temperature rise* and denoted by θ.

For the purposes of the present discussion the only type of load duty cycle which will be considered is the on-off type, such as experienced by a motor driving a refrigeration or air conditioning load. This assumes a load essentially constant to be applied for some fraction of the total cycle time, following which the motor is shut down for the remainder of the cycle.

To simplify the analysis only two types of temperature variation with load will be discussed: first, where the temperature instantly attains its steady-state value corresponding to the load applied, and, second, where the temperature varies exponentially, growing and decaying according to time constants which are characteristic of the particular motor.

Consider the case in which the temperature of the insulation exactly follows the steady-state value corresponding to the load applied. The temperature duty cycle has the same appearance as the load duty cycle: a simple rectangle having a height *T* (temperature rise above ambient) during the fraction of the cycle that the motor is on, and zero height (ambient temperature), while the motor is shut down. Then, for this temperature cycle to give unit life it is necessary that the total deterioration of the insulation during on and off periods be the same as if the insulation had remained at the unit-life temperature rise θ. Then the following equation must be satisfied:

$$At_0\epsilon^{+a(T-\theta)} + At_f\epsilon^{+a(-\theta)} = A \times 1.0 \qquad (3)$$

where

t_0 = fraction of cycle time motor is on (constant load)
t_f = fraction of cycle time motor is off
$t_0 + t_f = 1.0$

Equation 3 has the effect of averaging the actual temperature variation into the steady temperature θ. With the usual values of θ this simplifies to:

$$t_0\epsilon^{+aT} = \epsilon^{+a\theta} \qquad (4)$$

which gives the *straight* lines shown on figures 2, 11, and 12, when plotting *T* or $(T-\theta)$ against log t_0. As indicated in the body of this paper, it is a simple matter to translate such curves into the equivalent load or current curves if the steady-state temperature versus load or current characteristics of the particular motor are known.

As it has been assumed that the rate of deterioration of insulation depends only upon temperature, the results calculated on the basis of the above equation are correct for cycles of any time length, whether one hour, or several years. Thus, it is evident that standard motors possess considerable inherent overload ability (may exceed their normal temperature rise), judging from thermal considerations alone.

Consider the case in which the temperature varies exponentially. (It is assumed that the variation of temperature while heating under constant load can be represented by *one* exponential curve, characteristic of the particular motor, and similarly for cooling.) For economic utilization of the thermal properties of the insulation, since the temperature now varies with time, a generalization of equation 3 may be written as

$$\int_0^1 A\epsilon^{+a(\phi-\theta)}\,dt = A \times 1.0 \text{ (where } \phi \text{ is instantaneous temperature)} \qquad (5)$$

or

$$\int_0^1 \epsilon^{a\phi}\,dt = \epsilon^{a\theta}$$

whence (3) is obtained if $\phi = T =$ a constant for the fraction t_0 of the total cycle time.

The actual length of cycle time must now enter the calculations. Note that with a finite time-constant (for heating or cooling) as the cycle time approaches infinity the conditions of the previous case (instantaneous heating and cooling) are approached. In order to include the cycle time, it will be convenient to define two dimensionless quantities: α equal to the ratio of the total cycle time to the motor time-constant for heating, and β equal to a similar ratio for cooling. The ratio of α to β will be denoted by γ. The steady-state temperature rise corresponding to the load applied toward which the actual temperature climbs while the motor is on will still be denoted by T. Then, since the temperatures at the beginning and the end of the cycle must be the same, it can easily be shown that during heating (motor on) the temperature variation is given by:

$$\phi = T(1 - k_1\epsilon^{-\alpha t}) \qquad 0 < t < t_0 \qquad (6)$$

and during cooling (motor shut down) by:

$$\phi = Tk_2\epsilon^{-\beta t} \qquad 0 < t < t_f$$

where

$$k_1 = \frac{\epsilon^{\beta t_f} - 1}{\epsilon^{\beta t_f} - \epsilon^{-\alpha t_0}}$$

$$k_2 = \frac{\epsilon^{\alpha t_0} - 1}{\epsilon^{\alpha t_0 + \beta t_f} - 1}\,\epsilon^{\beta t_f}$$

Equation 5 then becomes:

$$\epsilon^{aT}\int_0^{t_0} \epsilon^{-aTk_1\epsilon^{-\alpha T}}\,dt + \int_0^{t_f} \epsilon^{aTk_2\epsilon^{-\beta t}}\,dt = \epsilon^{a\theta} \qquad (7)$$

which reduces to (3) for $\alpha = \beta = \infty$. Fortunately, for the purposes of calculation equation 7 can be further reduced to a function of the "integral exponential" (E_i) for which tables of values exist. Thus:

$$\frac{\epsilon^{aT}}{\alpha}\left[E_i(-aTk_1) - E_i(-aTk_1\epsilon^{-\alpha t_0})\right] + \frac{1}{\beta}\left[E_i(aTk_2) - E_i(aTk_2\epsilon^{-\beta t_f})\right] = \epsilon^{a\theta} \qquad (8)$$

Although this decidedly is not an explicit relation for T as a function of α, β, t_0, and θ, by dint of successive trials, the representative curves of figures 11 and 12 may be drawn.

In brief, the calculations demonstrate that under usual ambient conditions and with a reasonable variation between actual heating and cooling time-constants of motors used on normal time cycles the effect of assuming instantaneous variation of temperature with load is more conservative than necessary.

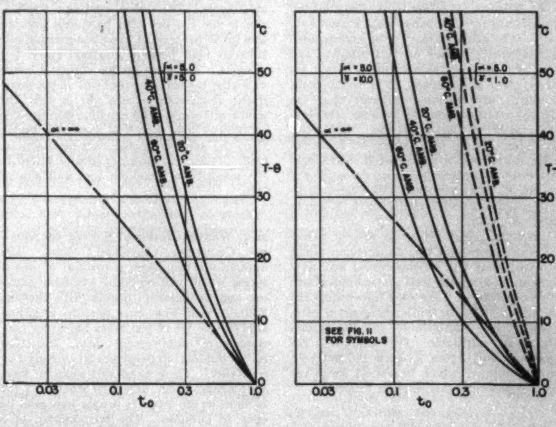
A typical small motor in the range from one-half to five horsepower will have a heating time constant of the order of ten minutes. With usual repetitive duty cycles of two hours or longer duration, therefore, the value of α will be of the order of 10 or more. The cooling time constant, with the motor at standstill is usually about five times as long as for heating. Thus, we may assume $\alpha = 10$ and $\gamma = 5$ as fairly typical values in service.

The effect of considering the thermal time lag of insulation is to allow a greater overload to be carried for the same duration or the same overload for a greater fraction of the total cycle time, as may be seen by referring to figure 11. For a temperature excess of $T - \theta = 25$ degrees centigrade (corresponding to 140 per cent current for a standard motor at 40 degrees centigrade ambient) a typical small motor ($\alpha = 10$, $\gamma = 5$) could permit an overload duration of 0.23. This is to be compared with the maximum duration of 0.33 which could be allowed if the cycle were so short that the temperature did not vary ($\alpha = \infty$), and to the value of 0.115 which would be recommended on the instantaneous heating basis ($\alpha = 0$) proposed in the body of the paper. The recommended values, therefore, make ample allowance for variations from usual conditions, and this margin decreases only slowly as the cycle time is increased. Figure 12 shows the relative effects of heating and cooling time constants. Increasing γ means increasingly slower cooling which, therefore, loses part of the advantage gained by the temperature lag while the motor was on. Anything which tends to equalize the rates of heating and cooling increases the allowable duration of overloads. A change from the typical condition of $\gamma = 5$ to equal rates of heating and cooling ($\gamma = 1$) for a temperature excess of $T - \theta = 25$ degrees ($\alpha = 5$, and 40 degrees centigrade ambient) increases the allowable duration from 0.31 to 0.50, as compared with the recommended value of 0.115. The extreme case of $\gamma = 10$, which shows some decrease in allowable time from the recommended values is quite unlikely in practice.

Offsetting this gain from time lag in heating is the temperature creep due to increased copper resistivity at elevated temperatures. From figure 11, for a typical case of $\alpha = 10$, $\gamma = 5$, 40 degrees centigrade ambient, thermal lag increases the allowable temperature rise for a cycle 15 per cent on and 85 per cent shut down, from 84 degrees centigrade to 105 degrees centigrade, or by a factor of 1.25. On the other hand, figures 4 and 7 indicate that on a continuous load, allowance for creep requires that the expected rise be set at only 93 degrees centigrade, if 105 degrees centigrade actual rise is to be obtained. Thus, the effect of temperature creep largely offsets that of thermal lag for high overloads and long cycles.

By this analysis, therefore, simple methods have been derived for determining the limiting permissible overload duration for any heating constants and any length of duty cycle, making use of figures 11 and 12. The results indicate, however, that the simple curves of figures 2 and 5 are adequate for most purposes, the additional overload capacity due to thermal time lag being of importance only for duty cycles of two hours or less.

Figure 12. Permissible excess temperature rise in intermittent service, for fixed insulation life (showing effect of cooling time constant compared to heating time constant)



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Discussion

L. A. Kilgore (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors are to be commended for a scientific attack on this problem and their ultimate simplification of some involved data and theory. The conclusions, of course, are no more accurate than the original assumed relation between temperature and life of the insulation. While this data seems fairly well confirmed from experience with actual machines, it would seem somewhat optimistic based on the results of the paper, "Temperature Life Characteristics of Class A Insulation," by J. J. Smith and J. A. Scott.

Since the paper refers in several places to 15 horsepower and up, the inference might be drawn that the typical curves and data given applied equally well to machines above 200 horsepower. I am sure the authors did not intend this, for while the general principles given are applicable, still the typical values are different. For example, the typical curves, figure 3, show 180 per cent starting torque, which is several times too high for general-purpose class I motors.

L. E. Hildebrand (General Electric Company, Lynn, Mass.): There is a point common to my paper and that of Messrs. Alger and Johnson which should be explained. I refer to the per cent time on for intermittent cycles. The same thing is said in both papers. However, one who reads them hurriedly without noting a change in viewpoint may hastily infer that we do not agree. Their paper shows the per cent time on for various loads based on an ideal long cycle. This might be called short year service, meaning that the motor is operated a few months per year or a few days per month, but when it does operate, it reaches and maintains continuous temperature for a long time. In their appendix, they show corrections for shorter cycles.

In my paper I consider ideal short cycles to which they refer in the section "Permissible Intermittent Overloads." In the ideal short cycle, the on and off periods are so short and so frequent that the temperature does not rise above the average. The ideal short cycle essentially describes machine-tool jobs. It is expedient and clearer to consider such jobs from the root-mean-square load standpoint. If the cycle becomes so long that the temperature does rise a few degrees above the average, a simple correction can be made. Hence if the cycle is hours per day, use long cycle methods corrected for thermal log. If the cycle is minutes per hour, use short cycle methods corrected for temperature variations. Most machine tool cycles are so short that no correction is necessary.

The permissible "time on" for short-cycle jobs is much greater than for long-cycle jobs. A motor with proper winding will carry about 140 per cent base load, 30 per cent time on if the cycle is long, 66 per cent time on if the cycle is short. 175 per cent base load, 6 per cent time on for a long cycle becomes 43 per cent time on for a short cycle.

The motor described in the section "Characteristics of Motors Designed for Intermittent Service" by Messrs. Alger and Johnson is very close to the one-hour motor of the next higher standard rating if it refers to the lower horsepower range. It is good for 175 per cent base load, 6 per cent time on ideal long cycle. The more common application is say 130 per cent base load perhaps less, on 80 per cent or even less running idle between load periods starting every few minutes. The extra torque ability is insurance that the machine tool will do the infrequent abnormally severe job or that the motor will operate the machine during some short peak load which is distinctly higher than the average or the root-mean-square load. Thus, this motor is ideally suited to the majority of machine tool and similar jobs.

H. L. Wallau (Cleveland Electric Illuminating Company, Cleveland, Ohio): The authors are proposing a method of motor rating which will combine available starting and breakdown torques with thermal limits to yield what they consider a reasonable service life of ten years for either continuous or intermittent duty, and that stator-winding temperatures be measured by resistance. These proposals seem to have logic behind them, although whether a life as short as ten years is proper may well be open to question. The introduction of a service factor permits the continuing use of the nominal ratings to which industry is accustomed.

In their final conclusion the authors state in part, "the temperature-life curve of figure 1 . . . should be reviewed . . . before final values for standards are accepted." This is very important inasmuch as the test curves from which curve *B*, figure 1, is replotted, do not yield a probable life expectancy of seven years for continuous operation at 105 degrees centigrade, and the use of this seven-year expectancy is basic to the values set up in the paper.

The curves from which Montsinger's figure 11 is derived, are shown in figure 6, page 783, April 1930, AIEE Transactions. The three curves numbered 4, 5, and 6 in this figure when plotted on semilogarithmic paper, with time to the logarithmic scale, and extrapolated to zero tensile strength, show relative temperature-life expectancies under oil, as follows:

Temperature (Degrees Centigrade)	Approximate Life	
	Weeks	Years
90	900	18
100	360	7
110	170	$3^1/_4$

Figure 11 as erroneously plotted indicates a life expectancy at 100 degrees centigrade of 11 years. For 105 degrees the correct indicated expectancy is of the order of five, not seven years. The plotting error was called to Montsinger's attention recently, as these data are reproduced on pages 261 to 263 of "Transformer Engineering," and the discrepancy was acknowledged.

A study of the individual curves of Montsinger's paper (figures 6 and 7) does not seem to warrant the statement that insulation in air will outlast insulation under oil. Curve 3 of figure 6 has a straight-line characteristic with a loss of tensile strength of one-half per cent per week, indicating an ultimate life of 200 weeks. This is for varnished cambric in air at 110 degrees centigrade. Curves 1 and 2 of the same figure for temperatures of 90 degrees centigrade and 100 degrees centigrade, respectively, have convex characteristics indicating a shorter life at these lower temperatures, and all three indicate shorter expectancies than the corresponding curves for this insulation under oil. Figure 7 shows similar discrepancies. It would appear that too few test data were available to Montsinger at that time to obtain results consistent with experience.

Much caution must be exercised therefore in the selection of the proper life expectancy of insulation before either standards or operating guides are formulated for industry, and it is quite possible that sufficient knowledge on this particular subject is not yet available.

Hubert H. Race (General Electric Company, Schenectady, N. Y.): The time-temperature relation of the deterioration of insulating materials is a very important problem to the designer and user of electrical apparatus and rightfully deserves the emphasis that has been given it at this convention. *Often the physical changes which make such deterioration evident are really indications of chemical changes which caused them, so that physical evaluations of deterioration are insufficient but should be accompanied by controlled chemical experiments.* Therefore it seems important to contribute to this symposium a short engineering statement of

Table I. Ratios of Rates of Chemical Change for Ten-Degree-Centigrade Intervals for Different Values of Heats of Activation (Q)

Q (Calories per Mol)	t_1 (Degrees Centigrade)	t_2 (Degrees Centigrade)	(k_1/k_2)
15,000	150	140	1.54
15,000	130	120	1.61
15,000	110	100	1.70
20,000	150	140	1.78
20,000	130	120	1.89
20,000	110	100	2.02
25,000	150	140	2.06
25,000	130	120	2.21
25,000	110	100	2.42

our knowledge of the general law expressing the temperature dependence of various physical and chemical phenomena. The most general statement of this law is

$$k = A\epsilon^{-\frac{b}{T}} \tag{1}$$

This general relation holds for phenomena such as chemical reactions, chemical equilibriums, evaporation, temperature coefficients of viscosity, and electrical conductance in semiconducting crystals. For chemical reactions, the law was given by Arrhenius in the form

$$k = A\epsilon^{-\frac{Q}{RT}} \tag{2}$$

where

k = a constant of a differential equation which defines the rate of the chemical reaction

A = an experimental coefficient

Q = the heat of activation in calories per mol

R = the gas constant = 1.986 calories per degree mol

T = absolute temperature in degrees Kelvin

Since the coefficients of the above equations have considerable significance from the general background of our theoretical and experimental knowledge, wherever possible new data showing temperature dependence of any kind should be examined in terms of this general relation. This can be done by plotting log (dependent variable) against $(1/T)$, which should give a straight line whose slope has physical significance.

Taking the logarithm of the ratio of k in equation 2 for two temperatures, we have

$$\log_e (k_1/k_2) = (Q/R)\,[(T_1 - T_2)/T_1T_2] \tag{3}$$

An approximate rule-of-thumb statement is that the rate of a chemical reaction doubles for a 10-degree-centigrade increase in temperature. This rule is illustrated in table I of this discussion, in which are given the ratios (k_1/k_2) of the rate constants of chemical reactions calculated from equation 3 for three sets of 10-degrees-centigrade intervals at each of three values of Q. For most chemical reactions, within the temperature range in which we are interested, Q lies between 15,000 and 25,000 calories per mol.

Equation 2, which has a large mass of chemical data supporting it, is different in form from the empirical relation given in equation 2 of the paper on "Rating of General-Purpose Induction Motors" by Alger and Johnson. The coefficients of their relation have no theoretical significance. However, by expanding equation 2 above in a series for $(T - \theta)$ it can be shown that neglecting second order terms gives the form of Alger and Johnson's equation 2, and that for the range of temperatures being considered the error in this approximation is less than five per cent. Therefore the two relations are equivalent within the experimental error.

Although the general validity of the above rule for chemical reaction rates is unquestioned, in any given insulation system a number of other conditions must be known before an estimate can be made of the effects of such chemical changes on insulation life. In service, continuous chemical changes may (*a*) improve, (*b*) have no effect upon, or (*c*) decrease the electrical quality of insulation, depending upon conditions. Examples of these three possibilities are numerous and may result from many different types of chemical change depending upon particular conditions, but for illustration a single reaction, that of continuous curing of varnish in stator coils in service, may give all three results in sequence.

Stator coils, to be assembled, must be flexible. Therefore the varnish used in making them is not completely cured to a hard rigid form. In service the chemical and physical changes called "curing" continue slowly, resulting in continuously improving electrical quality for some time. These gradual changes continue throughout the life of the insulation and even though they progress to the extreme where the varnish is very hard, brittle, and full of cracks they may not cause electrical failure if, for example, the apparatus is not exposed to moisture, and the basic insulation such as mica is not disturbed by vibration or excessive expansion and contraction. I helped to dismantle several of the original two-phase generators at Niagara Falls which were removed from service without failure, in which the organic insulation had been changed almost to dry dust but the machine had continued to operate as long as it was undisturbed. On the other hand these same changes might lead to early failure under more adverse conditions of service such as might be experienced by motors in mines or on shipboard.

The points I wish to emphasize in this discussion are: (1) chemical changes often underlie observed physical changes; (2) for this reason laboratory experiments on physical and electrical deterioration should be accompanied by chemical tests to determine causes; (3) experimental data showing temperature dependence should be examined in terms of equation 1 instead of being expressed in some other purely empirical form; (4) numerous specific conditions must be known before a conclusion can be reached as to the effects of such chemical changes on insulation life in any particular instance.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to discuss this paper from the broad question of name-plate rating versus output. My work has, of course, been concerned mostly with transformers in which temperature is the only limit to output.

AIEE Standards No. 13 at one time had the statement that the name-plate rating of transformers should not be exceeded whatever be the ambient. It is difficult to see how such a rule could be justified, but apparently some excuse for it was given. After getting rid of this rule in 1922 the way was open to recognize that name-plate rating and output may be entirely different. One of the first things we did in the rules was to permit one per cent overload continuously for each degree centigrade that the ambient is below the standard. On the other hand, if the ambient is continuously above the standard, permissible output was reduced accordingly.

This did not go far enough to meet the needs where short-time overloads (in excess of those permitted by the one per cent rule) are sometimes required to meet emergency conditions.

The next step was to set up permissible short-time overloads, ranging from short circuit of a few seconds to several hours time. To do this, however, required knowing, at least in a rough way, how short-time high temperatures affected the life of insulation. The tests reported in my 1930 paper showed that each time the temperature is increased eight degrees the rate of aging, of insulation immersed in oil, doubles.

Some of the data presented at this convention indicates that the eight-degree rule is too conservative; that is, it requires more than an eight-degree increase in temperature to double the rate of aging in air. I understand that someone in the Petroleum Institute presented a paper some time ago which showed that the deterioration of petroleum was doubled for each 8.5-degree-centigrade increase in temperature. I note that Messrs. Alger and Johnson used the eight-degree rule in their paper which, I feel, marks an important milestone in this question of rating versus output of rotating machinery.

I am not so much concerned in whether the eight-degree rule is absolutely correct, as I am in getting this fundamental method of analysis applied to electrical apparatus where temperature is the principal limit to output.

I do not want to give the impression that I am advocating that short-time overloads on rotating machinery be limited by temperature alone, as we have done for transformers, because there are limitations other than temperature for most classes of rotating machinery.

I do feel, however, that it is possible to go quite a ways yet in working out safe moderate overloads, as Messrs. Alger and Johnson have done. I venture the prediction that in a few years the name-plate rating of a large part of smaller rotating machinery will be used mostly for purchasing purposes, while the output will be more or less than the rating, depending on the operating conditions—the same as is true for transformers today.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Messrs. Alger and Johnson recognize the same difficulty outlined in Mr. Rutherford's current paper ("Rating and Application of Motors for Refrigeration and Air-Conditioning Systems," AIEE Transactions, volume 58, 1939, see 1939 annual Transactions index for page numbers) that the present horsepower ratings, particularly of fractional-horsepower induction motors, do not adequately describe the motor. Their proposal is to increase the service factor and leave the present horsepower ratings intact. This proposal would undoubtedly be acceptable to the refrigeration manufacturers since it effects no change in ratings, but does little to clear up existing confusion as to control and wiring. In general, the torque and current specifications given by the authors are acceptable as they are conservative values for present-day commercial capacitor-start motors. But these standards can not universally apply to split-phase motors. However, we see little point in reducing the locked-rotor current specifications for the one-eighth and one-sixth-horsepower sizes.

The whole subject of locked-rotor currents is one which may well concern AIEE whose membership includes the power companies who are as vitally interested in this subject as well as the motor manufacturers. The great disparity of requirements in this respect was brought to light by a recent survey of 51 leading public utilities in the United States. The maximum permissible locked-rotor current for a three-fourth-horsepower 220-volt single-phase motor allowed by each company was tabulated with the following results:

1 company specifies 12.25 amperes maximum
16 companies specify 20 amperes or less
29 companies specify 30 amperes or less
The National Electrical Manufacturers Association specification is 30.5 amperes
22 specify more than 30 amperes (of these 22, 13 have no specifications)

In other words, 29 of 51 companies have not accepted the value established by NEMA as the lowest value to which motor manufacturers can work without penalizing the motor design unnecessarily. Fortunately many of these 29 companies do not enforce their locked-rotor current rules. The few that do often work a hardship on the motor user, forcing the motor manufacturer to design and build special motors for certain localities. This procedure usually involves expense and inconvenience both to the manufacturer of the motor and the builder of the appliance, generally resulting that the customer who buys the motor-operated appliance has to pay more for a less satisfactory appliance than a more fortunate customer who is permitted to use the standard motor. Some standardization is urgently needed in order not to obstruct progress in the development of motor-driven appliances, and the resultant general increase in use of electric power.

T. C. Johnson: Two important ideas which are the natural outgrowth of the paper on "Rating of General-Purpose Induction Motors" just presented arise for discussion at this time. The first concerns the available torque of the general-purpose motor and the second concerns the relation between the service factor, frame size, and the ten-degree temperature rise margin.

The available torque of general-purpose motors is very important and therefore it is fitting to re-emphasize this point as made in the paper.

Under the competition of good design and the guidance of AIEE, NEMA, and ASA standards, a method of rating has grown up which specifies that a motor operated continuously at rated load under the most severe conditions will have a temperature rise of 40 degrees. The standards also specify that the motor must be capable of delivering 115 per cent of its rated output continuously without damage to itself and that it must have a maximum torque at breakdown of not less than 200 per cent of rated torque. Since due allowance must be made for possible voltage reduction and unexpected variations in the application, good practice requires that not more than 135 per cent of rated torque be demanded. However, all of this torque is available and should be used for economic application although the extent to which this overload torque can be used depends on the application since temperature rise is an additional limitation.

Considerations of available torque lead to a discussion of the service factor and its relation to the rating of successive frame sizes. The service factor is most simply defined as the ratio of the maximum continuous load that can safely be handled to the rated continuous load. Under the present standards this corresponds to the ratio of the output with a 50-degree rise to the output with a 40-degree rise. The possible difficulties due to the service factors increasing in size for the smaller-horsepower motors, as shown in the paper, may be removed by considering the service factor as a simple measure of the step in rating between successive frame sizes. For integral-horsepower motors which have an average step in rating of 30 per cent between successive frame sizes, the service factor, defined as before, is equal to 1.15 and corresponds therefore to a step of one-half frame size in rating.

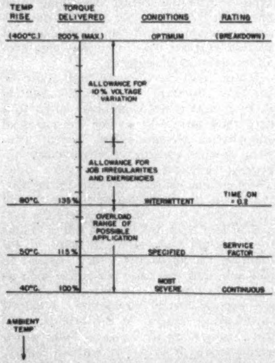

Figure 1. Available torque of integral-horsepower motors (15–200 horsepower)

For fractional-horsepower motors the case is very similar. A small motor has higher breakdown torque than a large motor for the following reasons: (1) the steps in rating between frame sizes are larger in order to cover the range economically, (2) the uncertainties in application are a greater percentage of the output, (3) the power factor is naturally poor, (4) applications are most commonly intermittent.

For these reasons the service factor, again defined as the 50-degree rating over the 40-degree rating, for small motors is equal on the average to 1.4. Since the average step in frame size horsepower for small motors is about 40 per cent, the service factor may most simply be thought of as a step of one frame size in rating.

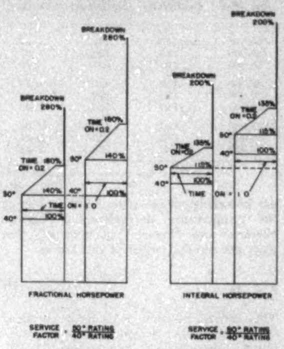

Figure 2. Relative output of successive frames

These two points of available torque and service factor are summarized on two figures. In the first figure the bottom line represents, operation at rated output, 40-degree rise, 100 per cent load, continuous service under most severe conditions. The next line above represents operation at the service factor rating, 50-degree rise, continuous operation. The top line represents the breakdown torque. Making allowances as shown for voltage variation and mechanical emergencies, there is left 135 per cent rated torque to be used. For a load which is on only two-tenths of the total cycle time, this full 135 per cent torque can and should be used for economic application.

On the second figure is shown the relative output of successive frame sizes, first for integral-horsepower motors where the 1.15 service factor corresponds to an increase in rating of one-half frame size and second for fractional-horsepower motors where the average service factor of 1.4 corresponds to a step of one whole frame size. The relative widths in the diagram correspond to the approximate times for which the corresponding torque can be utilized with safety. Thus the service factor may very conveniently be related to the step in rating between successive frame sizes.

P. L. Alger: We appreciate the comments of Messrs. Race and Montsinger, who agree with our use of the simple law of about half life for each eight-degree increase in temperature, as a practical way of expressing the effect of temperature on insulation. The actual life obtained on any given motor depends in a very high degree on the particular conditions met with, such as the ambient temperature, the load cycle, and especially the degree of dirt, moisture, vibration, etc. The use of the law is rather in estimating the effect of a temperature change on insulation life, found by experience in particular conditions, than in estimating the life under totally new conditions.

Answering Mr. Wallau's statement that our curve *B*, figure 1, is not an exact representation of Mr. Montsinger's 1930 test data, the curve does represent an over-all average of experience, of which the particular tests reported by Mr. Montsinger are only a part. The important conclusion is that the 105-degree-centigrade limiting hot-spot temperature for class *A* insulation has been found generally satisfactory in industrial service.

In reply to Mr. Kilgore's comment, the curves in the paper are intended only to cover general-purpose motors—that is, 200 horsepower and smaller at 450 rpm and higher speeds. The starting torques are assumed to correspond to the NEMA standard of 150 per cent of full load torque for four-pole motors. The 180 per cent value for the 25-horsepower motor shown in figure 3 provides a reasonable margin over this 150 per cent guaranteed figure.

Mr. Veinott points out the wide variation in starting-current requirements of different power companies and emphasizes the importance of having such adequate standards that the motor name-plate conveys a definite description of the motor's performance. This is very pertinent, as, under present conditions, many small motors are operated at loads greatly in excess of their name-plate values, and this leads to a great deal of uncertainty about the proper control and wiring.

There are two possible ways of procedure. One is to have a great number of special motors designed for particular services, each with a name-plate horsepower and time period of rating exactly representative of the expected motor performance. Such motors would naturally have a low ratio of maximum to full-load torque and would have a relatively low starting current per rated horsepower. The other procedure is to have a limited number of special motors of the foregoing closely rated types, and in addition to have a standard general-purpose type of motor suitable for operating under any one of a wide range of conditions. Such general-purpose motors, however, would naturally be used at widely different values of horsepower, depending on the intermittency of load, the ambient temperature, the desired life, and other conditions. If such motors are used, it is logical to give them a single conservative rating, representing the output the motor can always be relied upon to deliver under severe conditions; and, in this case, such motors will have relatively high starting and breakdown torques, high starting currents, and considerable overload ability, which will be used in many applications. The permissible starting current should, therefore, be based on the service-factor rating of the general-purpose motor, to be consistent with the rules for closely rated special-purpose motors.

In conclusion, I wish to point out again that this paper is intended to give a better understanding of the range of possible application of a standard general-purpose motor which, without change in name plate, is admirably fitted to a wide range of uses.

Determination of Temperature Rise of Induction Motors

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Synopsis: The "American Standards for Rotating Electrical Machinery" (American Standards Association) prescribe that the temperature rise of motors shall be measured by the thermometer method for purposes of rating. Definite limiting values of temperature rise are established for each type of machine. The AIEE Standards No. 1 fix conventional allowances of 15 degrees centigrade, 10 degrees centigrade, and 5 degrees centigrade between the actual hottest-spot temperature and the highest observable value of temperature as determined by thermometer, resistance, and embedded-detector methods, respectively.

This paper presents information on the relations between the measured values of temperature rise by different methods as found in tests on several hundred induction motors ranging from 10 to 1,000 horsepower in rating. It is shown that, with modern motor construction, variations of 20 degrees centigrade or more are sometimes obtained by the thermometer method on a given machine depending on location of thermometers or thermocouples, whereas the resistance measurements give relatively consistent values of temperature rise.

It is therefore suggested that the standards for temperature-rise measurements be revised, and that the resistance method be adopted for all forms of enclosed or protected machines which are not readily accessible for application of thermometers on laminations, insulated windings, and other adjacent parts.

TEMPERATURE rise is more frequently a limiting feature than is any other single motor characteristic in determining the maximum horsepower ratings that may be obtained from a given induction-motor frame size. Dependable heating data are of primary importance, because the probable length of insulation life diminishes rapidly at excessive winding temperatures. Procedures for obtaining motor temperature measurements are not well standardized at the present time, and the reliability of heating data cannot be satisfactorily evaluated unless the fidelity and accuracy of testing methods are known.

The objects of this paper are (1) to suggest revision of the present induction-motor standards to place temperature measurements on a better basis, (2) to present test data which illustrate the need for and justification of these revisions, and (3) to suggest new temperature ratings of motors on basis of using the resistance method of determining temperature rise.

AIEE Standards No. 1 dated April 1925, recognize three fundamental methods of temperature determination which are defined respectively as the thermometer, resistance, and embedded-detector methods. Rules governing the interpretation of these different methods are quoted from paragraph 1–7 of the standards:

> **1–7 Limiting Observable Temperatures and Conventional Allowances.** Limiting "observable" temperatures are deducted from the limiting "hottest-spot" temperatures by subtracting therefrom a specified number of degrees which, FOR PURPOSES OF STANDARDIZATION, is the margin fixed between the limiting hottest spot and the limiting observable temperatures.
>
> This margin is designated as the "CONVENTIONAL ALLOWANCE."
>
> The specified differences (which may be designated the "Conventional Allowances") by which the "observable" temperatures are, FOR PURPOSES OF STANDARDIZATION, assumed to be lower than the "hottest-spot" temperatures, are as follows:
>
> Thermometer Method—
> 15 degrees centigrade
> Resistance Method—
> 10 degrees centigrade
> Embedded-Detector Method—
> 5 degrees centigrade

On the basis of the standard 105-degree-centigrade limiting "hottest-spot" temperature, the maximum "observable" temperature must not exceed 90 degrees centigrade by thermometer for class *A* insulation; and from this were derived the present limiting ratings of 50 degrees centigrade rise by thermometer above the standard 40 degrees centigrade ambient for special-purpose open motors, and 40 degrees centigrade rise combined with a 1.15 service factor for general-purpose open motors. The "conventional allowances" for "hottest-spots"

Paper number 39-3, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 21, 1938; made available for preprinting November 25, 1938.

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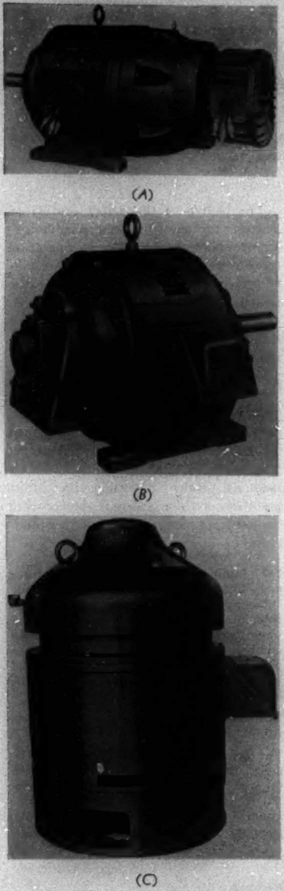


(A)

(B)

(C)

Figure 1. Typical induction motors of modern construction which are not readily accessible for thermometer measurements

(A) Totally enclosed fan-cooled wound-rotor type, (B) splashproof type, and (C) vertical motor for outdoor service

have generally been considered as five degrees centigrade less for totally enclosed than for open motors because the windings of the former are heated more uniformly, and from this premise the maximum rating of 55 degrees centigrade rise by thermometer above a 40 degrees centigrade ambient was adopted for totally enclosed motors.

Paragraph 5.050 of the ASA C-50 standards approved on January 6, 1936 (subsequently to adoption of AIEE Standards No. 1) specifies that all temperatures of induction motors shall be determined by thermometer method. ASA rules 2.055 and 2.063 relative to thermometer method are as follows:

> 2.055—*Thermometer Method of Temperature Determination Defined.* This method consists in the determination of the temperature by mercury or alcohol thermometers, by resistance thermometers, or by thermocouples, any of these instruments being applied to the hottest part of the machine accessible to mercury or alcohol thermometers.
>
> 2.063—*Measurement of Machine Temperatures.* As far as practicable, temperature measurements shall be taken during the test, without making any change in the machine which will affect the results, as well as immediately after shutdown. The temperature of totally enclosed motors (including totally enclosed fan-cooled motors) shall be obtained after shutdown by applying the thermometer to the hottest part of the machine which can be made quickly accessible by removing covers. If the core and windings are not accessible such as in totally enclosed or totally enclosed fan-cooled motors, holes must be provided through any intervening structural parts to permit the application of the thermometer method.
>
> The highest measurements obtained shall be the accepted values.

The location of the hottest observable point in a motor actually depends on a number of factors such as the enclosing features if any, type of winding and insulation treatment, arrangement of fans and air deflectors, number and size of air ducts, relative proportions of component losses, etc. No standardized thermometer locations would be suitable for all types of motor construction. Consequently the test results by thermometer method may or may not be representative of actual maximum temperature rise depending on familiarity of testing personnel with motor characteristics, and also on whether a conscientious effort is made to locate the hottest observable point in the machine. The term "accessible" is also subject to individual interpretation and cannot be sharply defined.

A different arrangement of thermometers may occasion a wide disagreement in test results when the same motor is tested by two different parties, and these variations are frequent sources of controversy. The testing situation is becoming increasingly complicated because of the definite trend toward more enclosing features in the design of induction motors. A large percentage of modern motors now has a mechanical construction which either reduces or eliminates entirely the accessibility of internal parts as illustrated by figure 1.

To clarify this unsatisfactory situation with respect to temperature measurements, testing methods should be modified in accordance with changes in motor construction, so that comparable results may be obtained when the same machine is tested by two different organizations. This paper recommends general recognition, adoption, and standardization of the resistance method. Results of many tests are presented to prove that the winding resistance gives a more reliable indication of actual insulation temperature than measurements made with thermometers.

Some Specific Situations for Which the Present ASA Standards Are Inadequate

Specific test results and situations are presented to illustrate the inadequacy of the present ASA standards relative to heating tests.

1. TOTALLY ENCLOSED FAN-COOLED MOTORS

Reliable winding temperature data usually are not obtained by thermometers inserted through holes bored in the frame of an enclosed motor as directed by ASA rule 2.063.

A test made on the totally enclosed fan-cooled motor designated as X in appendix I and table I, where the stator winding was explored by placing 28 thermometers and thermocouples at different points as indicated in figure 2, shows variations of 22 degrees centigrade (27 degrees centigrade to 49 degrees centigrade) in temperature rise, on the surfaces accessible to thermometers. A maximum variation of 32 degrees centigrade (27 degrees centigrade to 59 degrees centigrade) is obtained by including the inside surfaces of coil ends which are accessible to thermocouples but not to thermometers inserted through holes bored in the frame.

2. MULTIWINDING MOTORS

On multispeed motors having more than one stator winding, thermometers usually cannot be placed on the underneath winding. The lower-speed winding is almost invariably located inside next to the air gap. Because of reduced ventilation this lower-speed winding usually has the higher temperature rise, especially on constant-horsepower and constant-torque ratings. Thermometer measurements taken on the outside idle winding may be from 10 degrees centigrade to 20 degrees centigrade lower than the actual temperature of inside active winding. Confirming tests are described in appendix II, and the test data are presented in table II. With the "sandwiched" type of coil construction, the inside winding may not be accessible for either thermometers or thermocouples. The ASA standards do not have any satisfactory provisions for testing multiwinding induction motors. On open as well as enclosed multiwinding motors, only one winding is usually accessible for the thermometer method; consequently this criticism of the present standards applies to all types of mechanical construction.

3. Wound-Rotor Machines With Inaccessible Rotors

No provisions are made in ASA standards for rotor temperature measurements of totally enclosed wound-rotor motors of the form shown in figure 1. With this construction the rotor winding is usually the limiting feature, because its temperature rise is commonly 10 degrees centigrade to 20 degrees centigrade above that of the stator. On all forms of totally enclosed as well as on most splashproof wound-rotor machines, the insulated rotor winding is completely inaccessible to the thermometer method when outside enclosed collectors are used. The provision of paragraph 2.063 of ASA standards for "applying the thermometer to the hottest part of the machine which can be made quickly accessible by removing covers" is certainly not applicable where it becomes necessary to practically disassemble the machine in order to gain access to at least one end of the rotor winding. Such a major disassembly of large machines requires so much time that subsequent temperature readings are of little significance. In field tests it may not be possible to disassemble the motor.

4. General-Purpose Motors

Although general-purpose motors with open-type construction are more accessible for application of thermometers on the windings and laminations than the types of machines cited in the preceding paragraphs, nevertheless, there is considerable variation in the results which may be obtained by different parties due to varying locations of thermometers. Appendix IV and table IV present results of tests made on motors of 12 different manufacturers, and five of these machines showed a temperature rise higher than the name-plate stamping. It, therefore, seems most probable that this situation is a result of variations in testing procedure which may now occur under the present standards.

5. Explosion-Proof Motors

Although the Underwriters do not actually forbid the boring of holes for thermometers in explosion-proof motors, such a practice is dangerous and undesirable; because any failure to plug a hole securely might result in an explosion external to the motor after the machine is placed in normal service. The present standards as worded therefore may create a potential hazard when tests are taken on explosion-proof machines.

Summary of Test Results by Resistance Method

To investigate and demonstrate the dependability and consistency of the resistance method of measuring temperature rise, an analysis was made of approximately 300 heating tests on various types of polyphase induction motors over the range of sizes from 10 to 1,000 horsepower. Temperature rises were measured both by the resistance method and also by thermometers (or thermocouples). The test results may be summarized as follows:

1. On totally enclosed and totally enclosed fan-cooled motors, the stator temperature rise by resistance method is essentially the equivalent of that obtained by thermocouples applied at hottest part of winding surface. On the basis of averages, the thermocouples checked the resistance method within 1.1 degrees centigrade (48 degrees centigrade–46.9 degrees centigrade) for the 65 tests on totally enclosed fan-cooled motors, and within 0.6 degrees centigrade (40.1 degrees centigrade–39.5 degrees centigrade) for the 26 tests on totally enclosed motors which are tabulated in table III. To locate the hottest winding surface, some of the thermocouples were placed at points which were not accessible to thermometers inserted through holes in stator frame as explained in appendix III.

2. On open-type or partially enclosed splashproof (or dripproof) machines rated 2,200 volts or less, heating tests on 149 different motors as described in appendices III and IV indicate about two degrees centigrade less average stator temperature rise by the resistance method than by thermometers (or thermocouples at locations which could have been reached by thermometers through holes in the frame) searchingly applied in accordance with the present ASA standards. (The 149 tests include the stator temperature measurements on the 41 splashproof and 96 open motors of table III and on the 12 open motors of table IV.)

3. Measurements taken on the 36 insulated wound-rotors of table III show an average temperature rise of 4.6 degrees centigrade (34 degrees centigrade–29.4 degrees centigrade) more by change in resistance than by thermometer. Lower and in general less accurate values of temperature rise are obtained with thermometers because: (*a*) thermometers cannot be placed on center of rotor laminations in most cases, (*b*) the rotor must stop turning before thermometers can be applied and they do not attain maximum indication until after resistance readings are taken, and (*c*) rotors of many motors are not readily accessible and good thermometer contacts cannot always be obtained.

4. The resistance method is consistently reliable for all speeds and time ratings. In table III the results are segregated according to motor poles over the range from 2 to 12 poles for the continuous-rated squirrel-cage motors. Heating runs of one hour or less are separated from the tests of longer duration.

5. The resistance method is dependable over a wide range of motor load and line voltage as demonstrated in appendix V, table V, and figure 4. Tests on a representative 25-horsepower motor for different conditions of operation show that the temperature rise by any method is approximately proportional to the total motor losses.

6. Maximum differences of about ± ten degrees centigrade between the thermometer and resistance methods have been observed in tests on individual machines. The resistance method may definitely indicate either a higher or lower temperature than thermometers depending on mechanical construction of motor, the two extreme conditions being:

(*a*). Motors with improperly proportioned ventilation may have local hot spots where

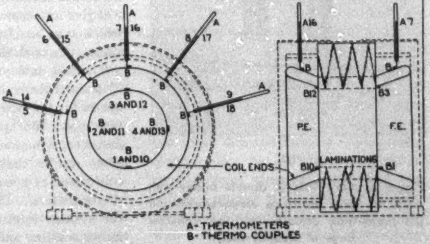


Figure 2. Location of thermometers and thermocouples on motor X

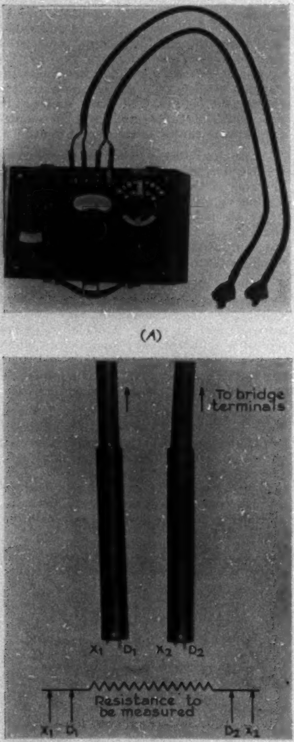
(Refer to appendix I and table I)

thermometer readings exceed resistance indication, but in many cases these hot spots are not located and explored by the thermometer method of test.

(*b*). High-voltage machines, especially 4,000 volts and above, usually (but not always) show a higher temperature rise by resistance than by thermometer because of the temperature drop in the coil insulation, in which case the resistance method is safer because it represents actual conditions.

Discussion

The thermometer method, as commonly used, is not a sufficiently definite procedure for determining the temperature rise of partially or totally enclosed motors, because it is probable that an insufficient number of thermometers will be used to locate the hottest point accessible to them. Wide variations in temperature may occur over a motor surface that appears to be uniform and symmetrical. The element of "chance" is therefore always associated to some extent with the thermometer method. On splashproof and other types of partially closed motors, thermometer readings may be as much as 10 degrees centigrade below the temperature of the hottest surface that might have been reached. Motor *K* of table IV in appendix IV is cited as a specific example wherein the actual maximum observable temperature rise of 61 degrees centigrade by thermometer was 11 degrees centigrade higher than the manufacturer's name-plate rating of 50 degrees centigrade—presumably because his test with thermometers did not locate the hottest spot.



(*A*)

(*B*)

Figure 3. (A) Portable double bridge with leads for stator resistance measurements and (B) prong-type contacts used on collector rings of wound rotors

On totally enclosed machines where it is necessary to drill a separate hole in the frame for each thermometer, only a few are likely to be used—sometimes only one, and this increases the probability that the hottest part will not be located. Furthermore when thermometers are inserted in holes bored through intervening parts, it is usually impossible to cover the bulbs completely with pads or putty as specified in paragraph 2.056 of ASA standards. If only the end of thermometer bulb makes a point contact with the winding or laminations, the reading may be influenced by the surrounding air to which the bulb is exposed. As a practical result of all these factors, the trend toward more enclosing features on motors has been accompanied by increasing variations in the test results that are obtained by the thermometer method. Motor *X* of table I in appendix I is a striking example.

As the present standards do not insure reasonably accurate and consistent results when various motor types are tested by different organizations, it is therefore highly desirable to adopt a more convenient, more definite, and less expensive method of measuring temperatures which will give an accuracy comparable to that which can be obtained by a thorough exploration with thermometers.

The test data point to the desirability of standardizing on resistance measurements for the determination of temperature rise. For practically all types of motor construction, the resistance method gives results that are essentially the equivalent of a careful exploration with thermometers. The resistance reveals the average internal temperature of the motor winding and thereby gives a more reliable indication of actual insulation temperature than a few thermometer readings taken at isolated points on the outside surfaces of the coils.

The relation between winding resistance and temperature is a specific, definite physical law that is entirely divorced from the element of chance which is inherently associated with the thermometer method. The winding resistance can be measured quickly, conveniently, and at little cost. From the standpoint of comparative tests, the precision of the resistance method is limited only by the accuracy of resistance readings and the measurements of initial and ambient temperatures. Consequently it is believed that a general adoption of the resistance method will promote greater accuracy in testing, will assure the motor user of greater insulation life, will reduce controversy by making it possible for different organizations to obtain comparable test results, and will provide the motor user with a convenient method for taking field tests.

Precedents have already been established for recognizing the resistance method. The International Electrotechnical Commission European Standards, the United States Navy Specifications 17*M*10, and an increasing number of commercial customers now either accept or require the resistance method of testing to prove temperature-rise guarantees.

Although the resistance method requires accurate measuring devices and precision in testing procedure, experience indicates that these difficulties are minor compared to the benefits obtained when testing motors that do not have open-type construction. Readily portable instruments have been developed and are now available which have an accuracy that is satisfactory for motor temperature measurements. At least 95 per cent of the resistance data for the heating tests included in this report were obtained with a portable-type double-bridge arrangement similar to that shown in figure 3. The remainder of the readings were taken with the voltmeter-ammeter method by passing direct current through the winding. The portable double-bridge has proved very satisfactory in general testing work. It is readily carried, accurate, rugged, has a wide range of measurement, and is not easily thrown out of calibration. A description of this general type of bridge is given in an article entitled "The Portable Double Bridge" by L. O'Bryan in the *General Electric Review*, volume 34, 1931, page 752.

In common with most indicating instruments, the probable per cent error of measurement decreases as the upper end of bridge scale is approached. Bridges covering a range from 0.0005 to 4.4 ohms, and having a maximum probable error of 0.4 per cent in ohms (or about one degree centigrade) at most unfavorable part of scale that must be used, and a minimum probable error of 0.1 per cent at upper end of scale, have been found suitable for temperature measurements of motors between 10 and 1,000 horsepower for which data are presented in this paper. Bridges of this type can be made with various scales and scale multipliers to adapt them to different horsepower ranges of motors. Even though cumulative errors of 0.4 per cent in both the hot and cold resistance measurements do cause an error of two degrees centigrade in temperature rise, this is small compared to the error commonly made when a thermometer is inserted through a hole bored in the stator frame of an enclosed motor.

Errors due to contact drop in current circuit are minimized by using four leads to connect the unknown resistance to bridge terminals as shown in figure 3. Leads X_1 and X_2 carry the current which flows through the unknown resistance whereas D_1 and D_2 are potential leads. The voltage between D_1 and D_2 does not include the contact drops at X_1 and X_2. Part *A* of figure 3 shows the spring-type clips which are used for stator resistance measurements. The two contact surfaces of each clip are insulated from each other to provide separate voltage and current contacts.

Wound-rotor measurements are obtained with the contacts in part *B* of figure 3. The sharp prongs X_1 and D_1 are held firmly against one collector ring while X_2 and D_2 are placed upon another collector ring. Prongs D_1 and D_2 are supported on flexible coil springs which depress under pressure and thereby permit all prongs to maintain contact even though they are not held at right angles with collector-ring surfaces.

The following fundamental precautions will minimize testing errors and insure dependable heating data by the resistance method:

1. Use a bridge which is accurate within 0.5 per cent on lowest part of scale that must be used.

2. Calibrate the bridge periodically and keep the leads in good condition. Make sure that contacts are clean, and check galvanometer when readings are taken.

3. The persons taking resistance measurements should use all necessary precautions to obtain and record accurate data. A good check on the accuracy of results is obtained by having two different persons take readings with separate equipments. The two resistance readings should agree within 0.4 per cent (or about one degree centigrade).

4. Take resistance readings every minute for about ten minutes after motor stops and project the curve to zero time.

5. When taking "cold resistance" before the heat run, never assume that the motor temperature is the same as the ambient. Always place thermometers directly on motor. The motor may have been moved recently from another part of factory where temperature was different, the machine may not have cooled off completely from some previous manufacturing or testing operation, or the ambient temperature may have changed suddenly because of an open factory door. Accurate cold temperatures are just as important as accurate resistance readings.

6. Always take both hot and cold resistance measurements between the same two stator terminals or the same two rotor collector rings to avoid errors due to slight unbalance in phase resistances. If nonuniform heating is anticipated because of unbalanced power supply or unsymmetrical design, record resistance measurements of each phase separately.

Suggested Motor Temperature Ratings for Resistance Method of Test

If the resistance method of measuring temperature rise is accepted as standard, the question arises as to what temperature ratings by resistance shall replace the present 40-degree-centigrade, 50-degree-centigrade, and 55-degree-centigrade ratings by thermometer for general-purpose, special-purpose, and totally enclosed motors, respectively. The conventional "hottest-spot" allowance differentials in AIEE Standards No. 1 would suggest that all these ratings should be increased 5 degrees centigrade for the resistance method—namely to 45 degrees centigrade, 55 degrees centigrade, and 60 degrees centigrade. The test data show that, for any normal low-voltage induction motor, the resistance method is practically the equivalent of the thermometer method when thermometers are searchingly applied to locate the hottest observable part of the machine. The two methods agree within three degrees centigrade in most cases and seldom is the disagreement more than five degrees centigrade. The test results therefore indicate that an increase of not more than five degrees centigrade should be made in the present motor ratings to obtain the equivalent ratings by the resistance method. In fact, an increase of even five degrees centigrade must be justified partly on the grounds that more accurate results will be obtained by the resistance method than by thermometers as commonly used.

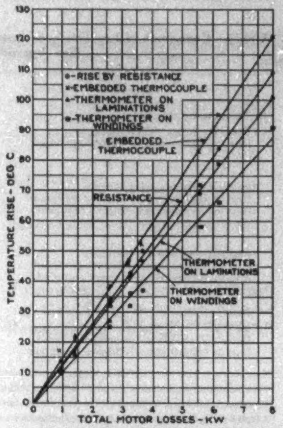


Figure 4. Relations between total losses and temperature rise by different methods of measurements for a typical 25-horsepower motor

(Refer to appendix V and table V)

In actual practice over the past ten years, however, the points of measurement by the thermometer method have not generally been the hottest surfaces. Hence, it is probable that 50-degrees-centigrade rise by resistance is a close approximation to the value that would be found by test on many partially closed motors now rated 40-degrees-centigrade rise by thermometer. The original 40-degree-centigrade rating was adopted in the standards on the assumption that thermometers gave values 15 degrees centigrade below the actual "hottest-spot" temperature; and when it is recognized that the resistance method gives test values that are approximately within 5 degrees centigrade below the hottest spot in many (but not all) motors, an argument is presented for adopting 50 degrees centigrade by resistance to replace the 40-degree-centigrade rating by thermometer.

To obtain confirming test data on the differentials between the internal "hottest-spot" and the limiting "observable" temperatures by different methods of measurement, thermocouples were distributed among the copper wires inside the stator slots when the random winding was installed in a typical low-voltage

Table I. Comparison of Two Totally Enclosed Fan-Cooled Motors of Different Manufacturers (See Appendix I and Figure 2)

	Front End				Pulley End			
Test Results for Motor X								
*Position—inside coil periphery	1	2	3	4	10	11	12	13
Rise by thermocouple—degrees centigrade	54	54	54	53	58	58	58	59

	Front End				
*Position—outside coil periphery	5	6	7	8	9
Rise by thermocouple—degrees centigrade	27	41	39	30	27
Rise by thermometer—degrees centigrade	39	43	38	29	27

	Pulley End				
*Position—outside coil periphery	14	15	16	17	18
Rise by thermocouple—degrees centigrade	47	47	47	48	49
Rise by thermometer—degrees centigrade		46	46	48	48

	Front End				Pulley End			
Test Results for Motor Y								
Inside coil periphery—degrees centigrade	50	50	51	51	49	51	51	50
Outside coil periphery—degrees centigrade	48	46	48	49	48	49	49	49

Temperature rise by resistance, motor *X*, one minute after shutdown 51 degrees centigrade.

Thermometers in positions 5 and 16 were not embedded in putty.

The other thermometers and all thermocouples were embedded in putty.

* Refer to figure 2 for locations of thermometers and thermocouples.

All above temperatures on motor *Y* were obtained with thermocouples.

Temperature rise by resistance, motor *Y*, 54 degrees centigrade.

25-horsepower motor as explained in appendix V. The heating tests on this motor, which are presented in table V and figure 4, show that for the three tests taken near normal load at 25.9, 26.4, and 25.8 horsepower, the corresponding differentials between the resistance method and the internal "hottest-spot" were six degrees centigrade, four degrees centigrade and five degrees centigrade, respectively—or an average differential of five degrees centigrade as compared to the present "conventional allowance" of ten degrees centigrade in paragraph 1–7 of AIEE Standards No. 1.

From the point of view of retaining the same limiting "hottest-spot" temperature of 105 degrees centigrade for class *A* insulation as originally intended in the standards, there is considerable justification for specifying 50 degrees centigrade by resistance in place of 40 degrees centigrade rise by thermometer in future standards. Such a step, however, might not leave sufficient margin for the conventional limits of voltage and frequency variation and for the 15 per cent service factor which are now specified in the ASA standards for general-purpose motors.

Similarly, 60 degrees centigrade and 65 degrees centigrade rise by resistance may ultimately replace 50 degrees centigrade and 55 degrees centigrade by thermometer for special-purpose and totally enclosed motors, respectively. However, a 65-degree-centigrade rating for class A insulation based on a 40 degrees centigrade ambient would not provide any margin for "hottest-spot" allowance. Actually the internal temperatures of an enclosed motor are normally more uniform than in an open-type machine. Nevertheless, it is logical that some margin for hot spots should be provided, because the resistance method can give only the average winding temperature. It may, therefore, be found desirable to accept 60 degrees centigrade instead of 65 degrees centigrade rise by resistance for totally enclosed motors, unless the standardized ambient temperature is reduced from 40 degrees centigrade to 35 degrees centigrade to be in closer agreement with prevailing temperatures in the United States. The long standing and justifiable precedent of rating open motors five degrees centigrade lower than enclosed machines may make it necessary to accept 55 degrees centigrade rise by resistance for special-purpose open motors, and in turn 45 degrees centigrade for general-purpose open motors, if 60 degrees centigrade by resistance is adopted as the temperature ceiling for totally enclosed construction.

Although the resistance method of temperature measurement is particularly adapted to inaccessible motors of partially or totally closed construction, test results demonstrate that it is also very satisfactory and dependable for open-type motors. However it may be desirable to retain the thermometer method in the standards for motors of entirely open construction, because it is the simplest possible procedure, and requires no special measuring equipment beyond the common mercury thermometer with which everyone is familiar. Even if resistance measurements are adopted as the standard method of testing and rating all types of motors, thermometers will still be needed for measurements of initial and ambient temperatures, bearing temperatures, for determining when the motor has reached a constant temperature, and for obtaining quick, approximate readings for check purposes.

Conclusions

To place motor specifications on a more sound basis, and to assure the motor buyer of more accurate and comparable temperature measurements, it is recommended that the resistance method of determining temperature rise be adopted as standard for all types of partially or totally enclosed induction motors, except for large, high-voltage machines for which the embedded-detector method should be retained.

It is also recommended that general industrial experience with respect to the present limits of temperature rise be reviewed, especially as regards "hottest-spot" allowances and differentials between various methods of measurement, so that the limits of temperature rise ultimately specified by the resistance method shall be in accord with the best interests of motor users.

The test data herein presented indicate that the new limits of rise by resistance should be at least five degrees centigrade above the present thermometer ratings for low-voltage machines with class *A* insulation. However, additional data and other considerations may indicate a ten-degree-centigrade increase to be logical, or may suggest separate temperature differentials for different voltage ratings or types of motor construction.

Appendix I. Comparison of Two Totally Enclosed Fan-Cooled Motors of Different Manufacturers

Comparative heating tests were made on two 75-horsepower totally enclosed fan-cooled motors having widely different construction features. These motors which are designated as *X* and *Y* were made by two different manufacturers. Both machines had the same NEMA frame number, speed, and horsepower rating, but motor *X* had a name-plate rating of 40 degrees centigrade rise whereas motor *Y* was rated 55 degrees centigrade.

Figure 2 shows the locations of 28 thermometers and thermocouples which were placed on the winding of motor *X* through radial holes that were bored in the stator frame. One thermocouple was placed adjacent to each of the ten thermometer bulbs to obtain check readings. One of the thermometer bulbs on each end of the motor was not imbedded in putty in order to observe any difference in indication for this reason. Eight of the thermocouples were placed on inside periphery of stator coil ends directly over the rotor end rings. The values of temperature rise obtained at the different points for continuous operation at normal load are shown in table I.

By the thermometer method as defined in ASA standards 2.055 and 2.063, the temperature rise of motor *X* may be measured as anything from 27 degrees centigrade to 49 degrees centigrade depending on the number and location of points where readings are taken. The present standards do not specify the amount of exploration that shall be made to locate the hottest point accessible to thermometers. In this particular case the location and distribution of thermometers is of much greater significance than the details of how the bulbs are covered with putty or pads.

By placing thermocouples on the inside periphery of coil ends in positions not accessible to thermometers, higher values of temperature rise are obtained (53 degrees centigrade to 59 degrees centigrade).

The 51-degree-centigrade temperature rise of motor *X* by resistance method is only 2 degrees centigrade more than the maximum value of 49 degrees centigrade that could be obtained by following the ASA rules. If the testing personnel had used only two thermometers in positions 5 and 9, the temperature rise by the ASA rules would be measured as 27 degrees centigrade, or 22 degrees centigrade lower than the value of 49 degrees centigrade that might have been obtained.

Table II. Motors With Two Windings (Refer to Appendix II)

Symbol	Motor Rating
M-1	4/6/8/12 poles—60/40/30/20 horsepower—1,800/1,200/900/600 rpm—three phase—440 volts
M-2	4/6/8/12 poles—7.5/6.5/6/5 horsepower—1,800/1,200/900/600 rpm—three phase—440 volts
M-3	4/6/8/12 poles—50/33/25/17 horsepower—1,800/1,200/900/600 rpm—three phase—220 volts

	Conditions of Test			Temperature Rise—Degrees Centigrade	
Motor	Poles	Horsepower	Volts	Active Winding	Idle Winding
M-1	6	40	440	*B*...36	*A*...22
M-1	12	20	440	*B*...27	*A*...19
M-2	4	7.5	440	*A*...23	*B*...20
M-2	6	6.5	440	*B*...30	*A*...22
M-2	8	6	440	*A*...31	*B*...24
M-2	12	5	440	*B*...45	*A*...30
M-2	4	15	520	*A*...59	*B*...48
M-2	6	10	457	*B*...46	*A*...30
M-3	6	33	220	*B*...27	*A*...19

B—Indicates inaccessible underneath winding next to air gap.

A—Indicates outside winding farthest from air gap.

On motor *Y* the temperature rise by resistance of 54 degrees centigrade is 5 degrees centigrade more than that obtained under the ASA rules, but is only 3 degrees centigrade above the values measured with thermocouples on inside coil periphery at points inaccessible to thermometers. The variation in temperature measurements by all methods on motor *X* is 32 degrees centigrade (59 degrees centigrade–27 degrees centigrade) whereas the corresponding variation on motor *Y* is only 6 degrees centigrade (54 degrees centigrade—48 degrees centigrade). The better uniformity of winding temperatures on motor *Y* is a result of a different type of winding and a more effective internal ventilation system. Consequently the thermometer method of ASA standards as usually applied would give results more nearly representative of maximum insulation temperature on motor *Y* than on motor *X*.

The tests on these two motors indicate that the temperature-rise-by-resistance method is much more dependable than thermometer readings which are obtained at a few points only. In this comparison the motor rated 40 degrees centigrade by thermometer actually had higher insulation temperatures than the 55 degrees centigrade rated machine. If the resistance method of test had been specified as standard, motor *X* definitely could not have qualified as a 40-degree-centigrade machine.

Appendix II. Multispeed Motors With More Than One Winding

If a motor has more than one stator-winding, surface temperature readings which are taken on the idle winding are of questionable value. It is common practice to place the winding with largest number of poles (and consequently the shortest end connections) inside next to the air gap. Even on open motors the underneath winding usually cannot be reached with thermometers, and it is utterly impossible to reach the inside winding of an enclosed machine by inserting thermometers in holes bored through the stator frame. Because of reduced ventilation at lower speed, the inaccessible winding is likely to be the limiting feature of the motor with respect to temperature rise on constant horsepower and constant torque ratings.

Table II shows thermocouple temperatures on both active and idle windings of three different four-speed motors having two stator windings. Machines *M*-1 and *M*-3 are of open-type construction, whereas *M*-2 is totally enclosed fan-cooled. Conditions of test including winding locations and temperatures of both active and idle windings are tabulated for nine different heating tests. Although a maximum difference of only 16 degrees centigrade appears on table II, differences up to 20 degrees centigrade have been observed.

Two-winding wound rotors are not very common, but when such construction is used at least one of the windings is likely to be inaccessible on open motors, whereas both are inaccessible to thermometer method on totally enclosed motors as well as on most splashproof motors.

When the "sandwiched" type of coil is used on a two-winding stator, the inner winding is in middle of slot whereas the outer winding occupies both the top and bottom positions. This inner winding is quite inaccessible to either thermocouples or thermometers.

To avoid misleading data, the resistance method of temperature measurement is recommended for all multiwinding motors.

Appendix III. Analysis of Heating Data From Induction Motors Representing Various Types of Construction

In order to present a comprehensive analysis of the fidelity of the resistance method, a review was made of 264 heating tests made over a period of three years where temperature rises were measured by both the resistance and thermometer methods. The test data which are summarized in table III include a wide variety of motors representing different horsepower ratings, speeds, time ratings, electrical designs, and mechanical forms of construction. With the exception of a few of the wound-rotor measurements, each heating test was taken with a different motor. Approximately 70 per cent of the tests were made on motors in NEMA frame sizes from 364 to 505, inclusive. The remaining 30 per cent are for larger machines up to 1,000 horsepower. Several tests are averaged for each form of motor construction in order to minimize individual testing errors and thereby obtain a truly representative indication of results that could be anticipated by a general application of the resistance method.

All the surface temperature measurements on the stator windings of the totally enclosed and totally enclosed fan-cooled motors of table III were obtained with thermocouples which were well distributed in order to locate the hottest point. The testing procedure for the enclosed machines was carried beyond the requirements of the ASA standards, because some of the thermocouples were in locations that could not have been reached by inserting thermometers through holes in the frame. Consequently the recorded winding surface temperatures were approximately three degrees centigrade higher on the average than could have been obtained by the thermometer method.

The data on the open motors of table III were obtained by the thermometer method as defined in the present standards. In general, thermometers (or thermocouples)

Table III. Heating Data on Various Types of Induction Motors (See Appendix III)

Number Of Tests Averaged	Number of Motor Poles	Form of Mechanical Construction	Type of Motor	Temperature Measurements Taken on	§Average Length of Test in Hours	Temperature Rise by Thermometer (or Thermocouple) Degrees Centigrade		Temperature Rise of Hottest Surface by Thermometer (or Thermocouple)—Degrees Centigrade	Temperature Rise by Resistance Method—Degrees Centigrade
						Winding	Laminations		
17	2	∥Fan cooled	Squirrel cage	Stator	5.5	47.9	32.6	47.9	47.2
18	4	Fan cooled	Squirrel cage	Stator	6.0	51.5	35.3	51.5	50.2
15	6	Fan cooled	Squirrel cage	Stator	6.0	45.2	33.2	45.2	44.2
2	8	Fan cooled	Squirrel cage	Stator	7.4	52.5	36.0	52.5	49.5
4	10	Fan cooled	Squirrel cage	Stator	6.7	45.5	34.8	45.5	45.0
2	12	Fan cooled	Squirrel cage	Stator	6.0	46.5	33.0	46.5	45.5
3		Fan cooled	Squirrel cage	Stator	1.0	44.0	31.7	44.0	43.0
2		Fan cooled	Wound rotor	Stator	4.7	54.0	30.5	54.0	52.0
2		Fan cooled	Wound rotor	Stator	1.0	39.0	28.0	39.0	37.0
Total 65		Fan cooled		Stator		48.0	33.5	48.0	46.9† Average
3	4	*Totally enclosed	Squirrel cage	Stator	8.8	51.7	*40.7	51.7	52.0
3	6	Totally enclosed	Squirrel cage	Stator	7.8	41.3	*35.3	41.3	40.0
2	8	Totally enclosed	Squirrel cage	Stator	9.2	52.5	*41.0	52.5	53.5
2		Totally enclosed	Squirrel cage	Stator	0.5	36.5	*21.0	36.5	36.0
16		Totally enclosed	Wound rotor	Stator	0.56	35.7	*23.1	35.7	36.8
Total 26		Totally enclosed		Stator		39.5	27.8	39.5	40.1† Average
6	2	Splashproof	Squirrel cage	Stator	3.3	27.3	32.3	32.5	27.8
8	4	Splashproof	Squirrel cage	Stator	4.0	27.3	29.4	30.0	28.1
7	6	Splashproof	Squirrel cage	Stator	3.4	25.6	28.9	29.6	27.1
5	8	Splashproof	Squirrel cage	Stator	4.9	31.0	33.8	34.0	34.8
5	10	Splashproof	Squirrel cage	Stator	3.1	19.0	21.4	21.4	19.6
1	12	Splashproof	Squirrel cage	Stator	3.5	25.0	28.0	28.0	26.0
5		Splashproof	Squirrel cage	Stator	0.5	28.2	32.0	32.0	30.2
4		Splashproof	Wound rotor	Stator	5.4	36.0	38.3	38.8	39.5
Total 41		Splashproof		Stator		27.4	30.5	30.8	29.0† Average
25	2	Open	Squirrel cage	Stator	4.0	23.9	30.1	30.7	28.6
14	4	Open	Squirrel cage	Stator	3.7	21.9	26.9	26.9	22.7
8	6	Open	Squirrel cage	Stator	3.7	21.1	25.9	25.9	20.8
12	8	Open	Squirrel cage	Stator	3.9	26.3	28.8	30.7	27.5
8	10	Open	Squirrel cage	Stator	3.3	20.9	25.0	25.0	23.1
3	12	Open	Squirrel cage	Stator	3.0	17.3	21.0	21.0	20.0
2		Open	Squirrel cage	Stator	0.5	34.0	26.0	35.5	34.0
18		Open	Wound rotor	Stator	4.0	26.3	30.3	30.2	30.4
6		Open	Wound rotor	Stator	0.75	31.0	30.0	31.7	32.7
Total 96		Open		Stator		24.3	28.4	29.0	26.9† Average
16		*Totally enclosed	Wound rotor	Rotor	0.56	31.1	26.6	31.5	37.1
4		Splashproof	Wound rotor	Rotor	5.4	32.8	27.3	34.0	38.0
12		Open	Wound rotor	Rotor	4.0	24.0	23.1	25.2	28.9
4		Open	Wound rotor	Rotor	0.75	28.8	23.7	28.8	33.0
Total 36			Wound rotor	Rotor		28.7	25.2	29.4	34.0† Average

264—Total number of tests.

∥ Motors designated as fan-cooled are also totally enclosed.

* Motors designated as totally enclosed are not cooled by external fan. Stator lamination temperatures were measured on outside of frame on totally enclosed machines.

§All tests longer than one hour (a total of 208) were taken on continuous rated motors. The remaining 56 tests of one hour or less were taken on motors with short-time ratings.

†Averages of temperatures are weighted in accordance with number of tests taken for each condition.

were distributed over laminations and winding at ten or more points. All surface measurements on wound rotors were made with thermometers after shutdown.

Most of the data on splashproof machines were actually taken with thermocouples, but similar maximum values of temperature rise by surface measurements could have been obtained with thermometers if a sufficient number of holes had been bored in the motor frames. It will be noted that on both splashproof and open machines, the average temperature rise of laminations exceeded that of the accessible parts of windings. Consequently the hottest part of many motors will not be reached if thermometers are placed on the windings only.

Maximum observed temperatures are tabulated (regardless of whether the maximum value occurred before or after shutdown). In each case a conscientious attempt was made to find the hottest observable spots on the machine. The average deceleration period was approximately one-half minute, from time load was removed until motor was stopped. Resistance measurements were obtained from one to two minutes after motor stopped. (These time intervals were slightly greater for large, high-speed machines.)

The heating tests tabulated in table III were not all made at the normal rated loads of the various motors. Tests on machines of similar constructions but with different ratings (such as 40 degrees centigrade, 50 degrees centigrade, etc.) are averaged together. Consequently the average values of temperature rise shown in this table are not a direct indication of the margin between actual tests and the conventional name-plate ratings for the different classes of motors.

Appendix IV. Comparison of 12 Open-Type Motors of Different Manufacturers

Identical testing methods were used to determine the comparative temperature rises of 12 different low-voltage four-pole, 60-cycle motors. Eleven of these machines were general-purpose open-type 25-horsepower motors rated 40 degrees centigrade rise, but one motor (designated as *K* in table IV) was rated 50 degrees centigrade because it had some protective features. Each motor was made by a different manufacturer. Complete test results for continuous operation at rated load are given in table IV.

The lamination and conductor temperatures were obtained by surface measurements with either thermometers or thermocouples depending in each case on the accessibility of motor parts. However, when thermocouples were used, they were purposely placed in locations that could have been reached by thermometers if holes had been bored through intervening structural parts. Therefore the data in table IV were obtained in accordance with rules 2.055 and 2.063 of the present ASA standards. Only the maximum observed temperatures are recorded, but measurements were made at many different points on each machine. With the exception of motor *I*, the resistance method does not deviate from the maximum temperature rise observed by surface measurements by more than 3.5 degrees centigrade, and on eight of the motors the agreement is within 2 degrees centigrade.

The tests on these 12 motors were made successively by the same personnel, and the same dynamometer and metering equipment were used throughout. Therefore most of those controversial points were eliminated which usually arise when an attempt is made to compare heating characteristics of different motors. Since the motors represent a wide range of design proportions and ventilation arrangements, the consistency of the temperature data obtained by the resistance method is highly significant.

Five of the 12 motors had a maximum observable temperature rise by thermometer that was higher than the name-plate rating. These tests indicate that comparable results are not being obtained by different manufactures under the present standards—presumably because of differences in the extent of exploration with thermometers.

Table IV. Comparison of Open Motors of Different Manufacturers (Refer to Appendix IV)

Motor	Name Plate Rating—Degrees Centigrade	Test Values of Temperature Rise—Degrees Centigrade: Laminations	Conductors	Maximum Rise by Surface Measurement	By Resistance
A	40	41	38	41	41
B	40	42	34	42	41
C	40	34	37	37	37
D	40	28	31	31	27.5
E	40	24.5	25.5	25.5	23.5
F	40	34.5	28.5	34.5	32
G	40	33	32	33	34
H	40	44	37	44	45
I	40	47	34	47	41
J	40	32.5	30.5	32.5	30.5
**K*	50	59	61	61	62
L	40	29	24	29	27.5
Averages		37.4	34.4	38.1	36.8

* Motor *K* had some protective features.

Table V. Continuous Heat Runs for Various Loads and Voltages (See Appendix V and Figure 4)

(Motor Name-Plate Rating—Four Poles, 25 Horsepower, 220 Volts, Three Phase, 60 Cycles)

Voltage	Horsepower Load	Watts Input	Amperes	Total Motor Losses (Watts)	Temperature Rise—Degrees Centigrade: Winding Thermometer	Laminations Thermometer	Embedded Thermocouple	By Resistance
220	0	966	24.6	966	9	14	16	11
254	0	1,410	37	1,410	16	22	21	17
187	0	740	19.4	740	6	10	10	8
220	25.9	21,900	66.5	2,591	25	33	38	32
254	26.4	22,250	64.5	2,582	27	35	38	34
187	25.8	22,500	78.5	3,308	36	42	47	42
220	32.3	27,800	83	3,729	37	47	53	50
254	32	27,200	76	3,285	32	42	46	43
187	31.5	29,700	107	6,190	66	79	95	84
220	38.0	34,000	103	5,636	58	69	83	72
220	41.9	39,200	120	7,976	91	101	121	108

Appendix V. Temperature Measurements for Various Conditions of Motor Load and Voltage

An open-type 25-horsepower motor rated 220 volts at 1,800 rpm was selected for a series of 11 heat runs covering a wide range of conditions from zero to 168 per cent of normal load and from 85 to 115 per cent of rated voltage. Temperatures were measured by thermocouples which were inserted in internal parts of stator slots when the random winding was installed, by thermometers on windings and laminations, and by resistance method as tabulated in table V for continuous heat runs at each condition indicated. The laminations and windings were readily accessible, and thermometer readings were taken according to the ASA rules.

For operation at rated voltage, the resistance method indicated one degree centigrade to three degrees centigrade lower temperature than thermometers at normal load or less, but for extreme overloads the temperature is higher by resistance, the maximum difference being seven degrees centigrade at 168 per cent of rated load.

Thermocouples were distributed through the stator slots in order to locate the hottest internal point in the motor, and the maximum temperatures observed are designated as "embedded thermocouple" in table V and figure 4. In this random winding the embedded thermocouples are in close contact with the conductors, and are not separated from them by coil insulation as in the case where embedded-detectors are placed between two formed coils in the slot of a high-voltage motor. Therefore these embedded thermocouples indicate closely the theoretical "hottest spot" of the winding which was located at the middle of lamination stack. The "hottest spot" averaged five degrees centigrade higher than the temperature by resistance for the heat runs taken near normal load.

Figure 4 shows that the temperature rises by all methods are approximately proportional to the total motor losses. In general, increasing differentials are obtained between the temperature measurements by different methods as the motor losses are increased by overloading the machine.

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Discussion

For discussion, see page 472.

Measurement of Temperature in General-Purpose Squirrel-Cage Induction Motors

C. P. POTTER
FELLOW AIEE

THERE are three fundamental methods of temperature determination[1] which might be applied to squirrel-cage motors, namely:

1. The thermometer method
2. The resistance method
3. The embedded-detector method

It is the purpose of this paper to report results obtained by the three methods and to discuss the practical aspects of temperature measurement.

The standards of the American Standards Association,[2] AIEE,[3] and National Electrical Manufacturers Association[4] specify that the temperature rise of induction machines shall be determined by the thermometer method. Section 50 of the Test Code for Polyphase Induction Machines[5] is more general and reads as follows:

> Temperature tests are taken primarily to determine the amount of temperature rise on the different parts of the machine while running under a specified load. This rise in temperature is measured by either the rise in resistance of the current-carrying part, or by means of a thermometer, thermocouple, or embedded temperature detector. It is sometimes desirable to use one method as a check on the other.
>
> In measuring temperature rise by resistance, care must be taken to observe the precautions set forth in paragraphs 10 to 16 in order to secure accurate results, since a small error in measuring resistance may cause a comparatively large error in determining the temperature.
>
> The usual method of measuring temperature of open machines is by the use of alcohol or mercury thermometers or thermocouples, applied to the hottest part of the motor that is accessible. In the case of totally enclosed machines, due to the mechanical difficulties attending the use of alcohol or mercury thermometers, both during the run and after shutdown, thermocouples may be more convenient. . .

The test code specifies that thermometers or thermocouples shall be applied to the hottest part of the motor that is accessible. Definition 2.055 of the American Standards for Rotating Electrical Machinery[2] is more specific and reads as follows:

> *Thermometer Method of Temperature Determination Defined.* This method consists in the determination of the temperature, by mercury or alcohol thermometers, by resistance thermometers, or by thermocouples, any of these instruments being applied to the hottest part of the machine *accessible to mercury or alcohol thermometers.*

Thermometers Versus Thermocouples

It would seem that if proper precautions are observed in placing the instruments, the same results should be obtained with either thermometers or thermocouples. In order to check this assumption, several hundred temperature test records of squirrel-cage motors in sizes from 1 to 100 horsepower were examined. The temperature tests made before 1936 employed thermometers, and the results were checked by the resistance method. Tests made during the last two years employed thermocouples and the results were again checked by resistance measurements. It therefore seemed practical to obtain a comparison between thermometers and thermocouples by using the resistance measurements as a reference. The results of this comparison were disappointing. It was possible to arrive at the general conclusion that the temperature rise measured by thermocouple is higher than that measured by thermometer, but there was too much variation to justify establishing a differential which could be used for all conditions.

There are several explanations for the discrepancies. In the first place, the resistance measurements were only used as a check and it is likely that not enough emphasis was placed on the precautions which must be observed when using this method. The instruments were commercially accurate and readings were taken carefully, but there was probably not enough attention paid to the following paragraph (11) of the test code:[5]

> Every possible precaution should be taken to obtain the true temperature of the winding when measuring the cold resistance. The temperature of the surrounding air must not be regarded as the temperature of the windings unless the motor has been standing idle under constant temperature conditions for a considerable period of time.

It was also thought that some of the discrepancies in temperature tests might be due to inherent differences between thermometers and thermocouples.

Thermometers are calibrated while immersed in liquid to a specified depth, and it goes without saying that the liquid makes intimate contact with all parts of the bulb and part of the stem. Furthermore, thermometers are inflexible and

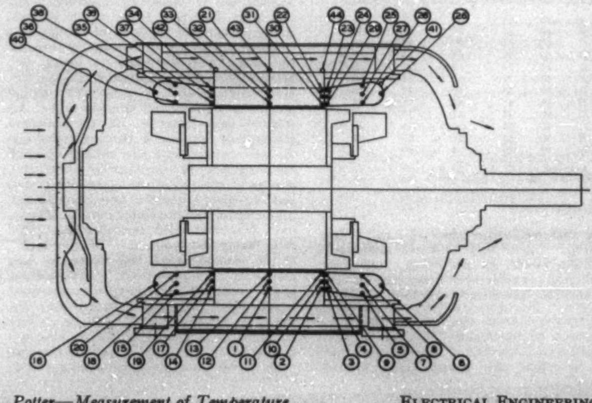

Figure 1. Location of thermocouples in totally enclosed fan-cooled motor

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Paper number 39-5, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted October 31, 1938; made available for preprinting November 28, 1938.

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[1] For all numbered references, see list at end of paper.

due to their shape, can touch a plane surface, or most curved surfaces, at only two points. The question might therefore be raised whether a thermometer gives a true indication of the temperature of a winding, even when the bulb is covered with putty. On the other hand, a thermocouple is made of small, flexible wires and the junction can be put in a small opening and can be made to have intimate contact with a winding. Check tests made on cylindrical coils showed that thermocouples read higher than thermometers, the difference depending upon the curvature and smoothness of the coil surface, and the care used in making the application.

When a thermometer is used for measuring the temperature of a winding, at least a part of its stem is usually exposed to the cooling air and it was thought that this might affect the readings. This was checked by immersing a thermometer bulb in boiling water and directing the air from a desk fan on the entire stem. It was found that the reading was not affected as long as any appreciable amount of the bulb was immersed. With only the tip of the bulb in the water, the application of the fan reduced the reading three per cent.

In order further to investigate the subject, it was decided to build two 40-horsepower motors and to make careful temperature tests on these motors, by resistance, thermocouple, and thermometer, and to place enough thermocouples in the windings to locate surely, and measure the temperature of the hot spot. It was decided to build one motor of the totally enclosed fan-cooled type, and to test this motor with two rotors, one with heavy load losses and the other with relatively small load losses. It was decided to make the other motor an open-type machine and to test this motor with one rotor having small load losses.

Location of Hot Spot

Careful consideration was given to the location of the hot-spot thermocouples and useful information was obtained from a series of papers[6–11] presented at the midwinter convention of 1913. From these papers the conclusion was reached that the location and magnitude of the hot spot in the stator winding of a squirrel-cage motor depend upon its

1. Physical characteristics
2. Electrical and magnetic characteristics
3. Ventilation
4. Loading

A motor which is symmetrical about its vertical center line usually has a blower on each side and both free ends of the stator winding should be equally cooled. The parts of the coils which are embedded in the core are not so well ventilated and it seems likely that the highest coil temperature will be reached in the axial center of the core. If the design is unsymmetrical especially in case there is a blower on only one side of the motor, the free ends of the winding which are on the same side as the blower will be cooler than those on the opposite side, and the hot spot will probably shift away from the blower. The location of the hot spot vertically in the slot is not so easy to estimate, and will depend to a great extent on the direction of the heat flow between the winding and core. The direction of heat flow will depend on which member is producing the greater amount of heat, which, for a given load, will vary with the electrical design, and for a given design will vary with the loading. With light loads the copper losses will be small and heat may flow from the core iron to the winding. At a certain heavier load there may be little interchange of heat between the core iron and the winding, and at still heavier loads, the heat may flow from the winding to the core. The winding near the mouth of the slot will not be as closely associated with the core iron as the rest of the winding, but on the other hand, will be subjected to the heat produced in the rotor, which, in a poorly designed motor, may be excessive.

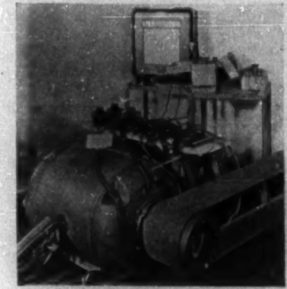
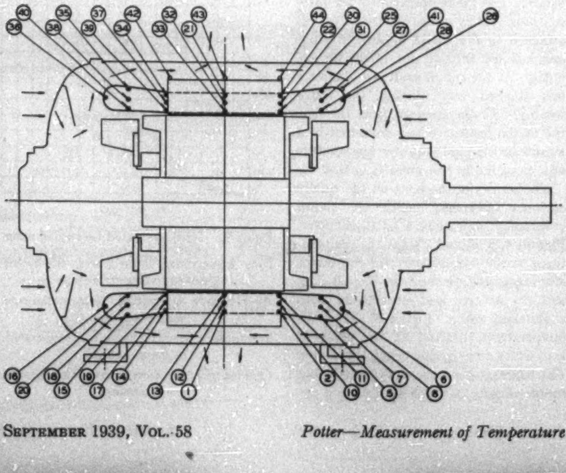


Figure 3. Equipment used for testing totally enclosed fan-cooled motor

Figure 2. Location of thermocouples in open-type motor



In order to be reasonably sure of finding the hot spot, 44 thermocouples are installed in the totally enclosed fan-cooled motor as shown in figure 1. A thermometer extends through a slot in the end plate and the bulb of the thermometer is immediately adjacent to thermocouple number 41. There are 38 thermocouples in the open motor as shown in figure 2, and again the bulb of the thermometer is immediately adjacent to thermocouple number 41. Thermocouple number 41 and the thermometer bulb are on the outside of the tape which covers the free ends of the windings and are covered with a small amount of sealing compound. Thermocouples number 42, number 43, and number 44 are located on the stator iron and are also covered with a small amount of sealing compound.

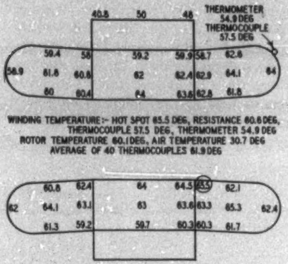

40.8 50 48
THERMOMETER 54.9 DEG
THERMOCOUPLE 57.5 DEG
59.4 58 59.2 59.9 58.7 62.6
58.9 61.8 60.8 62 62.4 62.9 64.1 64
60 60.4 64 63.6 62.8 61.8
WINDING TEMPERATURE:- HOT SPOT 65.5 DEG, RESISTANCE 60.8 DEG, THERMOCOUPLE 57.5 DEG, THERMOMETER 54.9 DEG
ROTOR TEMPERATURE 60.1 DEG, AIR TEMPERATURE 30.7 DEG
AVERAGE OF 40 THERMOCOUPLES 61.9 DEG
60.8 62.4 64 64.5 65.5 62.1
62 64.1 63.1 63 63.6 63.3 65.3 62.4
61.3 59.2 59.7 60.3 60.3 61.7

Figure 4. Totally enclosed fan-cooled motor at 75 per cent full load

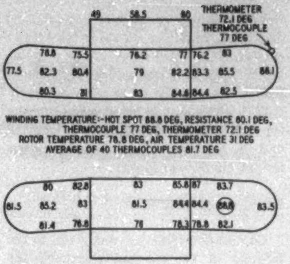

49 58.5 60
THERMOMETER 72.1 DEG
THERMOCOUPLE 77 DEG
78.8 75.5 76.2 77 76.2 83
77.5 82.3 80.4 79 82.2 83.3 85.5 88.1
80.3 81 83 84.8 84.4 82.5
WINDING TEMPERATURE:-HOT SPOT 88.8 DEG, RESISTANCE 80.1 DEG, THERMOCOUPLE 77 DEG, THERMOMETER 72.1 DEG
ROTOR TEMPERATURE 78.8 DEG, AIR TEMPERATURE 31 DEG
AVERAGE OF 40 THERMOCOUPLES 81.7 DEG
80 82.8 83 85.8 87 83.7
81.5 85.2 83 81.5 84.4 84.4 88.8 83.5
81.4 76.8 76 78.3 78.8 82.1

Figure 5. Totally enclosed fan-cooled motor at full load

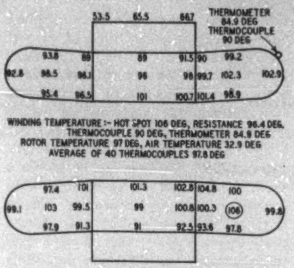

53.5 65.5 66.7
THERMOMETER 84.9 DEG
THERMOCOUPLE 90 DEG
93.8 89 89 91.5 90 99.2
92.8 98.5 96.1 96 98 99.7 102.3 102.9
95.4 96.5 101 100.7 101.4 98.9
WINDING TEMPERATURE:- HOT SPOT 106 DEG, RESISTANCE 96.4 DEG, THERMOCOUPLE 90 DEG, THERMOMETER 84.9 DEG
ROTOR TEMPERATURE 97 DEG, AIR TEMPERATURE 32.9 DEG
AVERAGE OF 40 THERMOCOUPLES 97.8 DEG
97.4 101 101.3 102.8 104.8 100
98.1 103 99.5 99 100.8 100.3 106 99.8
97.9 91.3 91 92.5 93.6 97.8

Figure 6. Totally enclosed fan-cooled motor at 115 per cent full load

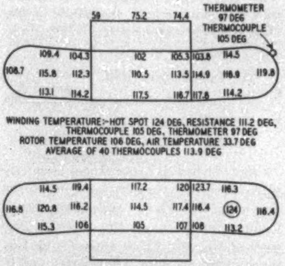

59 75.2 74.4
THERMOMETER 97 DEG
THERMOCOUPLE 105 DEG
109.4 104.3 102 105.3 103.8 114.5
108.7 115.8 112.3 110.5 113.5 114.9 118.9 119.8
113.1 114.2 117.5 116.7 117.8 114.2
WINDING TEMPERATURE:-HOT SPOT 124 DEG, RESISTANCE 111.2 DEG, THERMOCOUPLE 105 DEG, THERMOMETER 97 DEG
ROTOR TEMPERATURE 106 DEG, AIR TEMPERATURE 33.7 DEG
AVERAGE OF 40 THERMOCOUPLES 113.9 DEG
114.5 119.4 117.2 120 123.7 116.3
116.8 120.8 116.2 114.5 117.4 116.4 124 116.4
115.3 106 105 107 108 113.2

Figure 7. Totally enclosed fan-cooled motor at 125 per cent full load

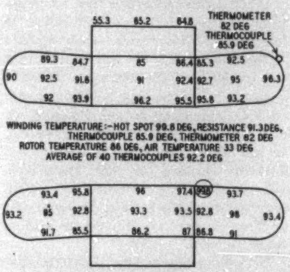

55.3 65.2 64.8
THERMOMETER 82 DEG
THERMOCOUPLE 85.9 DEG
89.3 84.7 85 86.4 85.3 92.5
90 92.5 91.6 91 92.4 92.7 95 96.3
92 93.9 96.2 95.5 95.8 93.2
WINDING TEMPERATURE:-HOT SPOT 99.8 DEG, RESISTANCE 91.3 DEG, THERMOCOUPLE 85.9 DEG, THERMOMETER 82 DEG
ROTOR TEMPERATURE 86 DEG, AIR TEMPERATURE 33 DEG
AVERAGE OF 40 THERMOCOUPLES 92.2 DEG
93.4 95.8 96 97.4 99.8 93.7
93.2 95 92.8 93.3 93.5 92.8 98 93.4
91.7 85.5 86.2 87 86.8 91

Figure 8. Totally enclosed fan-cooled motor at full load (heavy load losses)

Thermocouples numbers 11, 13, 31 and 33 are silk taped to the top of a top coil side, next to the 0.025-inch insulation separating the winding from the one-eighth inch wooden wedge. Thermocouples numbers 10, 12, 30 and 32 are silk taped to the top of a bottom coil side, just below the 0.060-inch insulation separating the coil sides. Thermocouples numbers 1, 2, 21, and 22 are silk taped to the bottom of a bottom coil side, next to the 0.025-inch slot cell. (A top coil side is the one nearer the air gap; a bottom coil side is the one nearer the outside of the motor.) Thermocouples numbers 8, 9, 19, 20, 28, 29, 39, and 40 have radial positions corresponding to number 11, etc., thermocouples numbers, 4, 7, 17, 18, 24, 27, 37, and 38 have radial positions corresponding to number 4, etc., and thermocouples numbers 3, 5, 14, 15, 23, 25, 34, and 35 have radial positions corresponding to number 1, etc., but are on the free ends of the windings just inside the tape which covers the free ends. Thermocouples numbers 6, 16, 26, and 36 are silk taped to the loops of the coils, just inside the tape. Figures 1 and 2 are drawn approximately to scale and show the relative dimensions of the various parts as well as the location of the thermocouples. Both stators are wound to the same specification, of double-cotton-covered wire, with double-layer diamond windings in semienclosed slots. The stators have only one dipped and baked coat of varnish, additional coats having been omitted to save time. There are no ventilating ducts in either the stator or rotor and no openings through the rotor. The squirrel cages are of cast aluminum construction.

Apparatus and Test Results

The arrangement of the apparatus is shown in figure 3. The thermocouple leads are enclosed in spaghetti tubing and are divided into four groups and brought out of the motor through the air-gap plug holes. The leads are connected to terminal boards and may be connected either to an indicating or recording instrument by means of dial switches. A double bridge was used to measure the resistance of the stator winding, and the temperature of the rotor was measured by means of a thermocouple attached to the tip of an air-gap gauge and inserted through one of the air-gap holes. At the end of each run, the rotor was stopped very quickly, the stator winding was disconnected from the line, the stator resistance was measured in a minute or less, and the rotor temperature was measured in two minutes or less.

The results of the tests on the totally enclosed fan-cooled motor are shown graphically in figures 4 to 9, inclusive. Figures 4, 5, 6, and 7 show the temperatures in the various parts of the motor after becoming constant, at 75, 100, 115, and 125 per cent load respectively, with a standard rotor. Figure 8 shows the temperatures attained at 100 per cent load with a rotor having heavy load losses. The figures in the large circles are the hot spots. Figure 9 shows the relation between hot-spot temperatures and temperatures by resistance, thermocouples, and thermometers for the various tests illustrated in figures 4, 5, 6, and 7. It is believed that the diagrams and curves

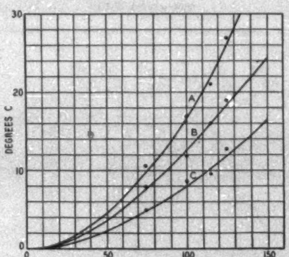


Figure 9. Totally enclosed fan-cooled motor

Forty horsepower, three phase, 60 cycles, 220/440 volts, 1,750 rpm

A—Hot-spot temperature minus temperature by thermometer

B—Hot-spot temperature minus temperature by thermocouple

C—Hot-spot temperature minus temperature by resistance

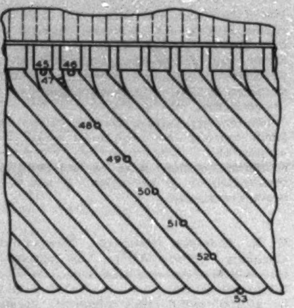

45
46
47
48
49
50
51
52
53

Figure 10. Temperature in coil extension of open-type 40-horsepower motor at full load

Nine Thermocouples

Number	45	46	47	48	49	50	51	52	53
Deg C	46.6	44.3	44.3	53.0	53.1	54.1	54.1	42.8	44.1

are self-explanatory, and the test results seem to be consistent with each other. It should be noted that the cooling fan is on the left side and the hot spot is on the right side of the fan-cooled motors. One point of particular interest is the close agreement between the average of the temperatures obtained from thermocouples numbers 1–40, inclusive, and the temperature by resistance, indicating that the thermocouples are located judiciously, and that the readings are accurate.

Similar tests were made on the 40-horsepower open-type motor and the results were not what had been anticipated. The open-type motor is supposedly symmetrical about both center lines and it is reasonable to expect that the temperatures of corresponding parts would be identical. However, the coil extensions on the pulley end are considerably hotter than on the opposite end. An examination of the motor showed that the windings are not symmetrical but are considerably shorter on the pulley end, which probably accounts for the difference in temperature. The readings of the thermometer and thermocouple number 41 were much lower than expected and very much lower than the temperature by resistance and hot-spot temperature. This made it seem probable that they were not located in the right place and the motor was inspected with this idea in mind. It was found that the thermometer was inserted through a hole drilled in the frame with the thermocouple immediately adjacent to the thermometer bulb, and while the location of the thermometer was not ideal, it did not seem to be far away from the proper location. In order to find the hottest external part of the coil extension, nine more thermocouples were placed on the surface of the winding as shown in figure 10. It will be noted that thermocouples number 50 and 51 give results about ten degrees higher than number 52 and 53. Temperature tests were then made at 100 per cent, 125 per cent, 144 per cent, and 160 per cent of full load using thermocouple number 51 and a thermometer whose bulb is immediately over this thermocouple. The results of these tests are shown graphically in figures 11 to 15 inclusive and are self-explanatory. The averages of thermocouples number 1–40 inclusive do not check the temperature by resistance, but fair agreement is obtained by eliminating numbers 6, 8, 16, 20, 26, 28, 36, and 40, which are located directly in the path of the cooling air. Figure 15 shows the relation between hot-spot temperatures and temperatures by resistance, thermocouples, and thermometers for the various tests illustrated in figures 11–14 inclusive.

Conclusions

A revision of the standards which specify the method of measuring the temperature rise of induction machines is desirable and should be undertaken. The determination of temperature rise should not depend on surface measurements taken either by thermometer or thermocouple, because it is often difficult, and sometimes impossible to locate these instruments properly. Even though a machine is so constructed that there is no interference with the installation of thermometers, it is not good practice to leave the location of thermometers to the judgment of even the most careful and conscientious testers, because they ordinarily have no way of determining the hottest accessible spot. Elimination of thermometers and thermocouples will leave only the resistance method for machines of ordinary size and voltage, and while this method requires skillful technique on the part of the tester, it has been used in transformer heat runs for many years and is strictly a commercial method.

If the resistance method is adopted, it will be necessary to revise the conventional allowances[1] for hot spot and to increase the standard temperature ratings for induction machines about five degrees

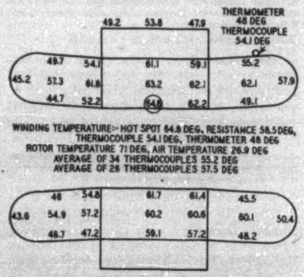

THERMOMETER 48 DEG
THERMOCOUPLE 54.1 DEG
49.2
53.8
47.9
49.7
54.1
61.1
59.1
55.2
45.2
57.3
61.8
63.2
62.1
62.1
57.9
44.7
52.2
64.8
62.2
49.1
WINDING TEMPERATURE:- HOT SPOT 64.8 DEG, RESISTANCE 58.5 DEG,
THERMOCOUPLE 54.1 DEG, THERMOMETER 48 DEG
ROTOR TEMPERATURE 71 DEG, AIR TEMPERATURE 26.9 DEG
AVERAGE OF 34 THERMOCOUPLES 55.2 DEG
AVERAGE OF 28 THERMOCOUPLES 57.5 DEG
46
54.8
61.7
61.4
45.5
43.6
54.9
57.2
60.2
60.6
60.1
50.4
48.7
47.2
59.1
57.2
48.2

Figure 11. Open-type motor at full load

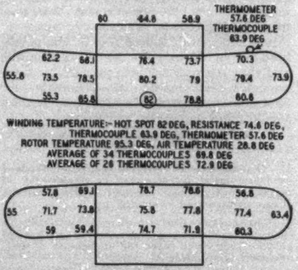

THERMOMETER 57.6 DEG
THERMOCOUPLE 63.9 DEG
60
44.8
58.9
62.2
66.1
76.4
73.7
70.3
55.8
73.5
78.5
80.2
79
79.4
73.9
55.3
65.8
82
78.8
60.8
WINDING TEMPERATURE:- HOT SPOT 82 DEG, RESISTANCE 74.6 DEG,
THERMOCOUPLE 63.9 DEG, THERMOMETER 57.6 DEG
ROTOR TEMPERATURE 95.3 DEG, AIR TEMPERATURE 28.8 DEG
AVERAGE OF 34 THERMOCOUPLES 69.8 DEG
AVERAGE OF 26 THERMOCOUPLES 72.9 DEG
57.8
69.1
78.7
78.6
56.8
55
71.7
73.8
75.8
77.8
77.4
63.4
59
59.4
74.7
71.9
60.3

Figure 12. Open-type motor at 125 per cent full load

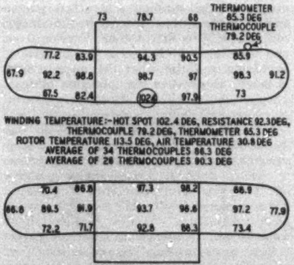

THERMOMETER 65.3 DEG
THERMOCOUPLE 79.2 DEG
73
78.7
68
77.2
83.9
94.3
90.5
85.9
67.9
92.2
98.8
98.7
97
98.3
91.2
67.5
82.4
102.4
97.9
73
WINDING TEMPERATURE:- HOT SPOT 102.4 DEG, RESISTANCE 92.3 DEG,
THERMOCOUPLE 79.2 DEG, THERMOMETER 65.3 DEG
ROTOR TEMPERATURE 113.5 DEG, AIR TEMPERATURE 30.8 DEG
AVERAGE OF 34 THERMOCOUPLES 86.3 DEG
AVERAGE OF 28 THERMOCOUPLES 90.3 DEG
70.4
86.8
97.3
98.2
88.9
66.8
89.5
91.9
93.7
96.6
97.2
77.9
72.2
71.7
92.8
88.3
73.4

Figure 13. Open-type motor at 144 per cent full load

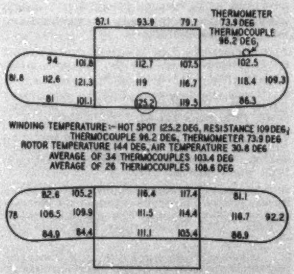

THERMOMETER 73.9 DEG
THERMOCOUPLE 96.2 DEG
87.1
93.9
79.7
94
101.8
112.7
107.5
102.5
81.8
112.6
121.3
119
116.7
118.4
109.3
81
101.1
125.2
119.5
86.3
WINDING TEMPERATURE:- HOT SPOT 125.2 DEG, RESISTANCE 109 DEG,
THERMOCOUPLE 96.2 DEG, THERMOMETER 73.9 DEG
ROTOR TEMPERATURE 144 DEG, AIR TEMPERATURE 30.8 DEG
AVERAGE OF 34 THERMOCOUPLES 103.4 DEG
AVERAGE OF 26 THERMOCOUPLES 108.6 DEG
82.6
105.2
116.4
117.4
81.1
78
106.5
109.9
111.5
114.4
116.7
92.2
84.9
84.4
111.1
105.4
86.9

Figure 14. Open-type motor at 160 per cent full load

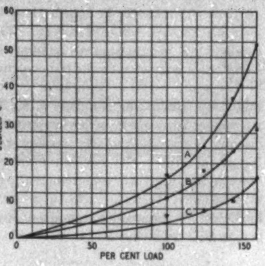


Figure 15. Open-type motor

Forty horsepower, three phase, 60 cycles, 220/440 volts, 1,750 rpm

A—Hot-spot temperature minus temperature by thermometer

B—Hot-spot temperature minus temperature by thermocouple

C—Hot-spot temperature minus temperature by resistance

centigrade. Such an increase would result in the following ratings:

Open-type continuous ratings 45 degrees centigrade

Protected and intermittent ratings 55 degrees centigrade

Totally enclosed continuous ratings 60 degrees centigrade

References

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3. Induction Motors and Induction Machines in General, AIEE Standards No. 9, June 1927, pages 9–11.

4. NEMA Motor and Generator Standards, publication number 38–49, May 1938, page 35, note III; page 51, note II; page 58, note II.

5. Test Code for Polyphase Induction Machines, AIEE Standards No. 500, August 1937, pages 12–14.

6. High Speed Turboalternators—Designs and Limitations, B. G. Lamme. AIEE Transactions, 1913, pages 19–25.

7. Temperature and Electrical Insulation, C. P. Steinmetz and B. G. Lamme. AIEE Transactions, 1913, pages 83–9.

8. Notes on Internal Heating of Stator Coils, R. B. Williamson. AIEE Transactions, 1913, pages 153–62.

9. Measurement of Temperature in Rotating Electric Machines, L. W. Chubb, E. I. Chute, and O. W. A. Oelting. AIEE Transactions, 1913, pages 163–75.

10. Method of Determining Temperature of Alternating Current Generators and Motors and Room Temperature, Henry G. Reist and T. S. Eden. AIEE Transactions, 1913, pages 177–84.

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Discussion

P. H. Rutherford (General Motors Corporation, Dayton, Ohio): The results shown in the paper by C. P. Potter agree very well with the results of similar tests carried out on small single-phase motors. Extensive tests carried out on a one-horsepower single-phase motor showed that the hot spot occurred in the center of the end turns of the winding and did not appear to shift with variations in load. This hot-spot temperature was approximately ten per cent greater than the temperature rise by resistance, a result which checks quite well with data obtained from much larger motors. If the resistance method proves satisfactory, it might be well to consider the allowances for hot spot as a per cent of the temperature rise by resistance rather than a constant allowance of about 5 degrees centigrade. This would serve to extend temperature standards to motors of ratings higher than 60 degrees centigrade in case such designs should prove desirable in the future.

It would have been interesting to consider the curves in this paper plotted against motor losses as well as per cent of full load. Since one motor was tested with a standard rotor and a rotor with heavy load losses, it would seem that for the same losses the temperature rise and possibly the hot-spot temperatures would agree quite well. A comparison of the temperature versus losses curves for the open and the totally enclosed type motors would also be very interesting.

P. L. Alger (General Electric Company, Schenectady, N. Y.): The discussion has brought out a possible difficulty in obtaining the correct temperature rise of a motor by the resistance method, due to the time delay between the instant of shutdown and the time the measurement is taken. This same problem occurs in other types of apparatus, for which the resistance method of measurement is generally used. I should, therefore, like to call attention to paragraph 10.292 of appendix I of the proposed ASA standards for transformers, which gives a simplified method of determining this correction by calculation. This method is to apply an empirical correction to the degrees centigrade as measured by the resistance method. The correction is equal to the product of the watts loss per pound of copper in the winding under test (determined by the current density in the copper) multiplied by a factor that depends upon the time elapsed between the instant of shutdown and the time the measurement is taken, as given in the following table:

Time in Minutes	Factor
1	0.19
1½	0.26
2	0.32
3	0.43
4	0.50

The factors given in this table apply to oil-insulated windings, and are representative of the normal rate of heat flow from the copper into the oil. It seems clear, however, that similar factors can be derived for motor windings or other apparatus, that would be quite accurate enough for test purposes, in view of the small magnitude of the correction.

For example, with a current density of 3,000 amperes per square inch in the copper, and a temperature of 75 degrees centigrade, a time delay of two minutes corresponds to a correction of only 2.4 degrees centigrade.

G. R. Anderson (Fairbanks, Morse and Company, Beloit, Wis.): Any proposal for revision of existing standards naturally brings out data and experience that should be utilized to the best advantage in its consideration.

The various papers presented have outlined fully the many factors that must be considered, the variations to be expected, and the weight to be given to each.

Referring to the recommendations in C. P. Potter's paper, the writer has taken the opportunity of making an analysis of approximately 100 tests on various sizes of enclosed fan-cooled motors ranging from 1 to 100 horsepower. The list selected contained only ratings designed for general-purpose application. Those with special characteristics or having large rotor load losses were eliminated. Each motor had been tested for shutdown winding temperatures by thermometer and by resistance. Averaging these tests gave the following:

Average increase of temperature rise by resistance measurement over thermometer measurement was 6.9 degrees centigrade.

Average increase in percentage was 19.2.

Another analysis of the same tests was made by selecting only 40 ratings that had temperature rise by either resistance or thermometer between the range of 40 degrees centigrade and 60 degrees centigrade. The averages of this group gave the following:

Average increase of temperature rise by resistance measurement over thermometer measurement was 8.2 degrees centigrade.

Average increase in percentage was 17.4.

It will be noted that these percentages compare favorably with those recorded by Mr. Potter, which averaged 22 per cent on the enclosed fan-cooled motor tested by him.

On the basis of these data it would appear reasonable to recommend that where present standards by thermometer measurement are now 40 degrees rise, that permissible equivalent resistance measurements should fall somewhere in the neighborhood of 47 and 48 degrees centigrade, and that where maximum permissible thermometer temperature measurements are 55 degrees centigrade, the resistance method should allow 65 degrees centigrade.

In addition to the above it is recommended

1. That the present 40-degree-centigrade standard by thermometer measurement be retained for all machines having modifications to which this type of measurement can be applied.
2. That resistance measurement be permitted for enclosed machines and those having mechanical modifications which make the application of thermometers impractical.
3. That the resistance measurement be the only method adopted as standard for field-coil temperature measurement.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): In the course of the application of thermal protective devices the writer had occasion to take numerous temperature runs on a line of fractional-horsepower motors at high degrees of overload, causing high temperature rises. Temperatures were measured by a thermometer placed on the "hottest accessible" part of the end winding, by a thermocouple installed on the end winding (*not an embedded detector*), and by rise of resistance. The results are tabulated in table I of this discussion.

Table I

Horsepower Rating	Per Cent Load	Open or Enclosed	Shutdown Temperature Rises by Thermometer	Thermocouple	Resistance
$^1/_6$	200	Open	61	83	77.5
$^1/_6$	160	Enclosed	76	89	79.2
$^1/_4$	190	Open	66	82	83.3
$^1/_4$	140	Enclosed	78	88.5	83.5
$^1/_3$	180	Open	66	86	85.4
$^1/_3$	130	Enclosed	70.5	89	74.5
$^1/_2$	180	Open	58.5	79.5	71.5
$^1/_2$	130	Enclosed	75.8	84.8	82.7
$^3/_4$	160	Open	87	100	100
$^3/_4$	130	Enclosed	89	97	81.5

As a result of these and other tests, the writer draws the following conclusions as applying to fractional-horsepower motors:

1. Thermocouples should be recognized form of temperature measurement. It is frequently the practice of builders of motor-driven appliances to run application tests on their machines. In many cases, thermocouples are practically the only way of measuring the temperatures attained by the windings because the motor itself is not readily accessible and sometimes control circuits are so interlocked that it is not easy or even possible to measure the rise by resistance. Refrigeration and air-conditioning appliance manufacturers necessarily use thermocouples for the temperature measurements made to determine the thermodynamic performance of the whole apparatus; thus thermocouples afford the simplest and easiest means for measuring the motor temperature.

2. The limiting temperatures, when measured by a thermocouple installed on the "hottest accessible spot of the windings," should be the same as the limiting temperatures for the rise-of-resistance method, instead of the same as by thermometer as now. This recommendation is conservative because the thermocouple temperatures are generally higher than those obtained by rise of resistance.

3. The rise-of-resistance method is not reliable for measuring the temperature of an auxiliary winding if there is a starting switch in the circuit because of variations in contact resistance. Very serious errors have been observed because of switch contact resistance.

4. In view of the fact that, with thermal overload devices, some very high temperature rises are often measured, it might be better to express the correction for the different methods of temperature rise as a per cent of the measured rise, instead of using a flat correction in degrees as now. Potter's figures 9 and 15 support this conclusion.

A. S. Hill (University of Maine, Orono): My experience with the thermometer method in research work on fully enclosed fan-cooled induction motors supports Mr. Summers' conclusion that reliable winding temperature data cannot in general be obtained on such machines by inserting thermometers through holes in frames or covers at the close of heat runs in accordance with ASA rule 2.063. In my first series of experiments on motor ventilation, back in 1932, an attempt was made to determine ultimate insulation temperatures in this way; but, despite the fact that the thermometers were always applied by the same personnel under conditions presumably more conducive to accuracy than those usually encountered in field tests, results were so inconsistent that the procedure was soon abandoned in favor of resistance measurements.

The impossibility of properly covering bulbs with pads or putty when, on shutdown, thermometers must be quickly applied through small openings, is in itself a sufficient justification for a revision of the standards relating to temperature measurements on partially enclosed and totally enclosed machines. Thermometer observations of stator core temperatures, in the fan-cooled motor investigation just mentioned, indicate that even when the bulbs are immersed in oil in small wells drilled in the core structure to receive them, and are as a consequence afforded a fairly close contact with the laminations, and almost complete protection from the influence of air streams, the readings are likely to be from four to eight degrees too low unless the annular openings around the stems at the top of the bulbs are completely closed with cones of putty. In commercial testing under existing standards, where thermometers have to be pushed in through holes at the conclusion of a run, the "point contact," justly criticized by Mr. Summers, is about all that can be expected; and, although at standstill the bulbs are not subject to cooling by forced convection, the large percentage of bulb area exposed to the surrounding air is certain to have a marked but indeterminable effect in lowering the reading. Such observations, though possibly of interest in comparing the performance of different designs, are obviously inadequate as a basis for rating machinery.

Both Mr. Potter and Mr. Summers very properly emphasize the great importance of an accurate determination of the winding temperature corresponding to "cold resistance." This correlation is unquestionably the most vital step in the successful application of the resistance method as a means of securing dependable heating data on machinery insulation, particularly in the case of motors of the enclosed type. For as the extent of enclosure is increased, not only are the windings less accessible to thermometers, but much more sluggish in cooling after load tests or in following changes in ambient temperature. With a completely closed structure many hours may elapse after any thermal disturbance before the copper reaches the exact temperature of parts, such as laminations, bearing brackets, or shields, to which thermometers can be conveniently applied. In an endeavor to attain an accuracy well within one degree in the evaluation of average winding temperatures, I have found it necessary to remove both end covers of the motor enclosure, place thermometers on the insulation and adjacent laminations, and, taking readings at regular intervals, defer the "cold resistance" measurement until a state of complete thermal equilibrium with the ambient was observed. While such a degree of disassembling would doubtless be inconvenient in factory testing, and ordinarily impracticable in the field, its desirability in an engineering investigation shows that the code requirements quoted by Mr. Potter and the precautions urged by Mr. Summers are extremely important and none too severe.

In an extended research where, after due investigation, thermometers can be strategically and permanently placed with bulbs properly protected from the influence of surrounding air, the temperatures which they indicate may be fully as consistent as those determined by any other means; but when hurriedly applied after shutdown, with insufficient contact area, inadequate bulb protection, and no certainty as to correct location, the accuracy of the resulting hottest-spot temperature cannot compare with that attainable by resistance measurements. Moreover, in loading-back tests, the resistance method offers the possibility of checking winding temperature at any time during the run by the momentary removal of a-c power to permit bridge observations, the machine being driven by its loading generator during this interval. Whether or not resistance values obtained under these conditions would be acceptable as a criterion of ultimate insulation temperature at the end of test, is open to question; but, in some instances at least, I have found them to yield results apparently more dependable than those of readings taken after the machine was shut down.

L. A. Kilgore (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In the measurement of temperature by resistance, there is an additional difficulty not mentioned by C. P. Potter and E. R. Summers. This difficulty arises from the fact that the temperature of a winding changes very rapidly in the first few minutes after shutdown and the reading at one minute after shutdown may be about two to six degrees lower than the actual average temperature; furthermore, if one tester gets the readings in $^1/_2$ minute and another in $1^1/_2$ minutes, the readings may differ by about this same amount.

There is an accurate method of overcoming this difficulty which the writer believes should be incorporated as part of any proposed standard test by resistance, to be used whenever the readings cannot be taken within one-half minute. This method consists of plotting the curve of temperature (or resistance) against time and projecting back to the instant of shutdown. The initial slope of this time-temperature curve can be shown to be the initial average loss per unit of conducting material divided by the thermal capacity. Thus, in copper (neglecting eddy currents and using constants for copper at 75 degrees), the initial rate of change in degrees centigrade per minute is equal to 0.9 times (current density in thousands of amperes per square inch).[2]

With tests made in this more accurate manner (projecting back to the instant of dumping load), our general experience on open motors above 200 horsepower has been that 10 to 15 degrees differential exists between temperature by resistance and by thermometer. For totally enclosed motors, this differential is somewhat less, depending on how the machine is ventilated and how hard it is worked, but tests on a number of motors indicate a five- to ten-degree differential.

It may be desirable to use temperature by resistance for enclosed machines and other inaccessible machines, but before this can be made standard, an agreement must be reached on the exact method of test and on the proper differential between resistance and thermometer temperature limits.

Henry Thomas (Sun Oil Company, Philadelphia, Pa.): The papers by Mr. Summers and Mr. Potter have covered the subject so well and presented data and logical discussions in such a complete form, that there is little to be added. The points are brought out so clearly showing the weakness of the existing methods of temperature determination and the advantages and the reasonableness of the resistance method, that there seems no answer except to adopt it as the only consistent, accurate, and convenient method to be used for all types of motors, but especially that of the enclosed, or partially enclosed type.

The authors have brought out very clearly that the thermometer method at best gives quite variable results, cannot be checked by any two persons, even those experienced, and that with a very large number of present-day motors it is impossible, or practically so, to use thermometers.

I have very strongly favored the use of the resistance method for use on induction motors since 1930 and this method has been specified on all equipment purchased by the company that I represent since that time. A great many tests have been made on motors of practically all sizes and types and from various manufacturers which have fully convinced me that this method is first of all, far more consistent, is easily made, and can readily be used in the shop or in the field. When the preliminary report on the test code for induction motors was first brought out, I wrote to the chairman of that committee regarding this matter, and recommended its adoption as the standard method, but allowing the use of thermometers either for special purposes, or as a check.

I am in agreement with most of the conclusions drawn from the data and reasoning presented in the papers. These conclusions and recommendations, if carried out, will prove a distinct advance in the accurate and consistent temperature rating of induction motors.

With reference to one of the proposals, however, I have been able to find very little justification for placing the new limits of temperature rise as measured by resistance five degrees centigrade above the present limit determined by thermometer. Our own careful observation over a period of eight to ten years indicates that the temperature rise of induction motors of the usual commercial types will agree very closely when taken by the resistance method and by thermometer, provided the thermometer is used with proper care as to selection of location on the winding and proper placing and protection of the thermometer bulb, or if a thermocouple is used and placed with the same consideration.

An analysis of the data presented in Mr. Summers' paper will, I believe, bear this out to a very large degree, for example in table III.

Average temperature rise, fan-cooled motors—46.9 degrees centigrade by resistance, 48.0 degrees centigrade by thermometer

Average temperature rise, enclosed (no fan) motors—40.1 degrees centigrade by resistance, 39.5 degrees centigrade by thermometer

Average temperature rise, splashproof motors—29.0 degrees centigrade by resistance, 30.8 degrees centigrade by thermometer

Average temperature rise, open motor—26.9 degrees centigrade by resistance, 29.0 degrees centigrade by thermometer

Our own tests on motors of various makes of the above types bear out these results very closely in every respect. If comparison is made of the thermometer against the resistance method, when thermometers are placed on practically any location on coils and without proper covering of the bulbs, either because of indifference or because of lack of accessibility of the parts, then the difference of the two methods may be 5 degrees centigrade or much more, up to 15 degrees centigrade or 20 degrees centigrade, as pointed out in the paper.

Mr. Summers states: "The resistance method gives results that are essentially the equivalent of careful exploration by thermometer." It should be remembered that the present method considers the rise as that of the hottest point on the winding, iron, or active parts of the motor, which can be read by thermometer, so it is only logical in making comparisons that readings as taken by the resistance method should be compared with the thermometer method when it is done in all its details with proper care. I believe it is quite evident, even with open motors, that it is more difficult and requires more time to take a sufficient number of thermometer readings to get the highest accessible temperature, than to take the reading by the resistance method. In the case of the enclosed motor, as indicated in the paper, the resistance method is the only practical one.

I do not believe any term such as "conventional allowance" should be considered in adopting a new method of rating, whether it refers to five degrees, ten degrees, or any other amount. A change to the resistance method would place the determination of temperature on an accurate basis with all data readily determinable, and easily checked.

The present standard of temperature rise, especially for closed motors, is already high and should not be changed because the resistance method is used. The resistance method is an average indication and does not indicate what is the so-called "hot spot" condition. The temperature which can be obtained by the thermometer method, if sufficient care is used, may be as high or even higher than by resistance, since it takes in all active parts as well as the windings. For this reason I feel that the present temperature rise should be maintained.

R. J. Sullivan (The Commonwealth and Southern Corporation, Jackson, Mich.): The adoption of motor temperature rise determination by resistance as the standard method, as proposed in these papers, is a step which is long overdue in the motor manufacturing industry. Manufacturers have been measuring temperature rise by resistance for years as a check on thermometer measurments, but the results obtained with the thermometers have largely been relied upon in rating motors and making test reports, due to the fact that this method has been specified as standard.

The writer, having been responsible for motor design and testing in several organizations, and being now associated with an organization which applies many motors, has long believed that motor temperature rise obtained by the resistance method is simpler, more consistent, and more accurate than that obtained by the thermometer method, and that the resistance method should be adopted as the standard method. Mr. Summers and Mr. Potter have made a valuable contribution to the industry by providing quantitative proof to support their recommendations.

When the resistance method is used with the proper equipment and precautions, persons not associated with the tests can rely on the reported results or check them, without the uncertainty which accompanies results obtained by thermometer measurements in regard to the location and method of application of the thermometers, presence of drafts, thermometer preheating and time of application after shutdown, and other factors.

An organization which purchases motors in any considerable quantity can better afford to invest in the necessary equipment for accurate resistance measurements than to maintain skilled test personnel for the intelligent application of thermometers in heat runs. The resistance method is a much more convenient and consistent method for the motor consumer to use, especially in the case of enclosed or built-in motors, and it would be of considerable value to have these tests consistent with the methods used by the motor manufacturers as a basis for ratings, guarantees, and test reports.

The precautions emphasized in the papers in regard to taking "cold resistance" are important and should not be overlooked by anyone checking motor temperature rise by the resistance method. A motor may be brought in for test from an unheated storeroom where the ambient temperature is 20 degrees centigrade or 25 degrees centigrade less than that of the test room, or a field test may be made before a machine has thoroughly cooled after previous operation in service. The resistance changes approximately one per cent for each $2^{1}/_{2}$-degree-centigrade difference in temperature.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburg, Pa.): The paper by Mr. Summers recommends a change from the thermometer to the resistance method of measuring temperature-rise in our various commercial standards (see also reference 7 of the paper). There is no question as to the desirability of making this change for some types of machines, but it is doubtful that a wholesale change, including large machinery, is justified at this time. Whenever and wherever the change is made, a question immediately arises as to the value which should be used for the permissible temperature-rise by resistance. The natural inclination is to establish this value by taking

Table II

	A	B	C	D
Allowable hot-spot temperature (degrees)	105	105	105	105
Allowance for ambient (degrees)	40	25	30	30
Allowance for difference between hot-spot and resistance measurement (degrees)	12	5	6	10
Per cent	(22.5)	(6.5)	(9)	(15)
Temperature rise by resistance (degrees)	53	75	69	65
Allowance for load factor (degrees)	10	0	3	0
Per cent	(23)	0	(4.5)	0
Temperature rise allowing for load factor (resistance) (degrees)	43	75	66	65
Allowance for variations in power supply (degrees)	6	3	3	5
Per cent	(16)	(4)	(5)	(8.5)
Temperature rise allowing for load factor and variations in power supply (resistance) (degrees)	37	72	63	60
Allowance for service mounting (degrees)	7	2	3	0
Per cent	(23.5)	(3)	(5)	0
Temperature rise allowing for load factor, variations in power supply, and service mounting (resistance) (degrees)	30	70	60	60
Allowance for difference between resistance and thermometer (degrees)	6	4	8	10
Per cent	(25)	(6.5)	(16.5)	(20)
Temperature rise allowing for load factor, variations in power supply, and service mounting (thermometer) (degrees)	24	66	52	50

into account the difference between the values by thermometer and by resistance determined by tests such as described in the paper. Data of this nature undoubtedly are of value and should receive due consideration; however, it should be determined whether a better method might not be to work down from the more basic value allowed for the hot-spot temperature to a permissible value by resistance. In so doing, it will not be practicable to make allowance for each of the various factors independently, but the whole problem should be considered broadly and a decision reached regarding the best philosophy of rating.

In rating and applying electric motors, a great many factors have to be given consideration, such as the permissible hot-spot value, ambient temperature, the difference between the hot-spot and resistance measurements, variations in power supply (particularly voltage), service difficulties resulting from insufficient knowledge of the expected load, differences in cooling between the test-floor and service mounting, etc. All of these factors are not merely theoretical possibilities, but can be of appreciable importance either individually or collectively. In table II of this discussion an attempt has been made under *A* to take all of these factors into account. The differences between temperatures are given in degrees and also in percentages, the latter in most cases being the best figure for evaluation. The allowances for all values under *A* are not the maximum experienced for each particular factor indicated but are values approximating the safer limit. It will be noted that this method results in a temperature-rise of 30 degrees by resistance and of 24 degrees by thermometer. Obviously, nobody would consider the adoption of these values as rating standards in view of the extensive experience available showing satisfactory all-round results with the present 40- and 50-degree ratings (by thermometer). This is simply an admission of the fact that we cannot afford to adopt methods too conservative, because it would result in an enormous economic waste.

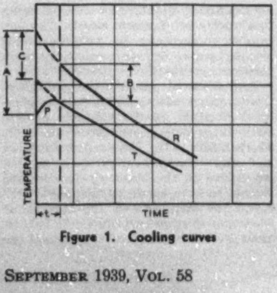

Figure 1. Cooling curves

Under *B*, values have been introduced approximating the average values found in practice. Zero has been given for the load factor because the actual load will be below at least as frequently as above the estimated load values. This leads to values of 70 degrees rise by resistance and 66 degrees by thermometer. It is again evident that nobody will seriously consider this practice, because experience with 40- and 50-degree ratings (by thermometer) do not indicate that the appreciably higher values given under *B* can be used safely. Under *C*, certain intermediate values have been selected more or less arbitrarily, resulting in a value of 60 degrees by resistance. There is no particular merit in the values given under *C* except that they lead to 60 degrees, which is the international value and one which also is now used in a number of our American standards. All of these figures indicate that the tendency which has developed to allow in the rating structure for all sorts of possibilities is impracticable.

We should, therefore, realize that in rating and applying machines there are three responsible parties involved—namely, the manufacturer, the central station, and the user—and that no one of them can be held responsible for factors which are beyond their knowledge or control. This naturally leads to a method of establishing satisfactory values for a rating structure such as shown under *D*. Here the equivalent ambient temperature has been assumed to be 30 degrees, a value which will cover the majority of all applications; a rather liberal allowance has been made for the difference between the hot-spot and resistance measurement values, and a moderate allowance has been made for the frequently occurring smaller variations in the power supply. The value arrived at for the resistance method is 60 degrees. With this as a background, the user knows that the machine which he buys will be suitable for the majority of reasonably normal conditions met in practice. On the other hand, if he feels that in his application he is likely to encounter exceptionally high ambient temperatures or that his power supply differs considerably from the rated value of the motor, or, again, if he is uncertain about his load or is not sure that he is applying the motor without interfering with the normal cooling and ventilating provisions, he must make allowances for such variations either through his own knowledge or by consulting available application data or the supplier of the motor.

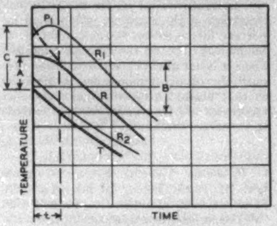

Figure 2. Cooling curves

The inadvisability of basing the temperature-rise value by resistance on certain differences between thermometer and resistance measurements is further indicated by figures 1 and 2 of this discussion. In figure 1, curve *R* gives the time-temperature curve (by resistance) after shutdown, and curve *T* is the corresponding value by thermometer. In this figure it is assumed that the time *t* expires before the first reading can be taken and that we are dealing with a large machine in which the copper temperatures are higher than the temperatures in the core. Both curves are extrapolated toward $t = 0$. If the thermometer is placed at the end connections, which may be the only accessible place, it will be found that the actual temperature is likely to rise for some time after shutdown, as indicated by the portion *P* of curve *T*. This means that the actual difference in operation between the thermometer and resistance values corresponds to *A*, while the difference at the earliest time of measurement corresponds to *B*. If both curves are extrapolated as shown by the dotted lines, we obtain the value *C*. We therefore have three widely different values to choose from, and consequently the final result will be greatly influenced by the procedure followed.

Figure 2 is intended to give similar conditions for a machine in which the core is hotter than the coil portions in the slots. Here the temperature, by resistance, of some coil portions is likely to rise slightly after shutdown, as indicated by the portion P_1 of curve R_1. Curve R_2 corresponds to the value of resistance applying for certain portions of the coils away from the core, while curve *R* may be considered to represent the average of all coil portions. Curve *T* again represents the value determined by thermometer as it is likely to be in this case. We now can again choose from the values *A*, *B*, and *C*, which widely differ from one another. All of these discrepancies may not be of primary importance in per cent of the total temperature-rises, but the figures merely show that there are some difficulties encountered in an attempt to obtain the difference between the thermometer and resistance measurements with any degree of accuracy.

In the use of the resistance method for large machines, there is one inherent difficulty of considerable practical importance. Frequently the temperature of the test floor varies appreciably and rather suddenly with weather changes; however, the resistance temperatures of large machines will follow such changes only slowly. This will naturally result in errors in the cold resistance of the machine, which is an important factor in the determination of the temperature rise by the resistance method. Since it is not always possible to delay tests until the copper temperature is the same as that of the surrounding air, some discrepancies will be unavoidable, particularly in the larger machines.

C. P. Potter (Wagner Electric Corporation, St. Louis, Mo.): In his paper, Mr. Summers calls attention to the fact that "all the surface measurements on the stator windings of the totally enclosed and totally enclosed fan-cooled motors of table III were obtained with thermocouples which were well distributed in order to locate the hottest point." In other words, "the testing procedure for the enclosed machines was carried beyond the requirements of the ASA standards, because some of the thermocouples were in locations that could not have been reached by inserting thermometers through holes in the frame." He further states that "the recorded winding surface temperatures were approximately 3 degrees centigrade higher on the average than could have been obtained by the thermometer method." The following table shows the results of the temperature tests on the totally enclosed motors, including temperature readings by thermometer which were estimated by assuming that the rise by thermocouple is 10 per cent higher than the rise by thermometer.

	Totally Enclosed Fan Cooled	Totally Enclosed
(*A*) Average temperature rise by thermocouple of hottest winding surface—degrees centigrade..........	48	.....39.5
(*B*) Average temperature rise by thermocouple of hottest *accessible* winding surface—degrees centigrade.....	45	.....36.5
(*C*) Average temperature rise by thermometer of hottest accessible winding surface—degrees centigrade.....	40.9	.....33.2
(*D*) Average temperature rise by resistance — degrees centigrade	46.9	.....40.1
(*D*−*A*) Difference between temperature rise by resistance and temperature rise by thermometer — degrees centigrade..............	6	......6.9

In appendixes IV and V of his paper, Mr. Summers compares temperature by resistance with the maximum temperature observed by surface measurements on either the windings or core iron. After studying his paper, this seems to be a desirable procedure but it probably accounts for some of the variation in results reported by different manufacturers. Some engineers undoubtedly compare the temperature by resistance with the temperature of the winding surface. If this were done in table IV, the maximum differences between resistance measurements and winding temperatures (at least some of which were taken by thermometer), are eight degrees centigrade in one motor and seven degrees centigrade in two other machines. Similarly, in table IV, when all the winding surface temperatures were taken by thermometer, the differences between temperature rise by resistance and temperature rise by thermometer at full load and the various voltages are seven degrees, seven degrees, and six degrees, respectively. In other words, the differential between winding surface measurements taken by thermometer and resistance measurements seems to be about seven degrees centigrade.

In table III the temperature rise of the laminations of the enclosed squirrel-cage motors is considerably less than the temperature rise of the windings, while in the open and splashproof motors, the reverse is true. It would seem likely that open and enclosed motors of a given rating have magnetic circuits which are more or less alike, and it is probable that the flux densities in the enclosed motors are higher than in the open machines. I would, therefore, like to ask the author, whether there is a possibility that the laminations are more accessible on the open than on the enclosed motors, and whether this may account for the difference in temperatures.

Mr. Summers deserves a great deal of credit for the completeness of his treatment of the determination of temperature rise of induction motors. He has presented a wealth of information in a systematic manner and I agree with him completely in his conclusions and recommendations.

It is gratifying to have so much discussion on the paper on measurement of temperature and to have those who take part in the discussion, agree, in general, with the conclusions reached by the writer. The comments made by Messrs. Hill and Sullivan are very interesting and Messrs. Anderson, Rutherford, and Veinott have given additional data confirming the results reported in the paper. Both Messrs. Rutherford and Veinott have also suggested that the allowance for the hot spot be expressed as a per cent rather than a constant value. This, in the writer's opinion, is a very excellent suggestion and one which should be adopted.

Messrs. Alger and Kilgore have suggested methods of correcting the temperature rise back to the instant of shutdown, and Mr. Alger states that it will be possible to develop correction factors for motor temperature tests similar to those used in transformer practice. This can undoubtedly be done, but it might be well to point out that the two cases are somewhat different. In a transformer the oil circulates due to the difference in temperature between the top of his case and the bottom of the case and this circulation is not immediately interrupted when the temperature test is finished. In a motor the ventilation is produced by the fans on the rotor and this ventilation stops as soon as the temperature test is finished. In the case of an average motor the temperature of the stator iron is ordinarily less than the temperature of the extended parts of the winding. When the temperature test is stopped, the temperatures of the exposed parts of the windings decrease while the temperatures of the stator core increase, indicating that an equalization of temperature is taking place in the machine. It is, therefore the writer's opinion that the resistance in a motor winding does not decrease nearly as rapidly as that of a transformer winding after the temperature test is ended, and it is suggested that this subject be given further study.

Mr. Thomas has commented from the point of view of the motor user and while he agrees that temperature rise should be measured by the resistance method, he does not agree that any higher values of temperature rise be assigned if the change in method is approved. This again is a subject which deserves, and will have, further study before any changes are made in existing standards.

E. R. Summers: In his paper Mr. Potter has presented data and arrived at conclusions which are in close agreement with my test results and recommendations. The following remarks regarding his discussion of my paper do not represent any basic difference of opinion, but are intended only to supplement some of the points which he has mentioned.

Mr. Potter has estimated that thermometers would have indicated ten per cent less winding temperature rise than was actually obtained with thermocouples on the totally enclosed fan-cooled and totally enclosed motors of table III. Nonuniformity of testing practices among different organizations give rise to considerable variations between the temperature differentials obtained by comparing thermometer and thermocouple readings. The leads of several thermocouples can be brought out through one hole in the frame, whereas a separate hole must be drilled for each thermometer used in an enclosed motor. Consequently readings are likely to be obtained at more points with thermocouples than with thermometers, and any general comparison of the two devices usually involves differences in thoroughness of exploration as well as variations in contact with internal parts. Furthermore thermocouples can readily be placed in locations not accessible to thermometers, and dependable comparisons would require exactly the same mounting positions.

Mr. Potter emphasizes the probability that some manufacturers compare temperatures obtained by the resistance method with thermometer measurements taken on the windings only. Such comparisons are not valid, because the thermometer method as defined in the standards includes the hottest surfaces on the laminations as well as on the windings. On the majority of open and splashproof motors, the laminations are hotter than accessible parts of the winding. To interpret comparative data from different manufacturers, it is therefore essential to ascertain if readings on laminations are included. If lamination temperatures are omitted on open or splashproof motors, the data cannot be used directly for purposes of standardization.

From my experience, I find that thermometers and thermocouples when used in exactly the same locations with similar protective coverings will give readings which usually agree within the accuracy of the test, or within two degrees centigrade, as indicated in the data for motor *X* in table I.

Mr. Potter has questioned the data on the totally enclosed motors of table III because the indicated temperatures on the laminations are considerably less than on the windings. Since the lamination and winding temperatures are very nearly the same on totally enclosed motors, thermocouples were placed on the windings only. The laminations were not accessible, and the indicated temperatures are lower than the actual values by the amount of thermal drop through the frame as indicated by the asterisk and note at the bottom of table III. These apparent discrepancies in lamination temperatures are indicative of the differences which may be expected when temperatures are measured only on the outside of the frame on totally enclosed motors.

Mr. Kilgore states that winding temperatures change very rapidly in the first few minutes after shutdown, and that test results by the resistance method may vary as much as six degrees centigrade if one tester gets the readings in 0.5 minute and another requires 1.5 minutes after shutdown.

Heat energy is dissipated at a much lower rate after shutdown than when operating at normal load. For a machine operating at a nominal current density of 2,500 amperes per square inch, the initial rate of change of copper temperature during the first few seconds after removing load (while motor is still coasting at full speed) is approximately $0.9\left(\frac{2{,}500}{1{,}000}\right)^2$ or 5.6 degrees centigrade per minute. However this high rate of change does not persist for two basic reasons:

1. The thermal capacity of the adjacent insulation and laminations occasions a sharp decrease in rate of change in copper temperature after the first few seconds.

2. The motor ventilation decreases as the machine is decelerated.

In many cases the motor can be stopped in less than one-half minute. The change in average copper temperature during deceleration will seldom exceed two degrees centigrade if the machine is stopped promptly, and part of this change can be accounted for by extending the resistance curve to zero time.

After a motor is stopped the rate of change in average copper temperature is usually less than one degree centigrade per minute for the first ten minutes. Figure 3 of this discussion shows the change in temperature by resistance method after shutdown on eight typical motors representing widely different ratings and types of construction. Zero time indicates the instant of opening circuit breaker to remove load. The dotted portion of each curve shows the time required to stop the motor and obtain first resistance reading. Curve *A* is the only case where a probable change in temperature of as much as six degrees centigrade is indicated during the first minute, and this occurred on a 40-degree-centigrade-rated 25-horsepower motor that had been operated continuously at 168 per cent of normal load and which had an abnormal temperature rise of 108 degrees centigrade one minute after removing load.

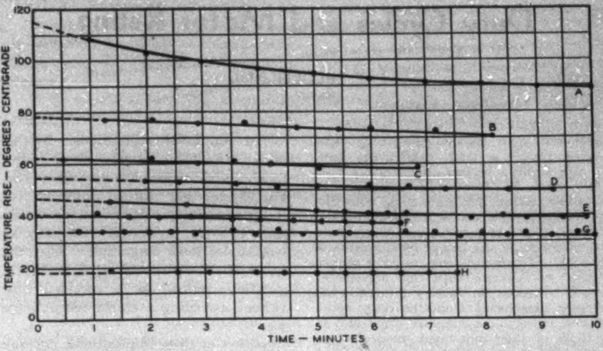

Figure 3. Curves showing change in average winding temperature after shutdown (resistance method)

Curve	Form of Motor Construction	Horsepower Rating	Speed (Revolutions per Minute)
A	Open	25	1,800
B	Splashproof	15	900
C	Totally enclosed fan-cooled	50	3,600
D	Totally enclosed fan-cooled	300	1,800
E	Totally enclosed	60	900
F	Totally enclosed fan-cooled	5	720
G	Splashproof	30	900
H	Open	40	1,800

To obtain reliable temperature measurements by either the thermometer or resistance methods, provision must be made for stopping the machine promptly to minimize effects of ventilation by motor fans. It is far more important to decelerate the motor quickly than it is to measure the resistance immediately after stopping, because heat energy is dissipated much faster while the motor is running (except for totally enclosed machines). The difference in resistance readings taken 1.5 minutes instead of only 0.5 minute after stopping motor is seldom more than would correspond to one degree centigrade on enclosed motors or two degrees centigrade on open machines.

The average differentials of 10 degrees centigrade to 15 degrees centigrade which Mr. Kilgore reports between thermometer and resistance methods are much greater than I have observed, and this disagreement in results is probably caused by (1) differences in thermometer exploration, (2) omission of lamination temperatures, and (3) differences in estimated change in copper temperatures during deceleration. Mr. Kilgore's comments would indicate that the 15-degree-centigrade conventional allowance is not sufficient for thermometers, if the resistance readings indicate 15 degrees centigrade higher average temperature than the thermometer method.

Mr. Hellmund's discussion is quite broad, and a number of his general comments apply equally well to all methods of temperature measurement. In the table where he has evaluated the various factors concerned with motor rating, the combined effects of service factor, variations in power supply, and allowance for service mounting are superimposed to arrive at the "conservative" limit of 30 degrees centigrade resistance rating in column *A* for general purpose motors. Under the present standards the 15 per cent (or ten degrees centigrade) service factor is imposed only for normal conditions of power supply and ventilation, and is not intended to apply for abnormal operating conditions. However, his evaluation of these different factors is certainly of pertinent interest.

Since the American and AIEE standards now specify the thermometer method, any general change to the resistance method necessarily involves direct differentials between the two methods. The cooling curves which Mr. Hellmund presents indicate that these differentials are quite indefinite and that widely varying values may be obtained. These curves make the problem appear more difficult than it really is.

The temperature by the thermometer method as now defined in the standards is the highest observed reading on either laminations or windings before or after shutdown, and would correspond definitely to the maximum point on Mr. Hellmund's curve marked *T* which should represent the highest reading thermometer.

The temperature by the resistance method may be defined either as the temperature immediately after shutdown (within a specified time) or as that obtained by projecting curve *R* to zero time. As previously stated the effect of projecting curve *R* to zero time seldom changes the result by more than two degrees centigrade if the motor is stopped promptly, and therefore the values from which one must choose are not widely different.

In the case of large or high-speed motors where no provision can be made to stop them quickly, the resistance method as

Duty Cycles and Motor Rating

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MEMBER AIEE

Synopsis: The horsepower rating of a motor carries a dual implication—first, torque ability; second, temperature rise. The two are frequently confused. In selecting motors for duty-cycle jobs the two concepts should be considered separately. The motor rating and type should be chosen to fit the torque requirements of the job. The proper time rating or service factor to associate with this horsepower rating to insure satisfactory insulation life can be determined from the duty cycle. Use of oversize continuously rated motors instead of short-time-rated motors to secure high torque ability on variable load jobs imposes an economic loss. Frequent starting and reversing or starting high inertia loads imposes a temperature hazard frequently greater than heavy overloads. Horsepower rating as now understood is not a satisfactory criterion of a motor's reversing ability. These topics are developed by means of simple hydraulic analogies.

BY DEFINITION, one horsepower means 33,000 foot-pounds per minute, equivalent to 746 watts. By convention and rules, the term horsepower when used as a motor rating, carries additional implications. In particular, the rating implies first, torque ability, and second, temperature rise. The horsepower rating suggests the ability of the motor from a torque standpoint to start, accelerate, and carry an overload of some magnitude. Also, the motor will carry steady rated load, or a specified overload for a specified time without exceeding a stated temperature rise.

Neither of these implications, nor any rules or generally known conventions, clearly suggest what a given motor will do on a duty-cycle job with a varying load, particularly if starting or reversing constitutes a regular part of the duty cycle. Sometimes, unduly large and costly motors are used to secure high torque ability for a temporary overload. A short-time-rated motor, for example one hour, 50 degrees, of correct size and rating would be more economical. However, if frequent starting or reversing or if starting a heavy flywheel load constitutes a regular part of the duty cycle, there is a distinct tendency to "undermotor" because there is not a general recognition of the heavy overload imposed by such service.

This paper is primarily concerned with what performance may ordinarily be expected from average polyphase squirrel-cage induction motors like those used on machine tools and similar applications when the load is not steady and continuous.

Torque Ability

In selecting a motor for a particular job, it is expedient and conducive to obtaining better performance or smaller motors, to consider the two concepts, torque and temperature, separately. First choose the horsepower rating from a torque standpoint only without any consideration of temperature. Temperature rise is considered separately later. The motor must have sufficient starting torque to start the load under the adverse conditions of low line voltage and reasonable overload. The motor must have sufficient accelerating torque to bring this load up to full speed. Sometimes the distinction between starting and accelerating torque is not made and higher starting torque than necessary is specified because of experience with another type motor which had low accelerating torque. The motor must have sufficient breakdown torque to carry a reasonable overload with low line voltage without an excessive drop in speed. More than adequate torque ability is an extravagance paid for by other characteristics or increased size.

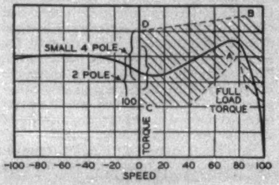

Figure 1. Typical speed-torque curve class A and B motors

There are four readily available classes of squirrel-cage motors:

Class *A*. Normal starting torque, normal starting current (usually made with a single-cage rotor)

Class *B*. Normal starting torque, low starting current (frequently made with a double-cage rotor)

Class *C*. High starting torque, low starting current (usually made with a double-cage rotor)

Class *D*. High starting torque, high slip (almost always single cage)

In this paper we will generally not distinguish between classes *A* and *B* as

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Paper number 39-59, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 31, 1938.

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previously described is not directly applicable, and imbedded detectors are recommended. Mr. Hill has mentioned the possibility of opening the power circuit momentarily to obtain resistance readings while the machine is running. We have taken some data in this manner which was consistent in every respect. Our experience with this method of taking resistance readings is not yet considered conclusive, but such a procedure does offer a possible means of testing large motors which cannot be decelerated in a reasonable time.

Mr. Hellmund's comments on the difficulty in obtaining dependable cold resistance temperatures are well stated. In general it is more difficult to obtain the "cold" winding temperature within one degree with thermometers when the ambient is changing, than it is to measure the ohmic resistance with comparable accuracy using a suitable double bridge. Mr. Sullivan and Mr. Hill have also carefully discussed the importance of accurate cold temperature measurements.

Mr. Thomas has objected to any addition to the present thermometer ratings to obtain the proposed new ratings by resistance. The suggested increase of five degrees was not intended to represent any expected difference between resistance data and the temperatures which could be obtained by a thorough exploration with thermometers (or thermocouples). This increase is suggested because the resistance method avoids the low readings sometimes obtained by thermometer, and 60 degrees centigrade rise by resistance is considered to be fully as conservative as 55 degrees centigrade rise by thermometer. On the basis of 40 degrees centigrade ambient and 105 degrees centigrade limiting hottest-spot temperature, a resistance rating of 60 degrees centigrade provides a hot-spot allowance of 5 degrees centigrade which tests indicate to be a representative value for totally enclosed motors.

It is very pleasing to note the close agreement between the discussions of Messrs. Potter, Hill, Sullivan, and Thomas. Although not in complete agreement on the proposed differentials and ratings by resistance, everyone apparently agrees in principle that the resistance method is desirable for enclosed and inaccessible machines.

they are usually interchangeable in application.

To establish a reason for selecting a particular horsepower rating and a particular class motor to do a particular job, typical speed-torque curves for average motors are shown in figures 1, 2, and 3. Data are intended for illustrative purposes only, as individual motors may depart appreciably from the average. Points *A* and *B*, figure 1, show minimum and maximum logical values for breakdown torque. It is assumed that any motor should carry some overload, say 25 per cent at 10 per cent reduced voltage, with a reasonable margin for safety, say 20 per cent. Since the motor's torque ability is proportioned to the line voltage squared, we require $1.25 \times 1.2 \div 0.9^2 = 1.85$ or 185 per cent breakdown torque, as tested under normal conditions. This is close to the 200 per cent minimum breakdown torque which is now standard for class *A* and *B* motors. It is assumed that a motor would not ordinarily meet the minimum requirements for the next higher standard rating. In the lower range, an average step or ratio of successive horsepower ratings is about 140 per cent. This establishes point *B*, figure 1, as 280 per cent for the greatest breakdown torque to be logically expected.

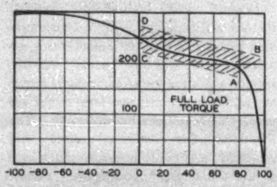


Figure 2. Typical speed-torque curve class D motors

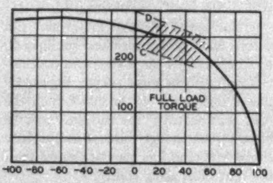


Figure 3. Typical speed-torque curves class D motors

Parenthetically, it should be noted that stray load losses constitute a part of the load on the motor. They appreciably reduce the breakdown torque below the value given by formulas in handbooks which do not recognize stray load losses.

In class *C* motors, breakdown torque is sacrificed to obtain increased starting torque. See figure 2 showing typical values.

With regard to starting torque, the writer knows of no process of reasoning which will suggest a logical value purely on the basis of the horsepower rating. Suitability to the usual application and the necessary compromises of design have established starting torque values for motors as now made. Extravagant starting torque can be obtained only by sacrifice of other desirable characteristics such as efficiency, power factor, or breakdown torque.

In figures 1, 2, and 3 a portion has been shaded to indicate the expected variations of different motors and of different ratings. Points *C* and *D*, figure 1, show the total range of variation in starting torque of class *A* and *B* motors. For different motors of a particular rating the range is of course smaller covering the lower, middle, or upper part of the range shown in figure 1.

Class *A* or *B* motors should be used for jobs not requiring unusually high starting torque, not started or reversed frequently, or not used to start a heavy inertia load. As will be shown later, there are many cases where a short-time-rated class *A* or *B* motor can be used economically.

Class *C* motors are intended for jobs requiring exceptionally high starting torque such as reciprocating compressors. In the smaller sizes, efficiency may be sacrificed, and in the larger sizes, breakdown torque must be sacrificed to obtain this higher starting torque. As will be shown later, class *C* motors can be reversed more frequently than class *A* or *B*.

Class *D* motors are used for a variety of jobs, including:

1. Hoists and similar jobs where maximum starting torque is required with sacrifice in ordinary efficiency.
2. Frequent reversing.
3. Starting, stopping, and reversing high inertia loads such as extractors and centrifuges.

In tentatively selecting the horsepower rating and best class of motor from a torque standpoint, the job should be considered with regard to its requirements for starting, accelerating, and breakdown torque. To allow for a possible 10 per cent drop in line voltage, the rating must be high enough so that 81 per cent of the breakdown torque of the motor is safely higher than the maximum load. (Breakdown torque is proportioned to the line voltage squared.) Also, the speed with this maximum load must not be unduly low. If the rating is three quarters of the maximum load, these conditions are generally fulfilled. Class *A* motors can be temporarily overloaded somewhat more. If unusually high starting torque is not necessary, a class *A* or *B* motor is of course indicated. If higher starting torque is required, a class *C* motor is proper. The horsepower rating must be such that 81 per cent of the starting torque will always start the load. Again, we allow for low line voltage. Whichever of these two specifications, starting or breakdown torque, indicates the higher rating is the factor which determines the rating from a torque standpoint. With polyphase motors and other motors not having low points in the torque curve, extravagant starting torque is not necessary to secure adequate accelerating torque.

Temperature

We must now consider the motor from a temperature standpoint. Is the tentatively selected rating cool? If very cool what short-time-rated motor can be used? As is well known, temperature is limited because insulation deteriorates more rapidly at high temperatures. The insulation should not wear out while the motor is otherwise modern and usable. Insulation deterioration is a function of time as well as temperature. Thus the temperature rise during a short period may safely be somewhat higher than conventional values if compensated by much longer periods during which the temperature is much lower.

The newer synthetic insulation ma-

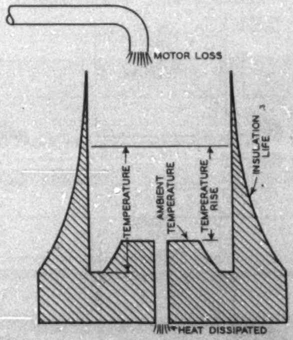


Figure 4. Hydraulic analogy illustrating temperature rise

terials now being used in motors have longer useful life than materials used a few years ago. Today enamel in combination with cellulose is generally used for wire insulation. This enamel is better and more generally used than when present temperatures were standardized. With all the developmental work now being done, it is not unreasonable to expect improvements to continue.

Figure 4 presents a hydraulic analogy to explain temperature rise. (Some distortion of the laws of hydraulics is necessary to make the analogy mathematically correct.) Water flowing into the tank is illustrative of the motor losses. Watts loss in the motor is analogous to cubic feet per minute water flow. The total kilowatt-hours energy loss is illustrated by the total cubic feet of water. The height of water in the tank is analogous to temperature. The capacity of the tank is illustrative of the heat storage capacity of the motor, proportional to the summation of the weight of the parts multiplied by their specific heat (weighted for temperature differences). The outlet at the bottom is illustrative of the heat dissipation by radiation, conduction, and convection. Obviously, the water rises to a level so that the outflow balances the inflow, that is, the temperature is steady.

Note that the walls of the tank are not of uniform thickness. This suggests the time factor in insulation deterioration. The slow rusting or corrosion of the walls is analogous to wearing out of the insulation. (The scale for insulation life is distorted to keep the diagram reasonably small.) The diagrams which follow are simplified by showing a tank with a thin wall. The reader should remember that a tank like figure 4 is always implied to represent more closely the fact that insulation wears out because of high temperature for a long time.

In figure 5, the analogy is elaborated to bring in more detailed factors. Losses flow into the tank from two pipes—first no load losses and second extra losses due to carrying a load. It is a quite accurate common assumption that the extra losses due to load are proportioned to the load squared. The heat dissipation is divided into two parts—the dissipation when the motor is not running and the extra dissipation due to extra ventilation when the motor is running. Valve *A* is analogous to the line switch. *A* and *C* are tied together to indicate "turning on" the ventilation when the motor is started. Valve *B* is opened more or less to indicate more or less load.

Figure 5*A* pertains to an average normal or high-speed motor, say 1,200 to 3,600 rpm. Figure 5*B* indicates the changed proportion of the factors for a slower speed motor, say 450 to 900 rpm of the same physical size but lower horsepower rating. Note the greater losses at no load and the lesser ventilation.

Figures 5*C* and 5*D* illustrate short-time-rated motors made in the *same size parts* for which figures 5*A* and 5*B* indicate continuously rated motors. The horsepower rating is higher, the torque ability, that is, starting, accelerating, and short-time overload ability, are increased. When operated at the increased rated load, the losses are higher as indicated by the larger inflow pipes. Since the heat dissipation ability is not increased, obviously the motor cannot be operated at rated load continuously without exceeding ordinary temperature limits. According to the conventional rating, say one hour, 50 degrees centigrade, the motor is started cold and at the end of one hour the energy losses have been either stored in the parts or dissipated so that the motor has just reached its temperature limit. In typical industrial applications there is seldom such a duty cycle. Usually the motor is operated with varying load or intermittently so that after a period the motor settles down to a continuous average temperature. In the analogy valves *A* and *C* may be open all or part time to indicate either continuous or intermittent operation. Valve *B* is turned off and on varying amounts according to the actual load. The height of fluid rises to an average level which is maintained because on the average just as many cubic feet of water flow in as flow out.

Normal and high-speed one-hour motors will generally carry a continuous load as high as the continuous rating associated with the size parts. For example, a 5-horsepower continuous motor and a $7^1/_2$-horsepower one-hour motor are usually made in the same size parts. The $7^1/_2$-horsepower one-hour motor will generally carry continuously as much load as the 5-horsepower continuous motor. However, the torque ability is increased to take care of short overloads and abnormal conditions. Thus the one-hour motor is well suited to many industrial loads.

A slow-speed one-hour motor may not carry, from a temperature standpoint, as much load as the lower rated continuous motor in the same parts. Figure 5*D* makes this clear. If the motor is on the line continuously, the no-load loss is high. Even though valve *B* is partially closed, the total loss may be greater than the heat dissipating ability.

The short-time conventional temperature test is not always an exact criterion of the merit of a motor. Figure 6*A* is analogous to a motor which is large and heavy for its rating with inferior ventilation. At the end of the conventional test, the temperature is not excessive since the motor has excess heat-energy storage capacity (that is, a large tank). On an actual intermittent load the motor operates hot due to poor ventilation

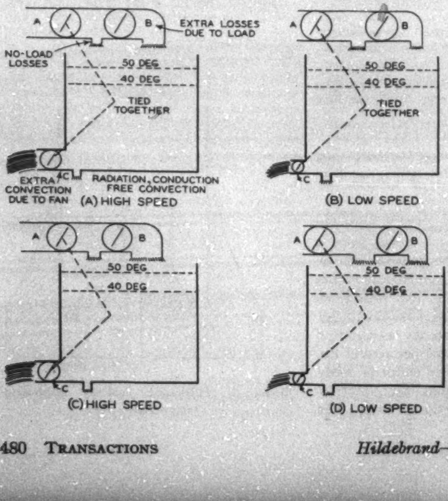


Figure 5. Hydraulic analogy illustrating factors affecting temperature rise

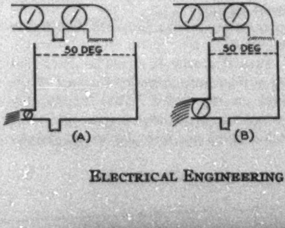
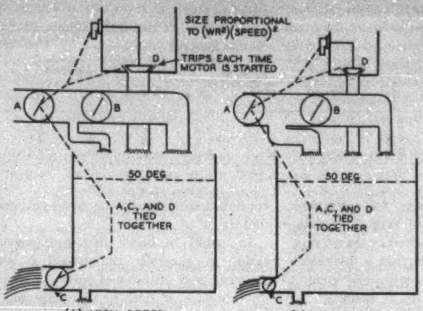


Figure 6 (right). Hydraulic analogy illustrating heavy poorly ventilated and light well-ventilated short-time-rated motors


SIZE PROPORTIONAL TO $(WR^2)(SPEED)^2$
D
TRIPS EACH TIME MOTOR IS STARTED
A
B
50 DEG
A, C, AND D TIED TOGETHER
C
A
B
D
50 DEG
A, C, AND D TIED TOGETHER
C

(A) HIGH SPEED (B) LOW SPEED

Figure 7. Hydraulic analogy illustrating extra losses due to starting

(small outflow from tank). Figure 6*B* indicates a lighter-weight well-ventilated motor which when tested by the conventional short-time test seems to be inferior as the temperature is high. On the actual varying load it operates cool because it is well ventilated.

Service Factor

Probably the economically sound field for these intermittent-duty motors is broader than their present range of use. Lack of a definite descriptive nomenclature which clearly indicates the ability of the short-time-rated motor on an intermittent load is possibly one of the largest detriments to the more extended use of these smaller, lighter, high-torque-ability motors. Obviously a slow-speed short-time-rated motor will not carry as much load with a safe temperature as a higher-speed motor which passes the same conventional test. It is not to be expected that all users of motors are sufficiently informed regarding details of design and application to use safely these more economical motors without fear of excess heating. Thus there exists a distinct tendency to "overmotor" from a heating standpoint in order to obtain a safe margin in torque ability.

There is a term in current use, the meaning of which can be broadened to indicate more definitely what these motors will do. The frequently quoted 1.15 service factor clearly indicates that the normal motor to which it is applied will carry 115 per cent rated load without exceeding recognized safe temperatures. A service factor less than unity would indicate that the motor must be loaded less than its rating continuously in order to maintain normal temperature limits. Of course, the load may be either continuous or a varying load of the same root-mean-square value.

If the job does not include frequent starting and reversing or starting high inertia loads, there is no hard problem in selecting the motor from a temperature standpoint. Merely calculate or estimate the root-mean-square load and see that the motor rating multiplied by the service factor is at least as high. For the purpose of this paper (root-mean-square horsepower load) is defined as

$$\sqrt{\frac{\Sigma\,(\text{hp})^2 \times \text{time}}{(\text{running time}) + \dfrac{(\text{standstill time})}{\text{constant}}}}$$

For the numerator each part of the duty cycle is considered separately. Take the sum for all parts of the cycle of the square of the horsepower load multiplied by the time for this element of the cycle. The constant in the denominator is the ratio of heat dissipation running to standstill, frequently assumed to be 4. Slightly higher values may apply to high-speed motors and lower values apply to low-speed motors. For enclosed motors without fans this constant is not much greater than one.

For application purposes, normal and high speed, one-hour motors may be assumed to have 80 per cent service factor. This factor is derived by the following reasoning: The standard motor has 1.15 service factor. The one-hour rating averages 140 per cent of the continuous rating in the same size parts. The one-hour motor will dissipate continuously as much loss as the continuous motor and the efficiency is essentially constant. Hence, the service factor of a one-hour motor may be assumed to be 1.15/1.4 = 0.82. We use 0.8.

Starting and Reversing

In the above all starting and reversing operations in the duty cycle are not included. The losses due to these operations are included separately. Frequent starting and reversing or starting a high inertia load imposes a severe temperature hazard, often more severe than a heavy overload. In figure 7 our hydraulic analogy is extended to illustrate the extra losses due to starting. As is well known, the stored energy in the rotating system constitutes a fundamental unit in acceleration problems. The stored energy is $3.87 \times 10^{-4}\,(WR^2)\,(\text{rpm})^2$ watt minutes. WR^2 is in pound feet squared. Each time a motor is started one of these units is supplied to the rotor in the form of heat. There are additional losses in the stator. Total losses for starting class *A* and *B* motors will average 2 to $2^1/_2$ times the rotor loss. With class *D* motors total losses will be $1^1/_4$ to $1^1/_2$ times the rotor loss. Class *C* motors have intermediate values.

In our hydraulic picture, figure 7, each time the motor is started, valve *D* is tripped, thus increasing the average inflow, analogous to increasing the average loss. Of course, valve *B* must be closed part of the time or the water will rise higher in the tank, that is, the ordinary load must be decreased to keep the temperature down to what it is for normal operation. Figure 8 shows a picture for reversing a motor. It differs from figure 7 in that the sudden inflow is four times as much.

The accelerating or reversing loss is proportional to the total WR^2 of the rotating system, including the motor and its load and proportional to the number of equivalent reversal per minute counting a start as one-quarter reversal.

We have not shown a picture for operation of a short-time motor, say one hour, on a reversing job. In fact, the terms become ambiguous. The motor is seldom

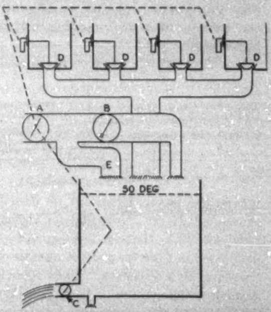


Figure 8. Hydraulic analogy illustrating extra losses due to reversing

Table I

		3,600 RPM			1,800 RPM			1,200 RPM					
Assumed			Assumed			Assumed			Assumed			Assumed	
Horsepower	Efficiency	$(WR^2)N$	WR^2	N	$(WR^2)N$	WR^2	N	$(WR^2)N$	WR^2	N	$(WR^2)N$	WR^2	N
2	.82	0.84	0.2	4.2	3.4	0.6	5.6	7.6	1.5	5.0	13	2	6.7
5	.85	1.7	0.5	3.4	6.8	1.5	4.5	15	3.8	4.0	26	5	5.2
15	.88	3.9	2.0	1.9	16	6.0	2.6	35	15	2.4	62	20	3.1

if ever operated for only one hour as the rating implies. It is in fact used for an all-day job. The load may be light and variable but the operation is continuous. Hence, reversing service should be specified in terms of ultimate temperature.

What is the magnitude of these extra losses due to reversing? No reasoning based solely on the implications of the rating will indicate the reversing ability of a particular motor without knowledge of some of the details of design. We can assume that if the reversing losses are greater than the normal losses at full load, the motor is carrying the equivalent of an overload and normal temperature margins are reduced.

Let us work out a specific case, say of a class *A* five-horsepower, 1,800-rpm motor with 85 per cent full-load efficiency and with total WR^2 including load, 1.5 pound feet squared. Assume that the total reversing losses are $2^1/_4$ times the rotor losses. The loss in watt minutes for one reversal is:

$$4 \times 3.87 \times 10^{-6} \times 1.5 \times (1800)^2 \times 2^1/_4 = 168$$

The loss due to normal operation at 1.15 load, as permitted by the service factor is:

$$5 \times 1.15 \times 746 \times 0.15/0.85 = 767 \text{ watts}$$

Thus one interpretation of the rating indicates that the motor can be reversed with this inertia, 767/168 = 4.5 times per minute without exceeding normal temperature.

A general formula based on this reasoning is:

$$(WR^2)\,(\text{rpm})^2N = \frac{4.82 \times 10^7 \text{ (service factor) (horsepower rating) } (1 - \text{eff.})}{K \times \text{eff.}}$$

K is the factor dependent on design, mentioned above. Note that for a given speed $(WR^2)N$, that is, the product of the moment of inertia and the number of reversals is a constant. This is well verified by many tests. Note also that this is an optimistic estimate of reversing ability. We have assumed that the motor will dissipate as much loss due to reversing as the losses due to continuous operation. If reversed very frequently, the ventilation is impaired as the average speed of the ventilating fan is decreased. This is analogous to continually opening and closing outlet valve *C*, figure 8. In deriving the formula for losses due to reversing, we have neglected friction and windage losses, core loss, and magnetizing current together with extra I^2R loss in the stator caused thereby. This is analogous to closing permanently spigot *E*, figure 8. For high-speed motors, this is not serious. However, it is a considerable factor for slow-speed motors. In our analogy the tank is already partly filled by these neglected losses. Only the remainder is available for reversing losses. Thus while the number of reversals with a given WR^2 is increased with lower speeds, the increase is not proportional to the square of the speed ratio as suggested by the above approximate formula.

Table I shows reversing ability of a variety of class *A* motors in accordance with the assumptions above.

The assumed WR^2 includes both motor and load.

N is the number of reversals per minute to give the same losses as normal operation at 115 per cent load.

This table has been prepared primarily to indicate that frequent reversing of standard general-purpose motors constitutes an overload beyond the implications of the name plate. There is not a general recognition of how much this overload is. Of course more reversals than tabulated can be obtained if the inertia of the load is small, thus reducing the total WR^2. Other kinds of motors are better adapted to frequent reversing. The class *C* motor will give distinctly more reversals and still maintain good efficiency in the operating range. Class *D* motors will give maximum number of reversals but the efficiency is lower at normal speed.

Of course, special motors can be made primarily for reversing service to give many more reversals. The principles involved are well known to designing engineers. No discussion of these is included here, as this paper is primarily concerned with the use of general-purpose motors.

It should be noted that horsepower rating as generally understood is an unsatisfactory criterion of reversing ability. In fact, it may be distinctly misleading. A small motor will give more reversals than a larger homologous motor if the load WR^2 is small. In general, those factors which are conducive to obtaining high reversing ability are exactly contrary to maintaining the normal margins for normal operation usually associated with a given horsepower rating. In fact, strange as it may sound, a lower horsepower rating is frequently consistent with greater reversing ability. One such case is associated with the reversing of slow-speed motors. See figure 8. The tank is already partially filled by no-load losses from spigot *E*. If no-load loss fills the tank three-quarters full, only one-quarter is left for reversing. Now if the torque ability is decreased to two-thirds of its former value, the tank is only half filled by no-load loss leaving half a tank for reversing. Thus, the reversing ability from a temperature standpoint is doubled, but the normal margin in torque ability is sacrificed.

Starting High-Inertia Loads

We have discussed frequent starting and reversing where the instantaneous temperature is close to the average. Starting high-inertia loads such as extractors imposes extra transient temperatures higher than the average. The average temperature rise is determined by the same considerations as for rapid reversing jobs. Figure 9 shows a hydraulic analogy. It differs from figure 7 in that the tank is divided into three compartments to simulate the separate heat storage capacity of the stator and rotor windings and core. The three compartments are connected by ports so that ordinarily no great difference in level can exist, that is, the temperatures of the parts of a motor are quite uniform for ordinary operation.

In figure 9 the extra inrush of water into the stator and rotor compartments simulates the extra starting loss in the motor due to starting a high-inertia load. The rotor losses flow into the rotor reservoir and the stator losses into the stator compartment. The loss is proportional to the load WR^2, hence high. All compartments are already partially filled. The extra inrush may cause one to overflow before the water can run into the others or be dissipated. Consider for example, the rotor losses which are: $3.87 \times 10^{-6}(WR^2)(\text{rpm})^2$ watt minutes. The size of the rotor reservoir is (weight of rotor windings) × (specific heat) × (safe temperature). What is a safe rotor temperature? Some of the factors which must be considered are melting point, latent heat of fusion, tensile strength at high temperatures, deterioration of alloys by change in composition or crystalline structure. It is a coincidence that the size of the rotor reservoir is about the same for the different commonly used materials. Copper will stand high temperatures but the weight for satisfactory characteristics (high resistance) is low. Aluminum melts at a lower temperature but it has outstandingly high specific heat and latent heat of fusion. Brass is heavy for a given resistance but it will not stand repeated high temperatures.

The transient stator temperature is determined by similar details of design. Of course, even transient temperatures must be held to values which will not wear out the insulation too rapidly. It is quite difficult to determine the peak transient temperature. In figure 9 we indicate the analogy to a thermometer inserted on the stator winding. A thermometer reads a temperature because its mercury gets hot and expands. Thus there must be a thermal lag in reading with rapidly changing temperatures as illustrated by the small passage between the thermometer and stator reservoirs.

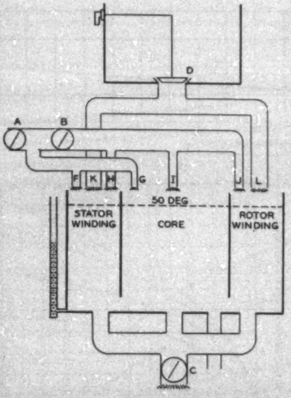


Figure 9. Hydraulic analogy illustrating extra losses due to starting a high-inertia load

These details have been discussed to indicate that design details rather than horsepower rating determines the ability of a motor to start and operate machines with high inertia. Standard motors are not ordinarily used for such service. Standard motors are designed for good characteristics at normal speeds. What ability they have to reverse frequently or to start high-inertia loads is a variable by-product of design.

Built-in Motors

A short statement is necessary regarding the operation of any motor on any job when that motor is built in as a part of a complete machine. In our hydraulic picture, the motor designer controls the size of the inflow spigots, *A*, *B*, and *D*. The machine designer controls the size of the outflow spigot. The user determines how frequently and how much the valves are opened. No one of the three can say how full the tank will be. This indicates that the performance of such motors from a temperature standpoint is a matter of divided responsibility.

Summary

1. When selecting motors for duty-cycle jobs, the two concepts, torque ability and temperature rise should be considered separately. The class or type and the rating should be such that there is no deficiency or undue extravagance in starting, accelerating, or breakdown torque ability. If the root-mean-square load is appreciably less than the motor rating required from a torque standpoint, a proper short-time-rated motor is economical.

2. Frequent starting and reversing or starting high-inertia loads frequently imposes the equivalent of an overload from a temperature standpoint.

3. The data and analogies given in the paper show that performance of a motor on starting, reversing, or other duty cycles is not clearly evident from the horsepower rating alone. The usual method of giving a motor a short-time rating is also inadequate for determining its performance on duty-cycle jobs.

4. It is suggested, therefore, that motors for duty-cycle service be given horsepower ratings representative of their over-all torque ability regardless of heating. It is suggested that service factors be used to indicate the load they can carry continuously within their proper temperature limits. Under this plan, a motor now rated one hour 50 degrees, would become a motor with the same horsepower rating but with, say, 0.8 service factor. This proposal forms a logical extension of the present plan of giving service factors higher than unity to general-purpose motors applied in continuous service on special applications.

Appendix A

Rotor loss during speed transients

$$T = J\frac{d\omega}{dt}$$

$$Tdt = Jd\omega$$

Rotor power loss $= T(\omega_s - \omega)$

Rotor energy loss

$$= \int_{T_1}^{T_2} T(\omega_s - \omega)dt$$

$$= J\int_{\omega_1}^{\omega_2} (\omega_s - \omega)d\omega$$

$$= J\left[\omega_s\omega_2 - \omega_s\omega_1 - \frac{\omega_2^2}{2} + \frac{\omega_1^2}{2}\right]$$

If $\omega_1 = 0$, $\omega_2 = \omega_s$, that is, start from rest and accelerate to full speed, rotor energy loss $= J\omega_s^2/2$.

If $\omega_1 = \omega_s$, $\omega_2 = -\omega_s$, that is, a complete reversal, rotor energy loss $= 2J\omega_s^2$.

Converting to watt minutes, WR^2 in pound feet squared and revolutions per minute, watt minutes rotor energy loss for one reversal is

Watt minutes $= 1.55 \times 10^{-5}(WR^2)(\text{rpm})^2$

T = torque in pound feet
J = moment of inertia in pound (gravity) feet²
ω = angular velocity in radians per second
ω_s = synchronous angular velocity
WR^2 = pound feet²
t = time
T_1 and T_2 = initial and final time

Discussion

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Hildebrand's paper showing how difficult it may be to predict the temperatures in a motor of varying duty cycle suggests how fortunate indeed is the builder and user of fractional-horsepower motors. The maker of refrigeration or air-conditioning equipment invariably builds one or more samples of his appliance which can be subjected to all kinds of tests in the laboratory, both at elevated and at subnormal ambient temperatures. Usually these tests involve the use of a large number of thermocouples and one more can readily be added to the motor winding. Thus it is possible to determine by laboratory test, just what the winding temperatures of the motor will be in service. The problem is even simpler for the refrigeration manufacturer if he uses a motor properly equipped with an inherent overheating protector which allows the maximum useful output to be obtained from the motor windings, without endangering the windings whatsoever.

Temperature Limits Set by Oil and Cellulose Insulation

CHARLES F. HILL
MEMBER AIEE

Synopsis: The life of cellulose insulation in oil has been investigated as a function of temperature under conditions of free access to oxygen and also in an inert atmosphere. Temperatures up to 140 degrees centigrade have been used. An attempt has been made also to study the life of oils as a function of oxygen concentration in an actual transformer at various temperatures. The amount of oxygen to produce a given acidity was also determined. On the basis of these data on oils, an attempt has been made to calculate rates of oxygen absorption by oil which may be used to estimate the relative deterioration in transformers of other dimensions. The results show cellulose deteriorates only mechanically, retaining its electrical properties. It is subject to both temperature and oxidation effects, the temperature effect, of course, taking place above 105 degrees centigrade.

THE continuous rating and also the overload rating of electrical apparatus is largely determined by the insulation. Fortunately, in transformers the very large heat capacity of the cooling liquid plus a high rate of heat transfer to the liquid from the metal parts permits a high overload for short periods of time, but even there, the solid insulation should be protected from too excessive temperatures. It must be recognized that a large fraction of the strength of the solid insulation can be lost in a short time at temperatures which can exist under overload condition. This has been recognized in some instances by the addition of protective devices, thermally operated.

The temperature at which these devices should operate and also the continuous operating temperatures, which would permit a reasonable life of the insulation, are as yet rather arbitrarily fixed by accumulated experience and short-time tests. Conclusions from operating experience are not based on ideal conditions. The amount of insulation deterioration permissible is also an undetermined factor in predicting insulation life. Montsinger[1] and Bush[2] have attempted to determine rates of deterioration which could be extrapolated with time to predict the life of insulation as a function of temperature. A recent paper by Putman and Dann[3] will also be of interest, particularly from the standpoint of overload temperatures.

It is not the purpose of this paper to draw very definite conclusions as to temperatures permissible, but rather to separate the deterioration processes which affect oil and cellulose insulation. Some idea of the temperature limits imposed by these separate processes may be obtained and possibly some conclusions can be drawn from the combined effects. An attempt has been made to cover both ideal and adverse conditions of operation.

Insulating oils and cellulose have been used almost exclusively in transformer equipment as insulation. Because of this and because of their very well-known limitations, an enormous volume of research has been done on these materials, which, when we consider the extent of the research and the improvements in the materials, is somewhat discouraging. Their limitations remain the same. The conclusion is again, that improvements in their behavior in transformers will be gained largely by providing better conditions under which they are to operate.

All of the materials to be considered here are organic in nature, but they differ somewhat in their behavior toward conditions existing in transformer operation. For example, the cellulose molecule contains hydroxyl groups which under severe temperature conditions, may be split off as water. "Chemically combined water" is the term usually applied to such hydrogen-oxygen combinations. Oils, on the other hand, are not subject to such deterioration as they do not contain oxygen in their pure state. They contain only hydrogen and carbon, but are capable of reacting with oxygen to form peroxides and organic acids and other deterioration products. Some of these oxidation products may also polymerize and sludge, a final product, is the result. The peroxides in particular and perhaps some of the low-molecular-weight acids, react with cellulose to cause its decomposition.

Oils in the absence of oxygen can withstand temperatures far in excess of any permissible operation conditions, but the presence of oxygen becomes a serious handicap. The results to be presented

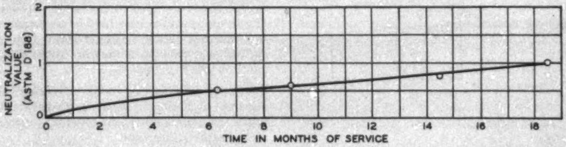


Figure 1. Acidity of transformer oil at 85 degrees centigrade—allowed to breathe once per week

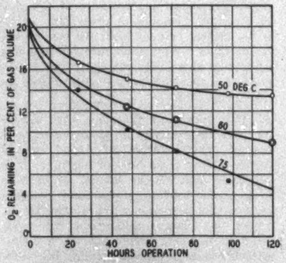


Figure 2. Residual O_2 in gas space

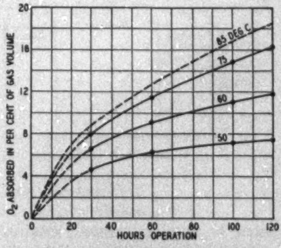


Figure 3. O_2 absorbed from gas space

Paper number 39-16, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 8, 1938.

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The author is indebted to J. G. Ford of the Westinghouse research laboratories for much of the experimental data.

1. For all numbered references, see list at end of paper.

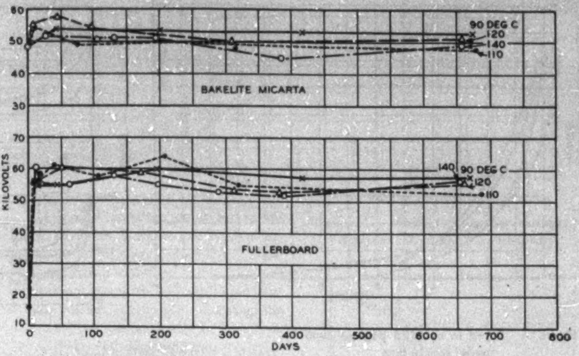


Figure 5. Breakdown tests

are given with the intention of emphasizing the oxygen hazard.

The experiments which concern oil alone are of interest in that they show a new method of attacking the problem of determining rates of oxidation. They also apply directly to oil in transformers which is somewhat more convincing than laboratory tests. The oxidation also takes place by contact of oil with air as it exists in the transformer. Two transformers were operated with the same conventional transformer oil. Figure 1 shows the acid accumulation in a 100-kva unit containing 93 gallons of oil, for 19.5 months with approximately one day per week off load. The one milligram KOH acidity was thus produced in approximately 16 to 17 months actual time at 85 degrees centigrade. Its oxygen absorption was 0.48 cubic foot per week or per cycle. The surface of the oil had an area of 3.47 square feet, or an average of 0.138 cubic foot of oxygen per square foot of oil area per week.

The second transformer was operated until the oil had passed its initial latent period when the gas space was blown out and sealed with a fresh air supply. It was then operated for several days, the residual oxygen in the gas space being determined periodically.

Figure 2 shows the data for 50, 60, 70 degrees centigrade for 120 hours and figure 3 is the same data graphed in terms of oxygen absorbed. The 85-degree-centigrade curve was obtained by extrapolation of the intercepts of the other curves. The rates of oxygen absorption of figure 4 are obtained by taking the average at points on the curve for a 20-hour period, 10 hours on each side of the point in question. The data of figure 4 are not to be considered highly accurate, but are approximate values which may be used in estimating oxygen absorption as a function of oxygen concentration. This transformer was operated until the average oxygen consumption could be determined which would produce a definite acidity. This amounts to 0.4 cubic foot of oxygen per gallon of oil for one milligram KOH acidity.

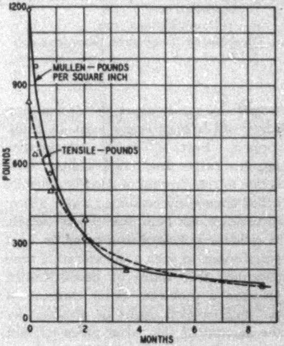
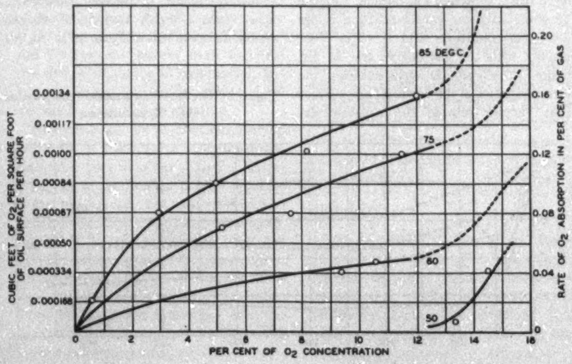


Figure 6. One-eighth-inch fullerboard in oil at 95 degrees centigrade—in air

Figure 4. Rate of O_2 absorption



An interesting calculation can be made by assuming the oxygen absorption in the larger transformers takes place chiefly at the oil surface or is proportional to the area. It is recognized that much oxygen absorption takes place in the oil condensed on the tank walls, but in general these will be proportional to the oil area. The hourly rate in the large transformer was 0.138 cubic foot per square foot per six-day week or 0.0009 cubic foot per square foot per hour. Calculating the rates on the vertical axis of figure 4 in terms of cubic feet per square foot per hour it is found that the intercept of 0.0009 from this axis on the 85-degree curve is at about six per cent oxygen concentration. The conclusion is that a six per cent oxygen concentration continuously will give a one milligram KOH acidity in such a transformer in 16 or 17 months. A one per cent concentration at the same temperature would give an absorption of 0.000168 cubic foot per square foot per hour. This will require 5.4 times as long or about 86 months to give the one milligram KOH in the same transformer. The ratio of oil volume to surface determines the rate at which acidity builds up in the oil for any otherwise constant condition.

Life Tests on Cellulose

Cellulose, like oil, is subject to oxidation. Unlike oil, it shows deterioration

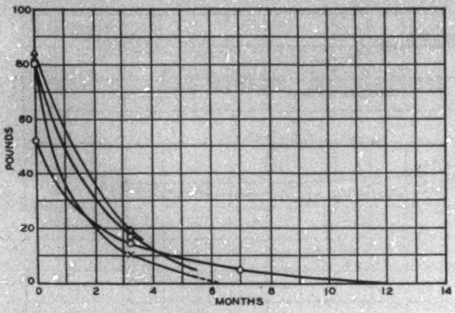


Figure 7. Aged in air (125 degrees centigrade) tensile tests

○—Untreated cotton tape
×—Yellow varnished tape
□—Number 3 varnished tape—one dip
Δ—Number 3 varnished tape—two dips

from temperature alone and at temperatures which may easily exist in transformers. Further, rapid deterioration sets in at rather fixed temperatures. This is due to the removal of what is generally called chemically combined water which, if carried to the limit, leaves only carbon. If cellulose is heated in a perfectly tight transformer to such a deteriorating temperature, this water of decomposition becomes a problem. Some measure of the deterioration might be obtained by the amount of this water given off, but in the end such data would have to be correlated with the properties of cellulose which are important to its use; namely, its mechanical and electrical properties.

In the present study, some impregnated cellulose materials have been included as well as untreated cellulose. The samples were immersed in oil, and the combination exposed to various temperatures and conditions as shown. The data are recorded in figures 5 to 12. Some samples were heated in contact with oil in air while a special large group was tested in oil in a nitrogen atmosphere, by placing them in small containers which in turn were in large, gas-tight tanks. The nitrogen atmosphere was obtained by a small constant flow from a tank, the oxygen content kept to a low value of the order of 0.1 per cent. The tank at 110 degrees centigrade gave erratic results due to lack of temperature and oxygen control early in the test, but some of the data are shown.

Several mechanical tests were used on the insulations with the most emphasis on the tensile strength. The Mullen test is a bursting test as used on fibrous materials for containers. Some attempt was made at embrittlement measurement and it is believed more emphasis should have been placed on such tests, particularly on treated fabrics. The compression test on shellac Micarta tubes (figure 10) demonstrated this to some extent. Electrical breakdown and resistance constituted the electrical tests.

The conclusion from this set of life tests is demonstrated by a few sample data. Electrically, both treated and untreated cellulose maintain their initial characteristics and may even improve. This is true up to 140 degrees centigrade in the inert-gas tests. By the mechanical tests, however, the untreated cellulose is shown to deteriorate rapidly above 100 degrees centigrade. The deterioration is rapid at 95 degrees centigrade with the oil exposed to the air.

A further example of this mechanical deterioration is found in figure 12 where paper tape is tested at 90 degrees, 120 degrees, and 140 degrees centigrade in a nonoxidizing insulating liquid. The tests were made in sealed containers. Figure 10 gives data on varnished tape under inert gas conditions and is to be contrasted with the 125-degree test in air, figure 7. Figure 11 demonstrates the heat resistance of the varnish and possibly that varnish retards the cellulose deterioration under heat. At any rate, the combination retains its mechanical strength.

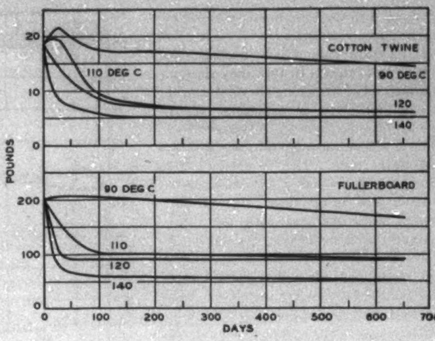


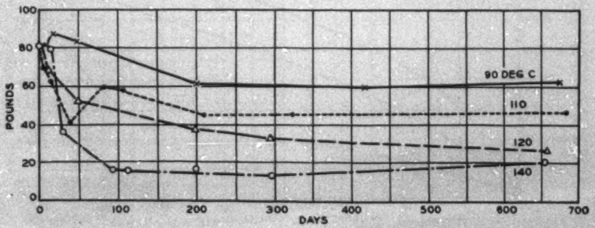
Figure 8. Tensile strength, aged in oil—N_2 atmosphere

Figure 5 and figure 11 demonstrate the type of results on electrical test; namely, that electrically, the insulations retain their properties and even improve.

Figure 6 and figure 7 show the very rapid deterioration of materials in oil exposed to air. This deterioration takes place on the untreated cellulose even before serious acidity is developed in the oil, due, we believe, to the effect of oxygen carried to the cellulose by the oil itself through the formation of peroxide.

Figure 8 is a further demonstration of the mechanical deterioration of cellulose under heat and shows the existence of the critical range of temperature between 90 degrees and 110 degrees centigrade. This is further borne out by the data of figure 12 when the cellulose was again exposed to temperature in a nonoxidizing liquid in sealed containers. There was a small amount of oxygen available in the test of figure 12 as the gas

Figure 9. Shellac Micarta tubes—compression test, N_2 atmosphere



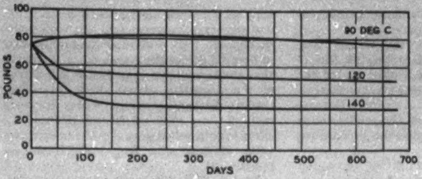


Figure 10. Yellow treated tape—tensile strength; N₂ atmosphere—in oil

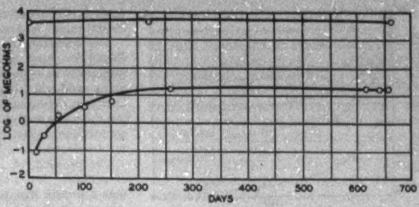


Figure 11. Number 1 varnish cloth—110 degrees centigrade; N₂ atmosphere

above the liquid was air in about the same ratio to oil volume as is found in a sealed transformer.

The data of figure 9 obtained by the force to collapse shellac-paper tubes and also some bending tests in which strips of treated materials were bent to rupture, showed definitely the hardening and embrittlement of impregnating material.

The conclusions from these tests on cellulose and impregnating materials when these tests are combined with experience at lower temperatures, can be condensed into a rather general statement; namely, that up to approximately 90 degrees centigrade, the life of such materials is satisfactory. Above that temperature, mechanical deterioration on bare cellulose becomes rapid, but impregnations protect cellulose to some extent at the elevated temperatures. It is also to be concluded that oxygen restriction is very important to cellulose as well as to oil, and that, while heat alone will mechanically deteriorate the solid insulations at 100 degrees centigrade and above, the presence of oxygen produces deterioration of both oil and cellulose below this temperature.

The method by which oxidized oil affects cellulose is not well understood. The usual impression is that the organic acids are the cause of deterioration and no doubt they have some effect. Stager[4] and Frances and Garrett[5] have discussed this and oil oxidation in more detail, but it now appears the earlier stages of oil oxidation are the more effective.

Predicting the life of a transformer on the basis of the rate of mechanical deterioration is somewhat difficult. However, the insulation is usually used in compression and not for mechanical binding and experience shows that transformers continue to operate after the insulation has reached the very serious apparent deterioration of one-fourth or less than one-fourth of its initial value. This last statement is true for even the best varnished insulations in which the varnish is largely a surface coating.

The results on cotton twine and fullerboard in figure 8 where the oil was exposed to a nitrogen atmosphere, show that untreated cellulose seems to reach a minimum strength of about 25 per cent at 140 degrees centigrade. Further, the decrease took place largely in 100 days. It has since been suspected that the residual oxygen absorbed in the oil may have played a part in this early deterioration, but that is yet to be proved. Our conclusion would be that several hundred hours of operation at 120 degrees to 140 degrees could be permitted for the conditions of an oxygen-free space which may be sufficient to take care of overload periods. Figure 12 gives somewhat the same type of results for the fireproof liquids except that the deterioration was somewhat more. There, however, a small amount of oxygen was present. The leveling off of the mechanical deterioration is largely a dehydration effect in the nitrogen-atmosphere tests.

The extrapolating of such data on solid insulation would seem questionable since it is believed conditions and rates of deterioration change from time to time. It is also uncertain what limits should be reached in an extrapolation of mechanical deterioration. It is the author's opinion that a very large number of transformers in service for a period of years contain fibrous insulation with mechanical strength reduced to 50 per cent of the initial value and in some cases, less than 25 per cent, but function satisfactorily. Some of the insulation from the tests at 140 degrees centigrade in oil with an inert atmosphere would no doubt operate satisfactorily. Such insulation retains good electrical properties. The intrusion of moisture is not to be considered a deterioration, but rather a contamination.

Summary

The experimental data given attempt to show two types of deterioration of cellulose or other organic insulations in transformers. Below 90 or 95 degrees centigrade, such solid insulations would have a very long life under an inert atmosphere.

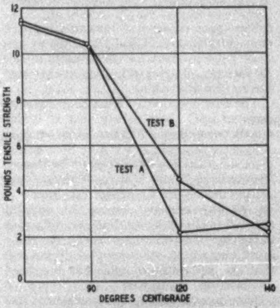


Figure 12. Ninety-six-day tests $2^1/_2$-mil paper tape in nonoxidizing liquid

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Discussion

V. M. Montsinger (General Electric Company, Pittsfield, Mass): I shall confine my remarks to the life tests on cellulose. The author has reported some very interesting results, the most important of which are (1) that if oxygen is not present cellulose will undergo very little deterioration either mechanically or electrically up to approximately 90 degrees centigrade or 95 degrees centigrade, and (2) that it retains a fair degree of its mechanical strength for many months even at 140 degrees centigrade. The peculiar thing about the results of Doctor Hill's tests is that after approximately 100 days there is apparently no further weakening of the insulation up to 650 days even at 140 degrees centigrade. This is difficult to understand, because we have been led to

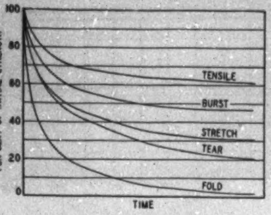

Figure 1. Mechanical deterioration of class A insulations as indicated by tensile, burst, stretch, tear, and folding strengths

believe that all class *A* insulations deteriorate with both time and temperature.

As I see it, there are two phases of this old question of aging of insulation that need further consideration.

The first question is, what causes insulations to age? Just to say that it is due mostly to oxidation does not go far enough. I recently showed, in my discussion of the paper on "Loading Transformers by Copper Temperature," by Messrs. Putman and Dann, the results of aging tests made in oil (1) with nitrogen atmosphere and (2) exposed to air, with practically no difference in the rate of deterioration, which was fairly rapid under both conditions. I do not feel that the mechanism of aging has ever been satisfactorily explained.

Then second, having determined the real mechanism of insulation deterioration, the next problem is to adapt the condition showing the least aging effects to apparatus under service conditions.

If, as shown in this paper, by merely keeping oxygen away from class *A* insulation its temperature can be increased to say 120 or 140 degrees centigrade, it might change materially our present practice of rating and loading apparatus. I do not believe that we are in a position yet to make any radical changes in temperature limits for class *A* insulations, and I believe that Doctor Hill will agree with me on this point. We need further confirmation of these new and very interesting results.

I note that the author's findings on two points agree with what I have found in the past, namely (1) that dielectric strength is no criterion by which to judge the degree of mechanical aging of insulation and (2) that the best method of measuring the amount of mechanical deterioration appears to be in comparing its tensile strength. My experience is that the effect of aging on mechanical strength is least upon the tensile strength. The order of magnitude of the effect ranges from tensile, to burst, to stretch, to tear, and is greatest for the folding test. This takes into account tests made on the principal kinds of transformer insulations including varnished and unvarnished papers, cloths, and pressboards. The wide variations obtained by these different methods of testing are shown in figure 1 which shows that folding tests indicate a very short life as compared with the life as indicated by tensile tests. It might appear that we should use the method which shows the greatest amount of deterioration—folding test. Experience, however, indicates that tensile strength comes more nearly representing the actual life of insulation as used in apparatus.

The question of the kind of test that should be used to determine the life of insulation in apparatus is only one of the many problems confronting an investigator. Generally speaking, no two investigators use the same methods and the results are so contradictory that no definite conclusions can be drawn.

The problem of investigating the aging of insulation is so important to the industry that I would like to suggest that the Institute set up the proper technical committee whose duty would be to correlate the work on the aging of insulation, and recommend standard methods of carrying on the work.

F. M. Clark (General Electric Company, Pittsfield, Mass.): The paper by Doctor Hill constitutes another important contribution to the problem of dielectric deterioration. Data in this field are rapidly accumulating and despite the wide differences noted at times, a clearer understanding is being obtained with respect to those factors which are important for the successful operation of commercial machines. Unfortunately, the problem of setting up temperature limits covering commercial operation of oil or cellulose or oil-treated insulation involves carefully controlled studies over long periods of time. I am, however, inclined to disagree with Doctor Hill when he states that data obtained in commercial equipment is somewhat more convincing than laboratory tests. Oxidation and pyrolysis tests are so affected by factors which are controlled only with difficulty even when known to be present that data obtained on large scale tests can be accepted as reliable only when supported and explained by accurately determined laboratory results. This is especially true for tests on mineral oil and cellulose.

Oxidation is, of course, an important factor in the successful operation of electrical apparatus and is an ever present threat which must be included in the life evaluation of organic insulation despite the varied methods which have been suggested for its elimination. But oxidation is not the only threat to the successful operation of cellulosic insulation at the higher temperatures of operation. This has been pointed out by Doctor Hill but I believe that the effect of nonoxidizing high-temperature exposure warrants greater consideration. Doctor Hill states that "heat alone will mechanically deteriorate the solid insulation at 100 degrees centigrade and above." This is true. In a paper which I presented before the Institute a few years ago entitled "The Pyrochemical Behavior of Cellulosic Insulation" (ELECTRICAL ENGINEERING, volume 54, October 1935, pages 1088–94) it was demonstrated that cellulosic insulation when exposed to high temperature bears the stamp of that exposure even though the high-temperature treatment be carried out under nonoxidizing conditions. The effects of this exposure fundamentally are chemical, and result in the chemical degradation of the cellulose, gas evolution, and deteriorated dielectric properties. These dielectric effects are especially noticeable for tests carried out on what are usually considered the highest type of dielectric materials such as cable and papers. I suggest that the reason the breakdown tests presented by Doctor Hill in figure 5 do not show such deterioration is that the breakdown examined is a function of the preceding corona discharge. This corona discharge usually occurs in tests such as those cited by Doctor Hill and its effect very largely determines the breakdown value, masking other factors which may be present.

Doctor Hill differentiates between the thermal aging of insulation at temperatures above 100 degrees centigrade and the oxidation aging of cellulose below 100 degrees centigrade. The part played by mineral oil in the aging of cellulosic insulation at 100 degrees centigrade and below has been a debatable question. Doctor Hill apparently agrees with those who claim that acids formed in the oil as a result of oxidation are factors contributing to the mechanical weakening of the insulation. Doctor Hill goes further and suggests that the "deterioration takes place on the untreated cellulose even before serious acidity is developed in the oil, due, we believe, to the effect of oxygen carried to the cellulose by the oil itself through the formation of peroxide." This is important if true. From the data submitted by Doctor Hill it is impossible to decide the validity of this assertion. That it may be true is indicated by a comparison of figures 6 and 12. Figure 12 concerns itself with paper tape immersed in a nonoxidizing and noninflammable liquid in a sealed container in contact with air, and figure 6 describes tests on fullerboard immersed in oil presumably with free exposure to air. However, Doctor Hill presents no data concerning tests on paper tape under oil similar to the tests described in figure 12. Presumably the amount of air in the sealed container of figure 12 constituted about 10 per cent to 15 per cent of the total volume of the container so that enough oxygen was present to give some oxidation effects if oxidation were an important factor with cellulose immersed in noninflammable type of insulating liquid. In such a material peroxide effects are definitely absent. The oxygen cannot be used up by the oxidation of the liquid which is chemically inert. Therefore, the effects of cellulose oxidation can be obtained only by the migration of the oxygen through the inert noninflammable liquid. This might be expected to retard the mechanical deterioration due to oxidation. Figure 12 confirms this expectation and shows that the paper tape under the noninflammable liquid shows no substantial change in mechanical strength after 96 days at 95 degrees centigrade. Figure 6 shows that the mechanical strength of the fullerboard in oil fell about 75 per cent during the same interval. These data may well indicate that the noninflammable liquids because of their chemical stability and nonoxidation characteristics effectively reduce the tendency of cellulosic insulation to deteriorate mechanically at temperatures of operation below 100 degrees centigrade where oxygen-free atmospheres have been shown by Doctor Hill to be of value for mineral-oil-immersed insulation.

There can be but little doubt that oil acids must not be ignored in the evaluation of the mechanical characteristics of oil-immersed cellulose. Mineral oil when oxidized produces oil-soluble products which are capable of absorbing alkali. These materials are usually classed as oil acids. Total oil acidity as illustrated by Doctor Hill in figure 1 does not appear to me to be of major importance. What does appear of importance is the formation of water-soluble organic acids. These are present to an extent dependent on the type of oil base and on its refining treatment. With the highly refined (white) oils the water-soluble corrosive acids predominate. With a carefully selected oil whose refining treatment has been properly supervised these water-soluble corrosive acids are present in only negligible amounts. The variation in the formation of these water-soluble corrosive acids may account for the wide differences which are at times reported in the literature covering the mechanical aging of oil-immersed cellulose.

Acetic acid is one of the corrosive organic acids evolved by the oxidation of highly refined white oils. It is this type of acid which is formed by the exposure of cellulose to high temperatures even in the absence of oxygen. Cellulose evolves a variety of degradation products whose formation begins slowly at relatively low temperature. These are in general poor dielectrics and of strong chemical reactivity. The presence of these products retained in contact with the cellulose accelerates further deterioration, a weakening in mechanical strength, and a decrease in dielectric efficiency. For high-temperature operation these effects appear to predominate. For lower temperature operation (80 degrees centigrade to 90 degrees centigrade) the most important factor affecting the mechanical life of cellulose based on the data of Doctor Hill appears to be one of oxidation. This apparently can be reduced and possibly eliminated by the use of nitrogen atmospheres to replace air or oxygen or by the use of a nonoxidizing, noninflammable liquid.

H. V. Putman (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): The thing which strikes me as most significant about Doctor Hill's results is the extremely wide range in the effectiveness with which the oil and insulation are protected against deterioration in different types of transformers. Using Doctor Hill's data, Mr. Vogel has shown that the life of the oil in transformers under nitrogen may be 50 times as long as with open-air breathing.

If we examine Doctor Hill's figures 7 and 10, we see that yellow treated tape in oil under air deteriorates as much in a month as it does in oil under nitrogen in two years.

And yet our present standards establish the same maximum temperature limits and short-time overload capacities for all types of transformers. Obviously those having effective means for protection of oil and insulation can stand higher temperatures and more severe overloads than those not having such means.

I am not prepared to say that we should increase the standard temperature rise of 55 degrees centigrade for transformers. Perhaps we should outlaw open-air breathing. Mr. Hellmund has pointed out that perhaps the reason European manufacturers are successful with a higher temperature rise is because all their transformers large and small are protected by some kind of a conservator.

It does appear in order to suggest that the AIEE transformer subcommittee undertake a review of our present rules affecting maximum temperatures and overloads with a view to bringing them into harmony with the engineering facts established in Doctor Hill's paper.

H. C. Louis (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): The paper by C. F. Hill, presents the results of some very elaborate laboratory tests showing the effects of temperature and aging on the electrical and mechanical qualities of insulation. The results add much information useful in the design, rating, and operation of electrical apparatus as related to insulation. They contribute fundamental data of value in the important and much debated subject under consideration, the allowable temperature limits of insulation, and whether those in existing standards should be modified.

We note with particular interest that in these carefully controlled laboratory tests, in some cases of definitely marked deterioration of mechanical characteristics of insulation the electrical characteristics did not deteriorate correspondingly, and continued to be apparently fairly good. This checks with a condition sometimes experienced in actual operation in cases where apparatus seems to be able to operate satisfactorily and reveals no weaknesses on electrical tests, although inspection may show the insulation to be in very questionable mechanical condition.

This question of determining the condition and expected additional life of insulation of used apparatus is one of practical operating concern, involving reliability of service and economics. Development and investigations of field methods for checking the condition of insulation by special tests and inspections should continue to be encouraged.

The correlation of the data from the paper under discussion with others relating to the same subject, as recommended by Mr. Montsinger, should therefore serve not only the primary purpose of the question of temperature limits and ratings, but should also contribute useful information in the problem of judging by tests and inspection the condition of insulation of apparatus in use, and the possible additional life of same.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As already briefly stated by Mr. Putman, the data presented by Doctor Hill in his paper and other calculations given by Mr. Vogel in his written discussion seem to explain a controversy of long standing with the International Electrotechnical Commission regarding permissible temperature ratings of transformers. The European countries wish to standardize on temperature-rises somewhat higher than those considered safe by the American representatives. The early assumption that the European practice might be less conservative can hardly be considered correct, because the scarcity and high price of oil in Europe make it necessary for them to be more economical in the use of oil than it is in this country. A slight difference in climatic conditions might, of course, have some influence on their service experience. However, it seems that on account of the greater need for preservation of materials, expansion tanks have been used in Europe to a great extent for transformers of even the smallest sizes. In contrast to this, in this country the distribution transformer has been used until fairly recently without such tanks and with the surface of the oil freely exposed to air and oxygen. It seems from these newly available data that they may have obtained service results as good as or even better than those we have obtained in the past, even though their rating standards include higher temperature-rises. Although it may be premature for us to change our rating standards at this time, it seems advisable in our international contacts to recognize these differences and to make some concessions in connection with the work of the IEC.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper adds considerably to our knowledge of oil-impregnated insulation. The effects of temperature are shown to vary appreciably depending on the conditions of test and test procedure. Nevertheless the paper brings out again the point that when class *A* insulation is operated continuously at temperatures of 90 or 95 degrees centigrade or higher the rate of deterioration increases fairly rapidly. In other words, a short life will obtain when the insulation is operated continuously at the present "hot spot" temperature limit of 105 degrees centigrade of the AIEE rules.

Doctor Hill's data show that when testing paper under oil which is exposed to air the oxidation products of the oil will affect the strength of the paper. This test procedure is satisfactory, I presume, if it simulates the service condition. For some equipment, such as capacitors and cable, the oxygen, however, is pretty well removed from the insulation in manufacture and kept so during operation.

In connection with his figure 12 showing the decrease in tensile strength with temperature, it seems to me that the criterion of tearing strength is most valuable to use in such tests. This test gives a large range in results of tests of deteriorated material and the results seem to be most consistent. Probably the folding endurance test, as developed in the research at Massachusetts Institute of Technology about 15 years ago, is too much influenced by moisture, both free and combined, to be reliable.

F. J. Vogel (Westinghouse Electric and Manufacturing Company, Sharon, Pa.): Doctor Hill's paper should be of considerable interest to all users of transformers because it gives basic data which can be used to estimate the life of oil and insulation under various conditions of operation. It shows how extremely important it is to protect transformer oil against oxidation, particularly when the oil is operated at high temperatures. It shows that even small amounts of oxygen continuously admitted to the transformer tank over long periods of time can seriously shorten the life of the transformer oil. Consequently, it is important that gasketing be done in the best possible and most permanent manner. A positive pressure of nitrogen ranging from one-half pound upward is further assurance against the possibility of oxygen or air entering the transformer tank.

Doctor Hill has shown that deterioration of insulation is a function both of temperature and oxidation, while the deterioration of the oil is primarily a function of oxidation, which, fortunately, can be eliminated almost completely by the use of nitrogen above the oil level.

For example, Doctor Hill shows that 0.4 cubic foot of oxygen per gallon of oil will produce an acidity of one milligram of KOH. Sludging is usually considered to begin when the acidity reaches 0.5 milligram of KOH, which would correspond to 0.2 cubic foot of oxygen per gallon of oil. For comparative purposes it is interesting to calculate the time required to produce this amount of acidity in the oil under various conditions of breathing in a typical transformer. As an example, we have used a 2,500-kva unit containing 680 gallons of oil, having an 88-inch oil level, with 10-inch gas space and 16.4 square feet of oil surface. This transformer would require 136 cubic feet of oxygen to produce 0.5 milligram of KOH acidity in the oil. We know from experience that a transformer of this size would use from 1 to $1^1/_2$ cylinders of nitrogen per year, or, roughly, 275 cubic feet. This gas is purchased under specification limiting impurities to 0.5 per cent, and for the purposes of calculation it may be assumed that the impurities are entirely oxygen—or 1.37 cubic feet. It may further be assumed, for the purpose of calculation, that the transformer is opened up once per year for inspection, and when closed up the gas space is blown out to five-per cent oxygen concentration, which would leave an additional 0.68 cubic foot of oxygen in the gas space annually. The total amount of oxygen in the gas space available for oxidation of the oil would therefore be 2.05 cubic feet per year, which would require 67 years before any sludging would appear—and most important, this would not be dependent upon the temperature of the oil.

If the transformer tank were bolted up tight with no breathing but were opened once a year for inspection and the gas space filled with air upon closure, 50 years would be required for sludge to appear.

In making calculations for various types of breathers, it is necessary to assume some daily load cycle and also to establish an average oil temperature. Seventy-five degrees has been assumed for the oil temperature with a temperature fluctuation of 20 degrees daily. Under this condition the transformer would breathe in approximately 612 cubic feet of air per year containing approximately 123 cubic feet of oxygen.

Let us assume, at this point, that the transformer is equipped with a single breather which takes in air when the transformer cools and breathes it out during the heating-up part of the cycle. Very approximately we can say that the amount of air breathed in equals the amount of air breathed out. This is not quite true due to the fact that there is some gas absorption in the oil. More exactly, we can say that the amount of oxygen breathed in is equal to the amount of oxygen breathed out plus the amount of oxygen absorbed by oxidation in the oil. To fulfill this latter equation, it is necessary to assume an average oxygen concentration and refer to figure 4 of Doctor Hill's paper to see if the amount absorbed plus the amount breathed out will equal that taken in. Assuming approximately $5^1/_2$-per cent average oxygen concentration, 34 cubic feet of oxygen would be discharged in the out breathing. From figure 4 the rate of oxygen absorbed per square foot of oil surface per year would be 0.00062. At this same concentration the amount absorbed in a year would be 0.00062 × 16.4 × 24 × 365 or 89 cubic feet. The conditions of the equation regarding oxygen are fulfilled, since 123 cubic feet breathed in equals 34 cubic feet breathed out plus 89 cubic feet absorbed. Since 136 cubic feet of oxygen are required for the oil to reach 0.5 milligram of KOH acidity, and 89 cubic feet would be absorbed annually under the conditions described, it would only take about a year or a year and a half for the oil in a transformer with a single breather to reach a condition where it would begin to sludge.

Similar methods of calculation may be used for other breather arrangements.

Occasionally double breathers are used which provide a constant circulation of air through the gas space above the transformer. This arrangement is quite advantageous from the point of view of preventing moisture condensation on the cover. However, it does provide a high oxygen concentration above the gas space with consequent short oil life. Assuming the same average oil temperature (75 degrees centigrade) and only 16 per cent oxygen concentration (which seems conservative) the oil in the transformer should require only 0.6 of a year to reach 0.5 milligram of KOH acidity.

The case of the simple expansion tank can be calculated with less assurance from the data presented by Doctor Hill because the oil in the conservator tank might be somewhat more quiescent than in the main tank and therefore less accessible to oxidation. Also the temperature of the conservator tank varies widely depending on the type of connection to the main tank. However, in an actual heat run the temperature of the oil in the conservator was 58 degrees, when the oil in the main transformer was 75 degrees. Assuming the same daily load cycle as previously and applying the calculation to the surface of the oil in the conservator, which was $7^1/_2$ square feet, it was found that of the 123 cubic feet of oxygen breathed in, 85 were breathed out and 38 absorbed, with the result that 3.6 years would be required before sludging would appear. With types of construction which tend to reduce the oil circulation and the temperature of the oil in the conservator, the time might be considerably longer—perhaps even 6 or 7 years.

The merit of nitrogen protection or tightly sealed tanks is apparent from the above calculations from the standpoint of oil deterioration.

In interpreting the insulation deterioration results which Doctor Hill presents, in terms of actual transformer construction, it should be recognized that the insulation in the transformer, which is subjected to appreciable mechanical stress, is actually at the oil temperature rather than at the copper temperature. In fact, it is only the insulation covering the copper conductor itself which reaches the maximum copper temperature, and here a deterioration in tensile strength of 50 to 75 per cent is probably not serious once the insulation is in place in the transformer. Doctor Hill's data on insulation deterioration in nitrogen indicate that the deterioration due to temperature takes place relatively quickly—that is, in 100 to 150 days. After that there appears to be little further deterioration, and the loss in tensile strength even at temperatures as high as 120 degrees would not appear to be a serious matter. This is particularly true since most of the insulation is used in compression rather than in tension, and the decrease in tensile strength is probably not accompanied by a commensurate decrease in compression strength.

It can be concluded from Doctor Hill's data that the maximum safe temperature limits, particularly for short periods of time, can be considerably higher in types of transformers having effective protection of the oil and insulation against oxidation.

The proposed recommendations of the American Standards Association for short-time overload capacity of transformers appear to be extremely conservative when applied to transformers having protection against oxidation. With such transformers, continuous operation at rated load, 55 degrees rise, and at 40 degrees ambient, is possible without measurable deterioration.

Charles F. Hill: Mr. Putman and Mr. Hellmund have called attention to European practice of higher temperatures in distribution transformers than permitted in America, and the fact that my results explain and justify the argument for this European practice. As Mr. Putman points out, it is illogical to use devices to keep out oxygen and then limit the transformer to the same temperature as a free-breathing unit.

Mr. Montsinger points out the apparent flattening off of the deterioration rate of cellulose in my results, after a relatively few months. The flat part of the curve could have been given a slight slope and still fit the data, but the rapid initial effects were rather striking. I suspect some initial oxidation due to residual oxygen in the oil with more frequent opening of the apparatus could have distorted the early results in this respect. The important point is that the mechanical deterioration at the end of two years is not nearly so great as normally expected. The shape of the curves points out the difference in conclusions to be reached, depending upon the length of the life test as emphasized by Smith and Scott in their paper.

As to the rate of deterioration and the "8 degree" rule or "10 degree" rule now under consideration, I believe this is too conservative for this case, but this subject is analyzed in more detail in my discussion of the paper by Smith and Scott (page 444).

My results emphasize again the accepted conclusion that deterioration of the organic materials (solids) is primarily mechanical and not electrical. The choice of tests to measure this deterioration is somewhat a problem, but we have concluded tensile tests are most dependable.

Referring to Mr. Halperin's comment, it is true as he points out that these results as well as previous experiments and experience show a critical temperature for cellulose around 100 degrees centigrade, but to get 100 per cent deterioration, even at higher temperatures, requires a long time. Our results also show, as he mentions, that oxygen has a very marked effect when both high temperature and oxygen are applied to oil-immersed cellulose.

Mr. Halperin has also raised the question of type of test and suggested a tearing test. I believe embrittlement measurements for this case would be more in order. We have had some degree of success with these.

I agree in general with F. M. Clark's statements concerning the relative value of controlled laboratory tests and "commercial" tests. The data on oils in my paper, figures 2 to 4, inclusive, are more nearly laboratory experiments than commercial in that conditions were rather closely controlled.

It is not clear to me just what Mr. Clark's explanation of the apparent lack of electrical deterioration may be. The oil-soaked fullerboard remains good electrically, as also did the oil in the inert gas tests.

Mr. Clark has also discussed the mechanisms by which oil or oxidized oil deteriorates cellulose. It was not the intention to add data to the paper to prove a theory of these mechanisms, but from other results we do believe peroxide stages of oil oxidation are a serious factor. Concerning the ability of oil to carry oxygen to cellulose, we believe that is generally accepted and it is probable that in the presence of an oxygen atmosphere, the nonoxidizing fireproof liquids may show less effect upon cellulose at 80–85 degrees centigrade than does oil. As Mr. Clark states, also, the lighter water-soluble acids formed in oil oxidation are the more active.

Our experiments were so selected as to show the deterioration effects and their degree within the various temperature ranges and so far as I can see from Mr. Clark's discussion, he agrees in a general way with our results. We recognize, of course, that some of the mechanisms of deterioration are not as yet explained.

Mr. Vogel has used the oil data to determine the life of a transformer oil under various conditions of operation, that is, under various amounts of available oxygen for oil deterioration. I believe our results will be of considerable value to the user of transformers in determining what he should demand in the way of protection against oil oxidation.

The data on oil and cellulose deterioration both should be of value to the operating engineer in determining what to do with transformers in service. Mr. Louis points out in his discussion that the operating engineer needs such information. I believe, as Mr. Louis suggests, that the data given will be found of considerable value. No doubt more experiments may be necessary to give a complete and accurate picture and I hope operating engineers will attempt to utilize the data enough to evaluate the results.

So far as temperature limits and standards are concerned, the paper raises some very important questions, but these have been pointed out by Mr. Putman and need no further discussion.

Capacitors and Automatic Boosters for Economical Correction of Voltage on Distribution Circuits

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MEMBER AIEE

Synopsis: Analysis of operating characteristics shows that fixed shunt capacitors may be used to provide voltage correction up to approximately three per cent on a typical four-kv distribution circuit. Installation of capacitors up to this limit, or to unity power factor on the feeder if that occurs with less capacity, generally gives voltage improvement at minimum cost.

Additional correction can be secured with a set of automatic voltage boosters beyond the shunt capacitors, except that single-phase taps from near the feeding point should have the other phases extended first to secure lowest cost. This procedure requires only three different devices, a 30-kva single-phase capacitor, a 90- to 180-kva three-phase capacitor, and a 5-kva automatic booster with two 1.33-per-cent steps, to secure appreciable economies in distribution cost.

RECENT years have seen the introduction and promotion of three additional types of equipment to correct abnormal voltage conditions on distribution circuits, namely: (1) the automatic voltage booster, (2) the shunt capacitor, and (3) the series capacitor. Each one functions in a different way to regulate voltage, and has certain other effects, which make direct comparison of price an unfair test of true economic value. This paper undertakes to compare them as they might be applied to the system of the Duquesne Light Company, subject to the voltage limitations and other conditions of that system, and to ascertain which device or combination of devices should be used to maintain the voltage at consumers' service switches within the prescribed limits. Inasmuch as the Duquesne Light system is generally similar to those operating elsewhere in areas of similar population density, it seems probable that the conclusions will be fairly representative.

The typical circuit is three-phase, four-wire, four-kv, regulated at the substation by single-phase induction regulators. By means of line drop compensators, the regulators are made to correct for voltage drop in the main feeder, and it is assumed that they regulate the primary voltage directly at the first tap or transformer (called the "feeding point"). This feeding-point voltage normally varies between 102.0 per cent at light load and 104.0 per cent at peak load, always with a tolerance of ±1.0 per cent for the margin between "raise" and "lower" in the contact-making voltmeters controlling the regulators. These values have been established to give as good voltage as possible as much of the time as possible to as many consumers as possible without sacrificing the economic advantage of raising the feeding-point voltage close to the 105.0 per cent limit during peaked load. With voltage drops at peak load of up to 3.0 per cent in the primary system, 2.5 per cent in distribution transformers, 2.0 per cent in secondaries, and 0.5 per cent in services, the delivered voltages will range from nearly 104.0 ± 1.0 per cent for a consumer fed by a very lightly loaded transformer at the feeding point down to 96.0 ± 1.0 per cent for a secondary consumer at some distance from a heavily-loaded transformer at the end of the primary. This circuit is discussed more fully in the paper "Automatic Boosters on Distribution Circuits"[3] from which figure 1 is taken.

Attempts to benefit by the assumption that consumers near the feeding point always have at least the voltage drop of a 50-per-cent-loaded transformer below the primary voltage at the feeding point during the circuit heavy-load periods, accordingly raising the feeding-point voltage an extra 1.0 per cent to 105.0 ± 1.0 per cent, have not been successful on the Duquesne Light system and have been abandoned because of complaints. The conditions shown in figure 1 represent the maximum voltage drops with which service voltage can be maintained within the established limits, and are typical of a 1,500-kva four-kv open-wire circuit feeding

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Paper number 39-63, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 2, 1938; made available for preprinting January 4, 1939.

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[3] For all numbered references, see list at end of paper.

three or four square miles of fairly uniform suburban load with an extreme length from the feeding point to the end of the longest primary branch of around two miles.

But load increases both in density and in area, and the tendency is to extend the long branches and to carry more and more load on the circuit. Immediately the voltage drop increases, and before long it becomes necessary to provide some means to bring the voltage back within the established limits. When the circuit has ample current-carrying capacity, an advanced feeding point is established nearer the center of load and a back feed is built to serve load along the feeder. Sometimes that does not suffice, and a new feeder or substation may be built to share the load. Both methods ordinarily are expensive, and frequently can be delayed for a number of years by the use of voltage corrective devices,[2] sometimes with considerable savings in annual cost.

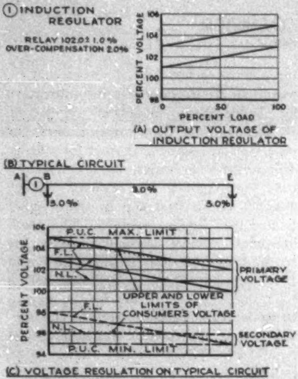

Figure 1. Voltage regulation of typical distribution feeder by induction regulators at substation

Automatic Voltage Boosters

About six years ago leading power companies started to use automatic voltage boosters to correct low voltage on long circuits, particularly for rural districts where rather coarse voltage steps were adequate. The size of step is directly reflected in the output voltage, however, and the earlier paper[3] indicated a two-step booster having 2.66 per cent total range as the most economical design for operation within a total primary voltage variation of 5 per cent. The effect of such a booster is shown in figure 2; with a primary voltage drop 89 per cent greater than could be permitted in the original circuit, figure 1*B*, the voltage delivered to consumers is held within the same limits by means of the automatic boosters. Analysis showed that automatic boosters were economically applicable where satisfactory regulation could be secured without boosters only by extensive installation of 1/0 or 4/0 conductors on the three-phase portion of the circuit.

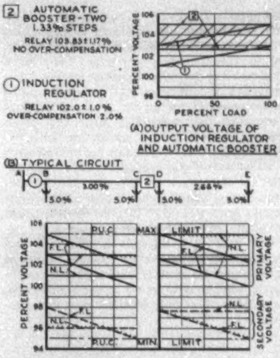

Figure 2. Voltage regulation of typical distribution feeder by induction regulators at substation and automatic boosters on poles

Shunt Capacitors

In 1930 a number of four-kv shunt capacitors were installed by the Duquesne Light Company on circuits in industrial areas to correct for the low power factor then existing. The depression which came even before these installations were complete had the triple effect of reducing the industrial load, making industrial consumers more anxious to benefit by allowances for good power factor, and retarding the expenditures required to serve adequately the continually growing domestic load particularly in areas only recently developed. As early as 1932, capacitors were being moved from industrial circuits to suburban areas to reduce the voltage drop in overextended circuits.

The voltage corrective effect of the shunt capacitor is due to the leading reactive current which, flowing through the circuit from the regulated source at the feeding point to the capacitors out in the load area, produces a rise in voltage equal to the product of the capacitative current and the line reactance. This voltage correction varies with the distance, from zero at the feeding point to full value at the capacitor. Ordinarily the capacitor is permanently in service, and so raises the voltage by the same amount at light load on the circuit as at full load.[4]

The maximum correction which can be secured by use of fixed shunt capacitors can be determined very readily from charts of voltage at the proposed location, the limit being that capacity which raises the voltage during light-load periods to the upper limit of 105.0 per cent. Usually the light-load voltage along a circuit is practically constant along the entire length, substantially equal to the contact-making voltmeter setting of 102.0 ± 1.0 per cent. With these voltage conditions only 2.0 per cent voltage correction can be secured by means of shunt capacitors. More correction can be used, however, by balancing the voltmeter at 101.0 per cent and increasing the line drop compensation, which would permit as much as 3.0 per cent correction by means of shunt capacitors.[*]

It is seldom possible or desirable to locate the capacitors directly at the end of the circuit, as this would give the maximum voltage correction at one point only and less correction at the ends of other branches of the circuit where full correction is as much needed. Generally they are installed at some such point as the end of the three-phase circuit or a branch point of the three-phase circuit, in either case leaving some drop in the primary laterals beyond the capacitor to the last transformers. A circuit embodying these characteristics is shown as figure 3, which indicates that full advantage has been taken of the permissible variation in voltage and that it is not feasible to correct the primary voltage more than 3.0 per cent with fixed shunt capacitors.

If more than 3.0 per cent correction is desired, then additional capacitors can be installed with a switch controlled by a contact-making voltmeter to cut them into service when the voltage drops below the desired level. With voltage limits at the point of installation of 101.0 per cent to 105.0 per cent, automatic voltage-controlled shunt capacitors could be applied to give up to 3.0 per cent additional voltage correction. A circuit with both fixed and automatic capacitors could have a total of 9.0 per cent primary drop, of which 6.0 per cent would be corrected by the capacitors. The automatic capacitors would require individual phase control to compensate for neutral shift, and might as well be single-phase installations. Voltage regulation on a typical circuit with both fixed and automatic shunt capacitors is shown in figure 4.

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[*] Usually the light-load voltage can be lowered somewhat more, which would permit some additional voltage correction by shunt capacitors. It must be borne in mind, however, that a circuit having good phase balance at peak load may have appreciable unbalance at light load, and that this unbalance added to the voltage rise caused by fixed shunt capacitors might cause excessively high voltage on the lightest loaded phase during light load. Accordingly, the limit to voltage correction by fixed capacitors has been set at three per cent.

The shunt capacitor raises voltage by drawing capacitative current. Inasmuch as the typical distribution feeder has a peak-load power factor approximately 90 per cent lagging, the shunt capacitor tends not only to improve voltage but also to improve the circuit power factor. This results in lower kilovolt-ampere demand for the same peak load, and releases carrying capacity in the circuit and in all of the supply system. On systems having low power factor this improvement in power factor is most desirable; the system capacity released is available to carry increased load and has the same value per kilovolt-ampere as an addition to the system.[5] When the system power factor already runs higher than the rating of the generators, then the only value creditable to improved power factor is the capacity released in main feeder, substation, and transmission system. The latter is true of the present analysis, and a value of $50.00 per kilovolt-ampere has been assumed for the reduction in demand. With the present price of 2,400-volt capacitors running under $8.00 per capacitative kilovolt-ampere, it is evident that credit for reduction in peak demand might pay a large portion of the cost of capacitors, leaving a rather small charge for the voltage improvement for which they are installed.

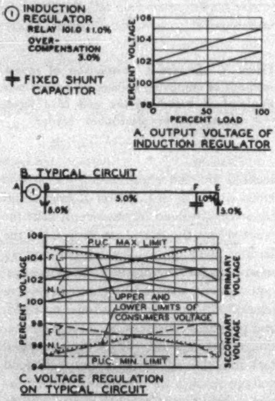

Figure 3. Voltage regulation of typical distribution feeder by induction regulators at substation and fixed shunt capacitors on poles

For example, a circuit carrying 1,500 kva at 90 per cent power factor might require the installation of 360 kva of shunt capacitors for voltage correction. The capacitors would raise the circuit power factor to 98 per cent and reduce the circuit demand measured at the substation from 1,500 kva to 1,385 kva. This reduction in demand of 115 kva, evaluated at $5,750, is secured from shunt capacitors having a purchase price of approximately $2,700. With the cost of the capacitors more than covered by credit for reduction in demand, the voltage correction is secured free of charge.

Series Capacitors

Capacitors of proper rating installed in series in a circuit also tend to reduce the voltage drop.[1] The effect is computed readily as the product of the capacitative reactance and the reactive component of the current flowing through the capacitor. Installed at the point where the primary voltage reaches the lower limit, 101.0 ± 1.0 per cent, it should have such an impedance as would, with the maximum load current flowing through it, raise the voltage 3.0 per cent, or to 104.0 ± 1.0 per cent. As the load decreases, the power factor drops, and the out-of-phase component tends to decrease but little. The voltage boost of the series capacitor, therefore, is nearly as great during light load as during heavy load, and it is necessary to check several different load conditions to avoid excessively high voltages on the output side. For present purposes, however, it is assumed that the capacitor selected to give 3.0 per cent rise at peak load is satisfactory. The voltage regulation of the typical circuit with this series capacitor is shown in figure 5.

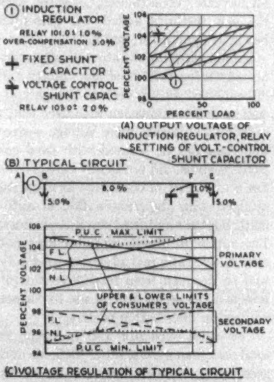

Figure 4. Voltage regulation of typical distribution feeder by induction regulators at substation and shunt capacitors, partially controlled by voltage, on poles

Economic Comparison

There are now five different methods for correcting the voltage regulation of a circuit, namely:

1. Rebuild for greater current-carrying capacity, by changing single-phase to three-phase, replacing present conductors with larger conductors, or building a new circuit to divide the load.
2. Extend the feeding point farther into the load area, back feeding the load along the main feeder and regulating voltage at the new feeding point by increased use of the feeder regulators at the substation.
3. Correct for excessive voltage drop in the existing circuit by installing automatic voltage boosters at the proper points.
4. Correct for excessive voltage drop in the existing circuit by installing shunt capacitors near the ends of the branches. Where fixed capacitors alone do not suffice, additional correction can be secured by means of voltage-controlled automatic capacitors.

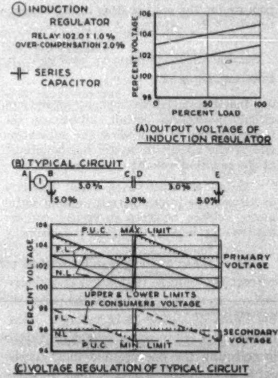

Figure 5. Voltage regulation of typical distribution feeder by induction regulators at substation and series capacitors on poles

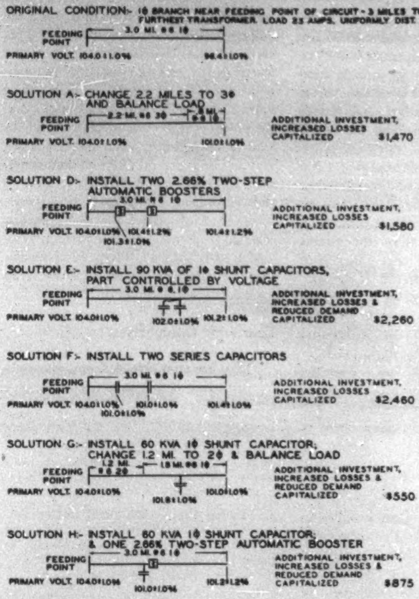
ORIGINAL CONDITION:- 1ϕ BRANCH NEAR FEEDING POINT OF CIRCUIT - 3 MILES TO FURTHEST TRANSFORMER. LOAD 25 AMPS. UNIFORMLY DIST.
FEEDING POINT
3.0 MI. #6 1ϕ
PRIMARY VOLT. 104.0±1.0%
98.4±1.0%
SOLUTION A:- CHANGE 2.2 MILES TO 3ϕ AND BALANCE LOAD
2.2 MI. #6 3ϕ
.8 MI. #6 1ϕ
101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $1,470
SOLUTION D:- INSTALL TWO 2.66% TWO-STEP AUTOMATIC BOOSTERS
3.0 MI. #6 1ϕ
101.4±1.2%
101.3±1.0%
101.4±1.2%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $1,580
SOLUTION E:- INSTALL 90 KVA OF 1ϕ SHUNT CAPACITORS, PART CONTROLLED BY VOLTAGE
3.0 MI. # 6. 1ϕ
102.0±1.0%
101.2±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES & REDUCED DEMAND CAPITALIZED $2,260
SOLUTION F:- INSTALL TWO SERIES CAPACITORS
3.0 MI. #6 1ϕ
101.0±1.0%
101.0±1.0%
101.4±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $2,460
SOLUTION G:- INSTALL 60 KVA 1ϕ SHUNT CAPACITOR; CHANGE 1.2 MI. TO 2ϕ & BALANCE LOAD
1.2 MI. #6 2ϕ
1.8 MI. #6 1ϕ
101.8±1.0%
101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES & REDUCED DEMAND CAPITALIZED $550
SOLUTION H:- INSTALL 60 KVA 1ϕ SHUNT CAPACITOR; & ONE 2.66% TWO-STEP AUTOMATIC BOOSTER
3.0 MI. #6 1ϕ
101.0±1.0%
101.2±1.2%
ADDITIONAL INVESTMENT, INCREASED LOSSES & REDUCED DEMAND CAPITALIZED $875

Figure 6. Economic comparison of several methods of improving voltage on a three-mile single-phase branch at the feeding point of a three-phase four-kv distribution feeder

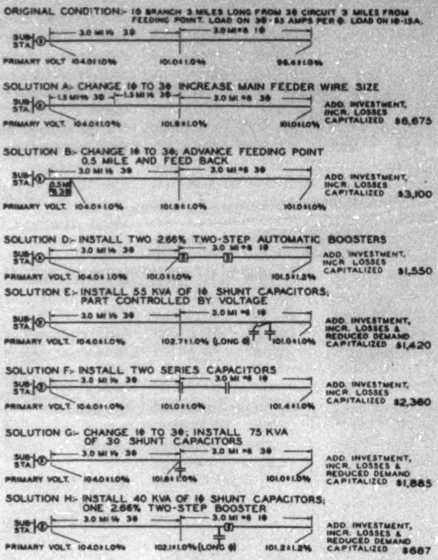
ORIGINAL CONDITION:- 1ϕ BRANCH 3 MILES LONG FROM 3ϕ CIRCUIT 3 MILES FROM FEEDING POINT. LOAD ON 3ϕ - 85 AMPS PER ϕ. LOAD ON 1ϕ-13A.
SUB STA.
3.0 MI ¼ 3ϕ
3.0 MI #6 1ϕ
PRIMARY VOLT 104.0±1.0%
101.0±1.0%
98.4±1.0%
SOLUTION A:- CHANGE 1ϕ TO 3ϕ INCREASE MAIN FEEDER WIRE SIZE
1.5 MI ⅞ 3ϕ
1.5 MI ¼ 3ϕ
3.0 MI #6 3ϕ
101.8±1.0%
101.0±1.0%
ADD. INVESTMENT, INCR. LOSSES CAPITALIZED $6,675
SOLUTION B:- CHANGE 1ϕ TO 3ϕ; ADVANCE FEEDING POINT 0.5 MILE AND FEED BACK
3.0 MI ¼ 3ϕ
3.0 MI #6 3ϕ
0.5 MI ¼ 3ϕ
101.8±1.0%
101.0±1.0%
ADD. INVESTMENT, INCR. LOSSES CAPITALIZED $3,100
SOLUTION D:- INSTALL TWO 2.66% TWO-STEP AUTOMATIC BOOSTERS
3.0 MI ¼ 3ϕ
3.0 MI #6 1ϕ
101.0±1.0%
101.5±1.2%
ADD. INVESTMENT, INCR. LOSSES CAPITALIZED $1,550
SOLUTION E:- INSTALL 55 KVA OF 1ϕ SHUNT CAPACITORS; PART CONTROLLED BY VOLTAGE
3.0 MI ¼ 3ϕ
3.0 MI #6 1ϕ
102.7±1.0% (LONG ϕ)
101.0±1.0%
ADD. INVESTMENT, INCR. LOSSES & REDUCED DEMAND CAPITALIZED $1,420
SOLUTION F:- INSTALL TWO SERIES CAPACITORS
3.0 MI ¼ 3ϕ
3.0 MI #6 1ϕ
101.0±1.0%
101.4±1.0%
ADD. INVESTMENT, INCR. LOSSES CAPITALIZED $2,360
SOLUTION G:- CHANGE 1ϕ TO 3ϕ; INSTALL 75 KVA OF 30 SHUNT CAPACITORS
3.0 MI ¼ 3ϕ
3.0 MI #6 3ϕ
101.8±1.0%
101.0±1.0%
ADD. INVESTMENT, INCR. LOSSES & REDUCED DEMAND CAPITALIZED $1,885
SOLUTION H:- INSTALL 40 KVA OF 1ϕ SHUNT CAPACITORS; ONE 2.66% TWO-STEP BOOSTER
3.0 MI ¼ 3ϕ
3.0 MI #6 1ϕ
102.1±1.0% (LONG ϕ)
101.2±1.2%
ADD. INVESTMENT, INCR. LOSSES & REDUCED DEMAND CAPITALIZED $687

Figure 7. Economic comparison of several methods of improving voltage on a three-mile single-phase branch at the end of a three-phase four-kv distribution feeder

5. Reduce voltage drop in the existing circuit by means of series capacitors, which can be selected to give a voltage rise to compensate for the voltage drop in the conductors.

Several methods may be used in combination, such as

6. Install as much fixed shunt capacity as circuit conditions permit, advancing the feeding point or rebuilding the substation end of the circuit to secure the remainder of the required voltage correction.
7. Install as much fixed shunt capacity as circuit conditions permit, adding automatic voltage boosters to correct for excessive drop beyond the shunt capacitors.

Any of these methods can be applied to correct the voltage regulation of a distribution circuit which exceeds the permissible limits of steady-state voltage. Economics now enters as an important factor in selecting the one method most applicable to the case at hand. The previous paper presented a number of comparisons of the first three methods applied to typical circuits. These examples, corrected for increased construction costs and with the new methods added, are presented herein as figures 6, 7, and 8.

Shunt capacitors automatically connected during periods of low voltage and disconnected during periods of high voltage, used in combination with fixed shunt capacitors to secure more voltage correction than can be secured with fixed capacitors alone, did not in any case give as low a total cost of correction as could be attained with the combination of automatic boosters and fixed shunt capacitors. The automatic shunt capacitor does not seem to offer any real advantage and may well be dropped from further consideration as a device for general use to correct voltage on distribution feeders.

The series capacitor also is higher in total cost than several other methods and need not be considered further as a means for the correction of voltage drop in distribution feeders. An exception must be made, however, for fluctuating loads which require the reduction of circuit impedance either by increasing copper size or by the installation of series capacitors or proper current rating and capacitance, but this is a problem in itself, entirely separate from the scope of the present paper.

The lowest total cost in every case is secured by the combination of several methods. The installation of fixed shunt capacitors well out on the circuit near the point where the peak-load drop from the feeding point approaches 5.0 per cent almost pays for itself in the value of the system capacity released by the reduction in amount of reactive current to be supplied, and provides considerable voltage correction at low net cost. Fixed shunt capacitors may be added until the light-load voltage reaches the maximum or the circuit power factor reaches unity, whichever comes first. Any additional voltage correction required can be secured by means of automatic voltage boosters beyond the shunt capacitors.* The installation of shunt capacitors and one set of automatic boosters compensates for 5.66 per cent voltage drop and permits loading the circuit to 288 per cent of the limit established by voltage drop without any of these supplementary devices. Additional automatic boosters might be installed, but sooner or later the point is reached where it is more economical to rebuild the circuit for greater capacity and better regulation. This point depends largely upon the length of circuit and the conductor sizes, but it is doubtful whether two sets of boosters in series beyond a maximum installation of fixed shunt capacitors can be justified on four-kv circuits up to five or six miles long.

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* Fixed shunt capacitors should not be installed beyond voltage boosters as there are no compensators to overcome undesirably high voltage during light-load periods.

Long single-phase branches connected near the feeding point of a three-phase circuit also should have fixed shunt capacitors. Any additional voltage correction required should be secured by extending the second and third phases; considerable voltage improvement is accomplished by a comparatively short extension of the additional phases and the cost generally is less than that of automatic voltage boosters on the single-phase circuit.

Adaptability to Growing Loads

The final test of any proposal affording comparable service is whether its use results in lowered total costs over a period of years. The earlier paper showed the annual cost through a 30-year period of growth of two typical circuits using automatic voltage boosters for voltage correction as compared with the conventional methods of rebuilding. The same two circuits have been analyzed using fixed shunt capacitors to the limits of unity power factor and 3.0 per cent voltage rise, followed by other methods of correction.

In the first case, the circuit originally consisted of two number 3 copper wires single-phase extending three miles from a feeding point having peak load voltage of 104.0 ± 1.0 per cent. The load originally was 20 kva distributed uniformly along this three-mile feeder and the voltage drop was entirely satisfactory, but load growth at the rate of ten per cent per year soon increased the drop to the limit. At this point a shunt capacitor is installed two miles from the feeding point to correct the voltage. Continued growth exhausts this means of correction and the circuit is then gradually converted to three phase. The steps are shown in figure 9 along with correction by boosters and by circuit rebuilding alone. The relative costs are plotted in figure 10. It is obvious that the conversion from single-phase to three-phase is delayed, with a savings of four per cent in accumulated annual cost.

In the second case the circuit originally consisted of four number 3 copper wires three-phase extending three miles from a feeding point having three-phase voltage of 104.0 ± 1.0 per cent. The load originally was 100.5 kva uniformly distributed along the feeder and well balanced, and the voltage drop was satisfactory, but load growth at the rate of ten per cent per year soon increased the drop to the limit. At this point fixed shunt capacitors are installed two miles from the feeding point to correct the voltage. Continued growth necessitates further correction and a set of automatic voltage boosters is added, followed by circuit rebuilding and additional capacitors. The steps are shown in figure 11, along with correction by boosters and by circuit rebuilding alone, and the relative costs are plotted in figure 12. Again it is clear that the capacitors and boosters together have delayed the more expensive rebuilding and have accomplished a substantial savings in annual costs.

Conclusions

These growing-load comparisons confirm the conclusion indicated by the sev-

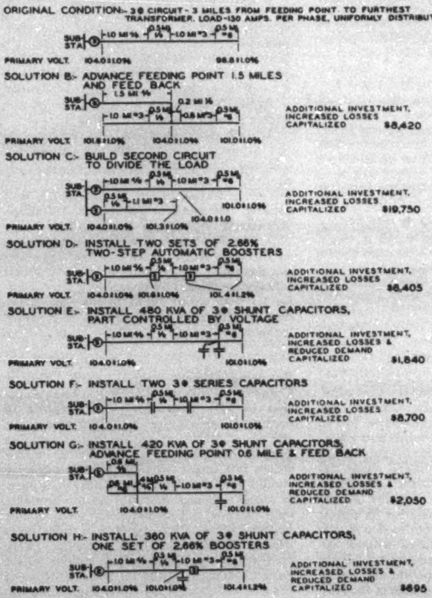
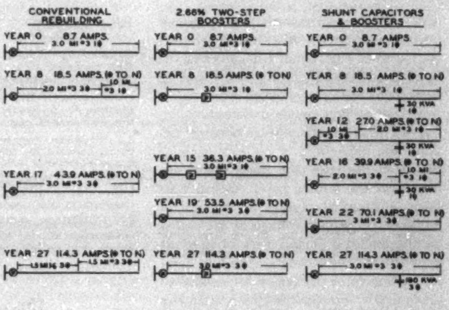

ORIGINAL CONDITION:- 3φ CIRCUIT - 3 MILES FROM FEEDING POINT TO FURTHEST TRANSFORMER. LOAD-130 AMPS. PER PHASE, UNIFORMLY DISTRIBUTED
SUB-STA.
1.0 MI ⅟₀
0.5 MI ⅟₀
1.0 MI #3
0.5 MI #6
PRIMARY VOLT. 104.0±1.0% 98.8±1.0%
SOLUTION B:- ADVANCE FEEDING POINT 1.5 MILES AND FEED BACK
1.5 MI ⅟₀
0.2 MI ⅟₀
0.8 MI #3
PRIMARY VOLT. 101.8±1.0% 104.0±1.0% 101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $8,420
SOLUTION C:- BUILD SECOND CIRCUIT TO DIVIDE THE LOAD
1.1 MI #3
PRIMARY VOLT. 104.0±1.0% 101.3±1.0% 104.0±1.0 101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $19,750
SOLUTION D:- INSTALL TWO SETS OF 2.66% TWO-STEP AUTOMATIC BOOSTERS
PRIMARY VOLT. 104.0±1.0% 101.8±1.0% 101.4±1.2%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $8,405
SOLUTION E:- INSTALL 480 KVA OF 3φ SHUNT CAPACITORS, PART CONTROLLED BY VOLTAGE
PRIMARY VOLT. 104.0±1.0% 101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES & REDUCED DEMAND CAPITALIZED $1,840
SOLUTION F:- INSTALL TWO 3φ SERIES CAPACITORS
PRIMARY VOLT. 104.0±1.0% 101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES CAPITALIZED $8,700
SOLUTION G:- INSTALL 420 KVA OF 3φ SHUNT CAPACITORS; ADVANCE FEEDING POINT 0.6 MILE & FEED BACK
0.6 MI ⅟₀
PRIMARY VOLT. 104.0±1.0% 101.0±1.0%
ADDITIONAL INVESTMENT, INCREASED LOSSES & REDUCED DEMAND CAPITALIZED $2,050
SOLUTION H:- INSTALL 360 KVA OF 3φ SHUNT CAPACITORS; ONE SET OF 2.66% BOOSTERS
PRIMARY VOLT. 104.0±1.0% 101.0±1.0% 101.4±1.2%
ADDITIONAL INVESTMENT, INCREASED LOSSES & REDUCED DEMAND CAPITALIZED $695

Figure 8. Economic comparison of several methods of improving voltage on a three-mile three-phase four-kv distribution feeder

Figure 9. Periodic alterations on three-mile 2,300-volt single-phase branch at the feeding point of a three-phase four-kv distribution feeder to maintain primary voltage within limits of 105 and 100 per cent for load growth of ten per cent per year


CONVENTIONAL REBUILDING
2.66% TWO-STEP BOOSTERS
SHUNT CAPACITORS & BOOSTERS
YEAR 0 8.7 AMPS.
3.0 MI #3 1φ
YEAR 8 18.5 AMPS. (φ TO N)
2.0 MI #3 3φ
1.0 MI #3 1φ
30 KVA 1φ
YEAR 12 27.0 AMPS. (φ TO N)
YEAR 15 36.3 AMPS. (φ TO N)
YEAR 16 39.9 AMPS. (φ TO N)
YEAR 17 43.9 AMPS. (φ TO N)
3.0 MI #3 3φ
YEAR 19 53.5 AMPS. (φ TO N)
YEAR 22 70.1 AMPS. (φ TO N)
3 MI #3 3φ
YEAR 27 114.3 AMPS. (φ TO N)
1.5 MI ⅟₆ 3φ
1.5 MI #3 3φ
180 KVA 3φ

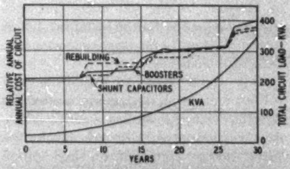


Figure 10. Relative total annual cost of serving a load growing ten per cent per year on a three-mile 2,300-volt feeder initially single-phase with number 3 conductors

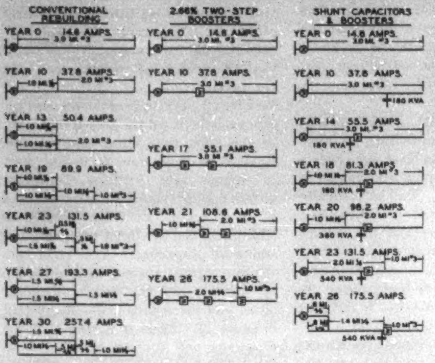


Figure 11. Periodic alterations on three-mile four-kv three-phase circuit to maintain primary voltage within limits of 105 and 100 per cent for load growth of ten per cent per year

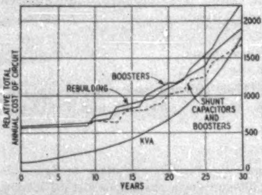


Figure 12. Relative total annual cost of serving a load growing ten per cent per year on a three-mile feeder initially four-kv three-phase with four number 3 conductors

eral static comparisons that correction of voltage on four-kv distribution circuits by means of fixed shunt capacitors to the limit established by power factor and by light-load voltage, followed, when additional correction becomes necessary, by one set of automatic voltage boosters beyond the capacitors or by extension of phases in the case of a single-phase tap from near the feeding point, makes possible substantial savings in the cost of maintaining proper voltage at all points on the circuit. It was very obvious at many points in the comparisons that careful adherence to this policy is essential to maximum economies and that it is unwise to attempt to use too many auxiliary regulating devices before installing 1/0 or 4/0 conductors in the heavier-loaded portions of the circuit.

Only three types of supplementary voltage-regulating devices are needed for suburban four-kv feeders. Single-phase shunt capacitors of 30 kva are used for the initial voltage correction on single-phase extensions, followed by single-phase five-kva automatic voltage boosters with two 1.33-per-cent steps except on a single-phase tap from near the feeding point which should have additional phases extended. Low voltage on the three-phase portions of circuits is corrected by means of three-phase four-wire shunt capacitors of 90 to 180 kva, followed by sets of three of the same five-kva automatic boosters. The storeroom setup is thereby simplified, and there is better opportunity for prompt reinstallation of the supplementary regulating devices after they are removed from their initial locations. This system provides worthwhile economies in the cost of distribution, and delays major changes until more load has developed to give a better indication of future needs.

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Discussion

P. E. Benner (General Electric Company, Schenectady, N. Y.): Mr. Olmsted has certainly done a very commendable job in presenting this straightforward and clear-cut method of analyzing the operation and evaluating the benefits of using shunt capacitors in connection with automatic boosters. He has shown that by proper application of capacitors installed for power factor correction it is possible to double the primary drop in the load area without increasing the limits of consumer voltage variation. This makes possible the use of smaller conductors or an increase of the load area served by any particular feeder

The author arrives at a value of three per cent as the maximum permissible correction by means of shunt capacitors. In this connection it is important to note that this three per cent correction is between the first transformer and the capacitor installation, and does not refer to the voltage rise from the capacitor clear back to the substation. It should be further noted that this three per cent correction or voltage rise does not take into account the possibility of using a branch feeder regulator with a buck as well as a boost range installed between the capacitor installation and the first transformer. The installation of such a branch feeder regulator with line-drop compensator would, of course, double the permissible voltage rise due to the capacitor, thereby raising the limit of three per cent to six per cent.

By limiting himself to the use of a two-step booster the author cannot take full advantage of shunt capacitor applications in that in many cases it would not be possible to locate the capacitor as far out on the circuit as might otherwise be desirable. It is believed that the small cost differential between the two-step booster and the four-step regulator with line-drop compensation would easily be offset by the postponement of the time of changing a circuit to three-phase or installing larger copper. The four-step regulator of conventional design can be connected for two steps raise and two steps lower, and therefore would permit the use of a shunt capacitor located further out on the line. Such an installation would make it possible to double the allowable primary drop as shown by the author in figure 3.

In addition to the author's five methods for correcting the voltage regulation of a circuit, it would seem desirable in a comprehensive analysis to consider the use of a branch feeder regulator of either the four-step or the induction type which have a buck as well as a boost range and are therefore particularly adaptable for use in conjunction with a shunt capacitor.

Also, in cases where voltage regulation is the primary consideration one should not overlook the possibility of obtaining the desired voltage rise by means of a fixed booster installed in the circuit in the load area as discussed in the paper, "Voltage-Regulating-Equipment Characteristics as a Guide to Application" which was published in the 1938 AIEE Transactions (September section). It is usually possible to obtain voltage correction by means of a fixed booster for only about 10 to 15 per cent of the cost of obtaining it by means of a shunt capacitor.

Raymond Bailey (Philadelphia Electric Company, Philadelphia, Pa.): The discussion in Mr. Olmsted's paper relating to credit for capacity released in a system by the use of static capacitors leads to a more general question as to conditions under which full credit for such system capacity should be allowed. From the point of view of sound business, it would seem reasonable only to allow full credit on the basis of cost per unit of capacity installed in those cases where this released capacity permits taking on load where otherwise investment would have to be made in the immediate future to provide the capacity for this load. For example, it is questionable how much credit should be allowed in a case where the use of static capacitors on distribution circuits releases capacity in the step-down transformers in a substation which are at present rather lightly loaded, as it may be an appreciable number of years until this released capacity is actually needed to carry load. A more or less parallel case exists in determining credit for released generating capacity and here one must consider what actual effect this released capacity would have on the future investment to provide generating capacity.

It will be found in many cases that it is sound business to allow as a credit only a certain proportion of the cost per unit of capacity in place, depending upon the time that will elapse before this capacity is actually needed.

This matter of value of system capacity is one that frequently arises in economic problems that have to do with public utility systems and it is believed that it deserves a very searching review by public utility engineers in order to avoid balancing on one hand an actual expenditure of money against calculated credit which may not represent a real saving.

C. E. Arvidson (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Olmsted's paper shows economic comparisons of various methods of correcting excessive voltage drops in distribution circuits and justifies the use of capacitors largely by capitalizing the released system capacity resulting from the capacitor installations. Does this released system capacity have any value during an interim when the transmission lines and substations involved are not loaded to capacity? In cases where the transmission lines or substations are loaded to capacity, does not the installation of capacitors serve to postpone the installation of additional system capacity rather than replace such additions? If capacitors can be considered as a means of permanently releasing system capacity, should not investigations be made to justify their installation on all distribution circuits regardless of existing voltage conditions?

It is the writer's opinion that the economic value of capacitors as a means of voltage correction depends upon existing conditions and that other methods of voltage correction may often be more economical than capacitors.

L. H. Hill (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): Mr. Olmsted is to be congratulated on his very thorough discussion of various means of regulating distribution feeders. It is evident from the paper that the elimination of wattless current on distribution feeders is highly desirable. In this regard the author apparently has overlooked the great saving in exciting kilovolt-amperes obtained by the use of step-type feeder voltage regulators instead of the induction type as referred to in the paper.

For example, induction-type regulators used to regulate the 1,500-kva feeder referred to in the paper require approximately 37.5 kva for excitation. On the other hand, step-type regulators for this same service require only about 7.5 kva for excitation—a net saving of 30 kva. This means that 30 kva, or almost ten per cent of the capacitors, could be eliminated by the substitution of step-type regulators for the induction type now used. Since capacitors are priced at about $8.00 per kilovolt-ampere, this would result in a direct saving of $240.00.

If capacitors were not used, an even greater advantage can be shown for the step-type regulator. The reduction of 30 kva in wattless current on this particular feeder would result in a decrease in demand of 12.4 kva. When capitalized at $50.00 per kilovolt-ampere according to the paper, this reduction in demand would amount to a saving of $620.00.

From these two illustrations it is evident that much can be done in the way of selecting equipment to release system capacity without resorting to capacitors. Even if capacitors are found desirable, however, the step-type regulator greatly reduces the number required.

J. W. Butler (General Electric Company, Schenectady, N. Y.): In view of my interest in the application of series capacitors I would like to comment briefly on their use in the circuit regulation picture, in addition to that considered by Mr. Olmsted.

Admittedly this paper and its presentation was not required to dwell on this point since the objective was to determine the most economical way to take care of the type of regulation that could be handled in several different manners as listed in figure 8. Series capacitors, however, are able to furnish a type of regulation in addition to that type dealt with in the paper that can be furnished by no other piece of regulating equipment. This discussion is given for the benefit of those individuals not intimately acquainted with the subject in case their opinion of the series capacitor was formed in substance by the account given by Mr. Olmsted.

A series capacitor gives a boost in the line voltage at the point of application as determined by its ohmic value and the power factor of the current. This boost takes place at the capacitor—hence the circuit on the power-source side has the same regulation as it had before installation. The voltage on the load side, however, is changed and for any given power factor and circuit, the voltage at a preferred location can be made to have practically zero regulation. The rest of the circuit, however, will have regulation, either plus or minus, depending upon its location with respect to the capacitors. Obviously then for a circuit having a distributed load, two or more series capacitors probably would have to be used to give satisfactory regulation to the complete circuit. It is this point that makes the series capacitor appear in an unfavorable light when considering Mr. Olmsted's problem.

Consider however a feeder having substantially no load taken from it until it gets to a bus, where there might be a saw mill, an electric dredge, a large motor that is intermittently started, or some other type of pulsating load that causes severe voltage fluctuations at the bus affecting all other connected loads, such as city lighting. Since the series capacitor produces its compensation *automatically* and *instantaneously*, it enjoys a unique position in the regulating field in being able to cure this type of voltage fluctuation.

Paul H. Jeynes (Public Service Electric and Gas Company, Newark, N. J.): This paper deals with a subject that has been carefully investigated by Public Service engineers, and it can be said that in general our experience and conclusions parallel closely those reported by Mr. Olmsted. Differences in detail may perhaps be accounted for by peculiarities of load conditions on the two systems.

For example, the statutory voltage tolerance in New Jersey is most rigorous, being plus or minus three per cent—not five per cent. In addition, we are acutely conscious of the error in supplying unnecessarily high voltage to the customer, both on ethical grounds and for the sake of good public relations. Although studies have demonstrated that the effect of increased voltage (within this three per cent) on customers' bills is practically negligible, this factor is vastly overrated in the minds of many—engineers as well as laymen. The use of the shorter-lived Mazda lamps is liable to create a suspicion that unduly high voltage may be responsible for the increased lamp renewals. In either event, it is important to be able to demonstrate that infinite pains are taken to avoid the condition.

Other characteristics of the Public Service system are the high power factors ordinarily encountered at time of peak loads, and the moderately high load density in the territory served. While we do have areas that we consider to be "rural," by some standards even these would be classified as zones of fairly good density.

One apparent difference which results from the above considerations is the matter of compensating primary circuits. Maximum permissible voltage is not maintained at the point of feed—that is, the first transformer on the circuit—unless that is necessary in order to keep the last customer from dropping below the permissible minimum. In other words, circuits are compensated to maintain nominal voltage at some point well out on the primary, which tends to reduce the voltage improvement to be expected from shunt capacitors attached at the end of the circuit.

Another effect of the small voltage tolerance is that the range of single-step boosters must be limited to five per cent, as a rule, while no applications for multistep boosters have yet been found.

Automatic Boosters

A number of automatic-booster installations are giving satisfactory service and have proved to be cheaper than any alternative. On a few occasions boosters were installed only to have it appear that this was the wrong answer. The difficulty lay in the type of load, which resulted in an almost unpredictable daily voltage cycle, and too frequent operation of the controls. In one case we were able to avoid trouble by using a time switch to cut out the automatic operation during daylight hours when the circuit supplied only power load, which caused the difficulty.

Like Mr. Olmsted we had discovered that the combination of booster and shunt capacitor is ideal for certain situations, but refrained from reporting on the subject only because we have made no actual installations. Our nearest approach is a recently completed combination of pole-type induction regulator with capacitors at the end of single-phase branches beyond the regulator. This has solved a difficult problem and is giving almost ideal service.

Shunt Capacitors

As Mr. Olmsted points out, light-load conditions usually limit the size of capacitor installations for voltage improvement. About 1,000 kva of 15-kva units are installed on Public Service lines, and we are enthusiastic about them. They give maximum relief at the point of attachment, where it is most needed, and the effect tapers off back to the center of regulation; as a result, they minimize the undesirable overvoltage supplied to customers nearer the substation, which is just the reverse of a boost applied part way out on the circuit.

We have not as yet made any installations switch-controlled by the voltage, though we have numerous units on pole-type constant-current transformers that cut in with the transformer when the street lights are turned on. The proposal for voltage control of capacitors connected does have appeal, particularly because the voltmeter would be located at the point of maximum voltage variation. This of course is not the case for boosters; the resultant debate over voltage versus current control of boosters is familiar to all. In any event, it appears to be purely a matter of relative costs; we are not yet ready to rule out automatically controlled capacitors as a future possibility.

"Released Capacity" Savings

On this subject of savings from "released capacity" in the supply system we are forced to differ with Mr. Olmsted—not in criticism of his figures, but of his general approach to the problem of evaluating savings. An important addition to his bibliography is proposed, being a brief comment by Alex Dow which appeared in the *Electrical World* for January 10, 1925.

Sometimes fantastic savings result from power-factor improvement. But unless *without* capacitors we would have to spend money that would not be spent *with* capacitors, these "released capacity" savings are worth zero dollars per kilovolt-ampere. Installations of the small amounts of leading reactive capacity required for voltage improvement very rarely affect the program for feeder, substation, or transmission capacity installations, in our experience. Even when capacitors can be justified by resultant postponement of capacity installations, the postponement is only temporary; a point is reached where the power factor approaches unity, the capacity must be installed in any event, and the capacitors are for a time of no further use.

The most fruitful source of savings in our experience lies in postponement of incremental substation transformers, and this possibility of savings is investigated whenever growing loads indicate that additions are imminent. Frequently postponement for a short period is found justified—usually two to four years—provided immediate use can be found for the released capacitors when, at the end of that time, the transformers must be installed in any event. Usually, also, no coincidental installations of transmission or other capacity are on the books during that period, so that the hypothetical "released capacity" savings beyond the transformers are nil.

The fantastic savings mentioned above were realized when, after a small installation of capacitors had made the postponement effective for one year, the load unexpectedly dropped off. Had the transformers been installed we would now be saddled with carrying charges of perhaps $5,000 annually, but the capacitors, having effected that saving, have been transferred to other locations where they are earning their keep and compounding the savings.

To sum up the foregoing argument, it seems wise to ignore possible prorata kilovolt-ampere savings from "released capacity" unless their specific source can be pointed out. If capacitor installations are justified despite this omission, any real credit from such a source is "velvet."

We have been unable to formulate any general rule to indicate the economic preference for polyphase, increased wire size, boosters, or capacitors; each case requires individual study.

Series Capacitors

Public Service experience with series capacitors is adequately covered by Mr. Olmsted's bibliography. At the present time another similar installation is being considered, and like its predecessors it is proving to be a delicate engineering problem. Series capacitors resemble the well-known little girl with a little curl right in the middle of her forehead—when they are good they are very, very good.

L. M. Olmsted: Mr. Butler calls attention to the fact that the series capacitor, in addition to its ability to correct steady-state voltage conditions, has the unique advantage of being instantaneous in effect and thereby correcting fluctuating voltage conditions too rapid to be corrected by any of the other devices. In a paper specifically limited to the correction of steady-state conditions it seemed unwise to discuss fluctuating voltage. Consequently the series capacitor, found uneconomical for steady-state conditions, was dismissed with only a suggestion as to its other advantages. I am glad that Mr. Butler has discussed the most advantageous field for the series capacitor and corrected any impression that the series capacitor is never desirable.

Messrs. Arvidson, Bailey, and Jeynes all have commented on the credit for system capacity released by the power-factor improvement coincident with the correction of low voltage by means of shunt capacitors. Admittedly the value of this system capacity depends upon the ability to postpone the installation of additional system generating, transmission, substation, or distribution facilities. Every case is different, and the true value ranges from almost zero to such "fantastic savings" as mentioned by Mr. Jeynes. The same question arises in the evaluation of losses, in attempting to assign a fair increment above the fuel cost to cover the use of system investment. The figure of $50 per kilovolt-ampere of system capacity released by the capacitors is consistent with the capitalized valuation of losses (omitting generating capacity which is already operating above rated power factor). Raising or lowering this credit for released capacity to suit some particular conditions might affect the choice of shunt capacitors for economical correction of voltage, might even justify the installation of shunt capacitors where voltage is satisfactory solely to postpone system expansion, but it cannot affect the economic advantage which all of the supplementary regulating devices hold in common over conventional line rebuilding, the advantage of high reclaim value for use elsewhere.

Mr. Jeynes' comments indicate very close similarity indeed between Public Service findings and our own. Even though the statutory limits in Pennsylvania are more liberal, the many thousands of voltage checks made by our troublemen on their service calls indicate that 90 per cent of our consumers receive voltage within ±2.5 per cent of nominal. We consider it advisable to restrict the amount of boost at any one location to less than 5 per cent but favor two small steps instead of one larger step. The apparent difference in the method of compensating primary circuits for line drop probably is negligible, as we have only recently changed from the method described by Mr. Jeynes to the one described in this paper in order to secure more accurate supervision over the voltage actually supplied to all consumers at their service switches; it is designed to check the upper and lower limits of voltage instead of testing the average consumer.

All of our automatic boosters are equipped with time delay of approximately 50 seconds. This delay has been found desirable to permit close voltage settings without too frequent operations of the tap changers. No conditions have been encountered where this arrangement has permitted excessive operations.

Mr. Benner points out that line-drop compensation can be added to pole-mounted voltage regulators, after which shunt capacitors might be installed at the ends of the circuit. With this combination, the voltage boost of the shunt capacitors benefits the entire circuit during heavy-load periods and overvoltage during light load is prevented by bucking action in the pole-mounted regulators. This combination was not considered in the paper because it was thought desirable to keep pole-mounted equipment as simple as possible and also because of the difference in cost between pole-mounted regulators and two-step boosters, but its feasibility is proved by Mr. Jeynes' discussion and it may have greater economic advantages than I had anticipated.

The fixed booster undoubtedly gives inexpensive voltage correction and should be considered, especially if no value is placed upon the released capacity coincident with voltage correction by means of shunt capacitors. In one case fixed boosters have been considered to correct for drop in a long feeder, but the cost was high because of the current to be carried and shunt capacitors near the ends of the branches, credited with reduction in losses and released capacity, gave much lower net cost.

Mr. Hill points out a very interesting advantage which step-type regulators have over the induction type. The lower exciting current, in combination with lower price, certainly merits some thought.

All of the discussers have been most helpful in adding to the information available and in indicating various other problems to consider in the effort to secure necessary voltage correction in the most economical way.

The Rating of Electrical Machinery and Apparatus

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FELLOW AIEE

THIS PAPER outlines briefly some of the technical, economic, and psychological aspects of the methods of rating electrical machinery and apparatus. Some proposed modifications of existing standards also are discussed, with the idea of bringing the standards into better agreement with present-day conditions, knowledge, and practices.

I. Objectives of an Ideal Rating Structure

Before discussing the present structure of rating standards and any changes therein, it may be well to set forth briefly the objectives of an ideal method of rating.

1. The ratings assigned to individual machines or devices under any adopted structure of standards should convey correct and useful information to their users. Therefore, they should indicate the load which can be carried with safety either continuously or for specified periods, and they furthermore should give reasonable assurance of satisfactory and economical operation from every point of view under typical or normal conditions. This might well be considered the primary purpose in assigning ratings. The information conveyed by the method of rating should, of course, make possible a fair comparison of competitive commercial products.

2. The system of standards should tend to bring about the most economical all-round application of electrical machinery and devices.

3. The entire structure of standard ratings should be as simple as possible in order to avoid confusion and consequent misapplication. Thus, the various American standards should be as uniform as possible and they should be as closely in accord with the international standards as they can be without interfering with requirements resulting from the particular conditions prevailing in this country.

In any attempt to meet these objectives, the psychological aspects of the rating structure should be given as much attention as the technical and economic. It should be realized that if the stamp of approval is placed upon certain values of temperature rise and corresponding ratings by experts in the industry, it should, and does, carry considerable weight with the laymen, the less informed technicians, and the engineers and creates the impression that such temperature rises and ratings are approximately correct for normal operation in actual service. The fact that a simple method of rating cannot give complete information for all conditions of service is no reason why the rating given to a machine should not be indicative of typical or normal conditions. If, for instance, ratings for class *B* railway motors are based on a temperature rise of 105 degrees centigrade, as has been done in one of the International Electrotechnical Commission standards, instead of on a rise of 120 to 130 degrees centigrade, which has been demonstrated to be both satisfactory and economical in the United States, the operating engineer naturally will not load the motors in service so that they operate at the higher temperature rises. In other words, the IEC method of rating undoubtedly will result in uneconomical applications. This illustration brings out a point of view entirely different from the one frequently expressed, namely, that the method of rating is useful merely in facilitating a fair comparison between competitive products.

As in many similar cases, it is impossible to meet these objectives in full because recognition cannot be given to some conditions without interfering with others. For example, a rating structure taking into account the most economical application may not at the same time have the desired simplicity; in which case, a reasonable compromise between the conflicting objectives should be adopted.

II. Present Standards and Practices

Almost since the beginning of the electrical industry, the basic principle of rating electrical apparatus has been to specify the temperature rise permissible either for continuous operation or for limited periods. The basic considerations, limiting values of temperature, and methods of testing are covered in AIEE Standard No. 1,[1] while standards applying to specific types of machinery are covered in various National Electrical Manufacturers Association and American Standards Association rules. Standards for international use have been established by the IEC. Since the maximum temperatures reached by the insulation cannot be determined by any of the present methods of determining temperature, namely, the thermometer, resistance, and embedded-detector methods, values somewhat below the maximum permissible temperatures have been selected as standards. The difference between the maximum temperatures and those obtained with the available methods of test varies with different designs; however, values covering conditions found in conventional designs have been selected as standards for these differences.

Although AIEE Standard No. 1 gives all three methods of measurement, the ASA and NEMA standards recognize only one of the three methods for some types of machines and their individual parts, the thermometer method being given in the majority of cases.[2] In other instances, as, for example, the tentative standards for traction apparatus, both the thermometer and the resistance method are recognized, but "the resistance method is considered as the rule."[3] Both the ASA and IEC transformer standards[4] specify the resistance method for windings. The international standards for machinery[5] specify embedded detectors for large

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Paper number 39-6, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 31, 1938; made available for preprinting November 29, 1938.

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1. For all numbered references, see list at end of paper.

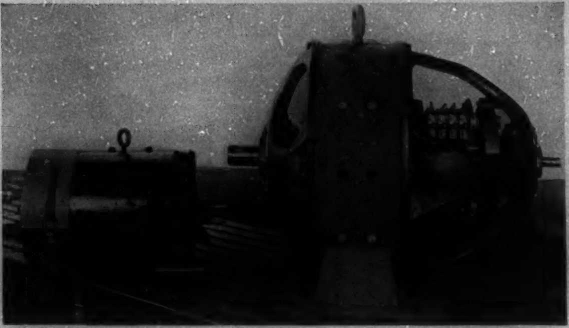

Figure 1. Comparison of a modern traction motor and a general-purpose industrial motor

Left—Traction motor, 125 horsepower based on temperature rise by resistance, 120 degrees centigrade in armature, 130 degrees centigrade in field; continuous speed, 1,450 rpm; weight per horsepower, 8 pounds

Right—Industrial motor, 125 horsepower based on temperature rise by thermometer, 40 degrees centigrade; continuous speed, 1,150 rpm; weight per horsepower, 25 pounds

machines, but in most other instances they recognize either the thermometer or the resistance method for windings, with implied preference for the resistance method. The thermometer method is of course specified for such parts as cores, commutators, etc., where the resistance method is not applicable. The same general practice is followed in the international standards for traction machinery,[6] but recent trends indicate that for this application the resistance method will practically eliminate the thermometer for any windings. (The subject of temperature measurement is discussed further in two contemporary papers.[7,8]) A greater degree of uniformity between these standards obviously is desirable, because with present-day knowledge there seem to be no sound reasons for some of the differences existing.

The standard temperature rises for continuous ratings usually have been selected with the intent of permitting continuous operation without injury to the insulating materials under an assumed ambient temperature of 40 degrees centigrade. An exception to this is the new ASA transformer recommendations which specify operation at an average ambient temperature of 30 degrees centigrade and a maximum of 40 degrees centigrade. The tentative standards for traction motors assumed, by inference, an average ambient of 25 degrees centigrade and a maximum of 40 degrees centigrade.

In addition to the continuous ratings discussed so far, overload ratings with temperature rises higher than those specified for continuous load are provided by some of our national standard rules for some types of electrical machines and apparatus. This is considered justified by the fact that deterioration of insulation is a question of both temperature and time and that satisfactory life can be obtained if higher temperatures are maintained only for short periods and at not too frequent intervals. Other commercial standards specify short-time ratings, the most typical among which is the one-hour rating for traction motors, which like any other short-time rating indicates to a certain extent the heat-absorption ability of the machine. (Short-time ratings are discussed in a contemporary paper by L. E. Hildebrand.[9])

In some instances the basic temperature rises specified in AIEE Standard No. 1 have been departed from, usually, however, for sound technical or economic reasons. For example: The temperature rise, by thermometer, for general-purpose motors has been specified as 40 degrees instead of 50 degrees centigrade, the limit given in Standard No. 1. This was done for the reason that general-purpose motors are often applied where the load is not exactly known and also by nontechnical users, and a somewhat greater margin of safety was therefore considered advisable when the 40-degree standard was adopted. An extreme in the other direction is the temperature rise of 120 degrees, by resistance, specified for the armature and 130 degrees for the field in the tentative ASA standards for traction motors in comparison with the 75-degree rise specified in AIEE Standard No. 1. For traction work, higher temperatures and possibly a shorter life of the insulation may be justified by the economic gains possible through a reduction in the weight of and space occupied by the motors. Those familiar with railway problems know how important a reduction in size and weight is, not only in the motor itself but also in the resultant reduction in the truck and other parts. The use of small motors with higher temperatures in locomotives often makes it possible to reduce the number of driving axles and thus materially reduce the length and weight of the entire locomotive. In class *A* railway motors, armature coils have been found to have a shorter life than in industrial applications, but the expense for rewinding more frequently is compensated for by other economies gained. There is considerable evidence that class *B* insulation used in railway motors lasts almost indefinitely in spite of the increased temperature rises over those given in other standards. It is thus evident that in railway work a departure from the usual limits of temperature rise is fully justified by both economic considerations and actual practice.

Figure 1 shows the contrast in size which has resulted from the two most extreme departures from the temperature rises specified in Standard No. 1. At the left is a modern traction motor with class *B* insulation and designed for temperature rises (by resistance) of 120 and 130 degrees for the armature and field, respectively. At the right is a general-purpose motor of the same horsepower rating designed for 40 degrees temperature rise (by thermometer). The difference in the size of the two motors is marked; the weight of the traction motor is only about one-third that of the industrial motor. The speeds of the motors are different, but in both cases are in accord with those commonly used in the most modern practice for the particular applications. The fact that the large machine is not appreciably higher in cost than the small traction motor is surprising, but this is merely further evidence that the practices established for the two extreme cases are justified for the particular conditions to be met. While the adoption of the railway practice for general-purpose motors would result in reduced dimensions and weight, this would be of little value in most applications. On the other hand, it would mean disadvantages such as less accessibility of parts, reduced overload capacity on account of reduction in mass, etc. The condition illustrated by the particular case cited—namely, that higher temperature rises are instrumental in reducing weight and size but have only a minor effect on cost—is found to hold true in many cases, particularly with the smaller sizes. The reason for this is that under crowded conditions of assembly, the expense for labor is likely to increase, and also because a change from class *A* to *B* insulation means an additional increase in cost of material with the present market prices of the class *B* materials involved.

The temperature rise of 55 degrees specified in the ASA standards for enclosed ventilated motors is another departure from the value specified in AIEE Standard No. 1. The difference of 5 degrees was adopted on the ground that temperatures within enclosed motors are usually more uniform than in open motors, resulting in a smaller difference between the hot-spot and measurable temperature. Furthermore, there is likely to be a decreased tendency toward deterioration of the insulation in fully protected windings.

It was early realized that the maintenance of safe temperatures was not the only condition to be met by commercial machines and apparatus. In addition to this, generators and transformers must have proper regulation; motors of all kinds must have sufficient starting and pull-out torque to meet prevailing service requirements; all commutating machines must give satisfactory commutation; machines and apparatus of all kinds must have efficiencies resulting in economical operation; a-c machinery must meet certain power-factor conditions; starting currents of motors must be limited to tolerable values; etc. In the realization of this, various standards set up by NEMA and ASA specify standard limiting values for the factors just mentioned. In arriving at these standard values, allowance has been made for reasonable variations in the power supply, particularly with reference to voltage. In practically all cases, however, the temperature rise remains the primary basis for rating and the other factors are usually looked on as supplementary standards.

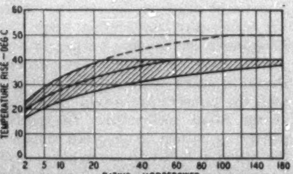

Figure 2. Band indicating actual temperature rises of general-purpose induction motors—four, six, and eight poles

In actual practice it frequently happens that in order to meet the requirements just mentioned, the apparatus has to be larger than dictated by temperature considerations, which in turn means that the actual temperature rises will be considerably below the specified standards, particularly for the lower ratings. In order to have economical manufacturing and stock conditions, the number of frame sizes for any line of machines or apparatus is limited, and as a result a frame size somewhat larger than necessary for the particular rating often has to be used, which again results in the actual temperature rise being lower than the specified standard. Figures 2, 3, and 4 show actual conditions for several types of apparatus.

Mention should be made of an increasing tendency to equip apparatus with thermal protective devices. Recent improvements in these devices and especially the high degree of protection which is possible with transformers are bound to result in better and broader utilization of the protected apparatus. At present the advantages of these protective devices are utilized in the application of apparatus, but eventually their use may influence the methods of rating.

III. Discussion of Changes in Rating Structure

One of the most important advantages of standards lies in the fact that valuable application experience is accumulated on the standards selected and any change nullifying such experience is likely to result in marked economic losses. Therefore, changes should be avoided unless worth-while advantages can be secured through them. In considering possible improvements in existing standards, it seems advisable first to consider minor modifications which would simplify and unify the present national and international structures of standards without causing difficulties of the nature just mentioned. Secondly, the various general practices which have been established and which are not in accord with the standards or are not recognized therein should be studied. If a critical examination of these practices indicates that they are sound in principle, the standards should be revised to recognize them. Finally, it should be determined whether on account of new developments and experiences, certain changes in well-established standards and practices resulting therefrom are justified by the economies accomplished in the industry as a whole.

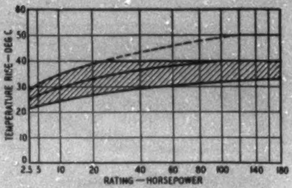

Figure 3. Band indicating actual temperature rises of general-purpose d-c motors having speed range of 850 to 1,750 rpm

Following are a number of specific suggestions which, with the exception of a few minor changes proposed, relate essentially to supplementary provisions in the standards and are more of an evolutionary character; consequently they will not appreciably affect any standards or practices already in existence.

1. Early attention should be given to the following modifications in AIEE Standard No. 1:

A. The value specified for the temperature rise (by resistance) of class *B* insulation should be changed from 75 degrees centigrade to 80 degrees centigrade, the value specified in the IEC rules. The experience previously cited with class *B* insulation on railway motors indicates that this change is perfectly safe; furthermore, it means no hardship to manufacturers since existing designs would meet the new standard. A change of the 55-degree value, by resistance, for class *A* insulation to the IEC value of 60 degrees also seems desirable if investigation shows it to be safe.

Better agreement between AIEE Standard No. 1 and IEC standards could further be reached by lowering the class *B* thermometer value of 70 degrees in AIEE Standard No. 1 to 65 degrees, the value specified by IEC. However, it seems that this would be a step in the wrong direction in view of our railway experience, and, furthermore, the new value could not be met by existing designs.

B. A "Class *B*-1 Winding Practice," as distinguished from the ordinary class *B*, should be established.

The fact that long life and undoubted economies have been obtained in traction work with temperature rises for class *B* insulation far in excess of those specified in Standard No. 1 naturally suggests the idea that this be recognized in any revision of this standard. In formulating the new standard, a number of stipulations could be made in addition to those for ordinary insulation. Among them, the use of high-temperature soldering or brazing materials for joints and bands could be specified, as has been found expedient in railway practice. Precautionary statements could be added,

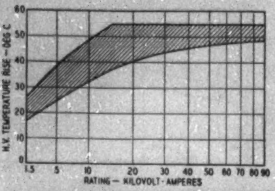

Figure 4. Band indicating actual temperature rises of distribution transformers of 2,400, 4,800, 6,900, 11,500, and 13,200 voltage classes

indicating, for instance, that the higher temperature rises should be adopted only where previous experience with machines of similar size and construction has demonstrated that satisfactory operation and life can be expected. (Various considerations which enter into this question other than direct deterioration of insulation by temperature are covered in a companion paper by C. Lynn.[10]) The availability of such high temperature standards would tend to establish a uniform practice if and when advantage is taken of the traction experience in other fields, such as aviation, navigation, and special industrial applications. Some consideration might be given to similar action in the use of class *A* insulation if this appears to be justified by a further study of operating experiences.

(As a matter of course, proper provisions for the application of glass insulation in connection with class *B* insulation and the suggested "Class *B*-1 Winding Practice" should be made in AIEE Standard No. 1 at the same time.)

C. Standard No. 1 should be revised to formulate and to recognize more definitely methods of rating and application other than those based on indefinite life with continuous full-load operation at an ambient temperature of 40 degrees centigrade.

Standards for reduced life expectancy should be established for use where this is considered economical. Recognition should be given to the usual condition of low average ambient temperatures, as has been done in the new transformer and traction apparatus rules. Short-time and intermittent-load conditions also should be covered by suitable provisions. These steps seem advisable because engineering and economic considerations cannot, and should not, be separated. A basic standard such as No. 1 should recognize their interrelation and establish guiding principles sufficiently flexible to cover varying economic conditions. Here again the available railway experience and practice may well serve as a guide. However, consideration should also be given to other economic conditions, as, for instance, motors built into various types of machinery, where on account of obsolescence or other factors, short life of the machinery is to be expected. In electrical machines for automobiles, for example, it would be a decided waste to apply insulation for an indefinite life expectance. Valuable data are given on intermittent-load applications in a contemporary paper by Alger and Johnson.[11]

Some of the points suggested may be covered by guiding principles for the application, but in other cases the rating structure may be involved and the most suitable procedure will have to be determined by further study.

2. In addition to the modifications just discussed, the following situations should be studied with the idea of improving various standards if and whenever revisions appear to be advisable.

A. As is generally appreciated, an ambient temperature of 40 degrees prevails only over relatively short periods in the majority of applications. This, together with the fact that deterioration of insulation is a function of both temperature and time, provides a margin of safety in all but a few exceptional applications. This and the further fact that machines and apparatus carry full loads continuously only in rare applications, makes it doubtful that the extra 10 degrees provided by the 40-degree rise of general-purpose motors is really warranted. However, a study of figures 2 and 3 indicates that up to 100 horsepower, the temperature rises of commercial machines would in general be below 40 degrees even with a 50-degree standard because the size of the machine is appreciably influenced by other considerations, such as starting and pull-out torques, power-factor values, commutation, etc. In other words, even granting that the additional margin provided by the 40-degree rating is not really necessary, no marked economies could be obtained by the 50-degree standard with the present established standards of performance relating to factors other than temperature. Therefore, at this time there seems to be no valid reason for disturbing the 40-degree standard for general-purpose motors up to about 100 horsepower; in fact, there is considerable merit, under conditions indicated in figures 2 and 3, in adhering to the 40-degree standard because it has the advantage of letting the user know that he can make use of the extra margin whenever conditions make this desirable. Thus the established standard of 40 degrees will lead to economies in some cases rather than interfere with maximum economy. The dotted lines in figures 2 and 3 show the temperature rises which might be obtained in actual practice with satisfactory all-round standards of performance if a 50-degree standard were established for 100 horsepower and above. From the figures it will be seen that slight economies might be obtained by limiting the 40-degree standard to the range below 100 horsepower.

Figure 4, applying to transformers, indicates that owing to the necessity for obtaining satisfactory regulation, the actual temperature rises in transformers are usually below the established standard of 55 degrees for ratings up to about 15 kva. Therefore, applying the same line of reasoning as given for general-purpose motors, a lower standard of temperature rise could be established for the lower ratings. On the other hand, since transformers are usually applied by specialists in electrical engineering who are in general familiar with the conditions indicated in figure 4, there is little need for making a change in well-established standards of rating. Furthermore, the increasing application of thermal devices indicating temperature rises in service may eventually assure the fullest and most efficient utilization of transformer capacities. This situation is covered fully in a paper previously presented by Messrs. Putman and Dann.[12]

B. Careful tests have demonstrated that if the oil in transformers is protected from contact with air so that it will not absorb any oxygen, there is practically no deterioration in insulating materials at the limits of temperature suggested by the AIEE and ASA standards. Similarly, there is considerable evidence that the insulation of hydrogen-cooled machines is subject to less deterioration than machines cooled by air. While for the present these advances may be taken advantage of by the methods of applying these machines and transformers with neutral atmosphere, serious consideration eventually should be given to the recognition of such improved operation in the AIEE and other standards. A careful study of the conditions necessary to assure this improved operation and their inclusion in the standards will be advisable.

C. Although there was merit in a single standard for the measurement of temperature by thermometer specified in many of the ASA and NEMA rules as long as the windings and other parts of machines were readily accessible, recent trends make it necessary to recognize in these standards the measurement of temperature by the resistance method, at least under some conditions. This change is brought about by the fact that an increased number of machines is being built in a way which makes windings and other hot spots inaccessible. Even where these parts may be reached by removing covers, too much time usually is consumed in doing so to permit accurate readings. In other apparatus, such as refrigerators and air-conditioning equipment, the motors are enclosed in such a way as to make thermometer readings entirely impossible or useless. In this connection, it should also be considered that in other countries the resistance method is generally preferred for motors, transformers, and generators for industrial and power purposes. More recently, at the insistence of the American representatives and others, the resistance method has been adopted by the IEC as the governing method for use with the windings of traction motors. This is another reason for giving more recognition to this method in many of our standards, although its immediate adoption in all cases and particularly in those where thermometer readings are possible, may not be found advisable.[8]

A proposition has been made recently, especially in a paper by Rutherford,[13] that a motor be rated on the starting torque per horsepower, accelerating torque per horsepower, and starting torque per ampere locked-rotor current. These factors should be taken into consideration along with the temperature rise of the motor at the rated horsepower. In another paper,[11] a system is proposed whereby the rating is based on a combination of temperature and starting current, the latter serving as an approximate measure of the torques. These methods are fully described in the papers and are mentioned here principally for the sake of completeness.

Although the action suggested in this paper is by no means revolutionary, it would nevertheless tend to improve the existing structure of ratings appreciably by making it more uniform and putting it on a sound economic basis. It is evident that any standard must be kept up-to-date and that progress will be retarded materially and certain economies will not be accomplished if there is too much delay in recognizing economically and technically sound developments through suitable modifications and supplements to our standards. A careful study of the points mentioned here and also others given elsewhere relating to standards would therefore seem appropriate, and it would seem that a revision of AIEE Standard No. 1 in particular is in order. Although mention has been made here of specific types of apparatus for the purpose of illustration, this should not be construed as suggesting that any specific type of apparatus be covered by Standard No. 1. This standard should in the future, as in the past, deal only with general principles and serve as a guide in establishing other standards for specific lines of machines and apparatus.

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12. Loading Transformers by Copper Temperature, H. V. Putman and W. M. Dann. AIEE Transactions, volume 58, 1939, pages 504–14.

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Discussion

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): No doubt there is justification for revision of certain present standards; however, extreme caution should be used in making any changes. It is unfortunate that there is not sufficient data available showing the relationship between operating temperature and the thermal life of various types of insulation. Consequently, the results of increasing the operating temperature of equipment cannot be predicted and our experiences may be costly unless caution is used in revising present standards.

Experience indicates that the temperature rise of a machine is indicative of its reliability and, for certain equipment applications, the permissible temperature rise should possibly be reduced. This is especially true for large machines where the loss of a machine usually results in serious consequences. Even under modern conditions, design errors and contingencies of manufacture and operation make it advisable to provide a margin of safety in the thermal design of a machine.

The reliability angle of this problem could be minimized by the development of suitable nondestructive tests which would make it possible to anticipate insulation failures. If it were possible to schedule replacement or rewinding of equipment, it would probably be possible to obtain an economic basis for increasing the permissible temperature rise of equipment.

R. E. Hellmund: The appeal made in the discussion of my paper for the use of extreme caution in changing standards is well justified and is in accord with statements in the paper itself. However, caution must not be carried to an extreme contrary to sound economic principles. It obviously would not be economical to establish rating standards which would give safe operation under any service conceivable; this would result in enormous waste in the majority of applications. The basic standard should be such that it results in safe and reliable operation in the large majority (possibly 75 to 85 per cent) of all applications, with a definite understanding that the remaining cases will be given special consideration in some way or other. The subcommittee of the AIEE standards committee, on basic principles for rating of electrical machines and apparatus, expects to undertake very extensive studies and to sponsor further papers and discussions on such phases as ambient temperature, life of insulation, allowances between hot-spot temperatures and temperatures measured by thermometer and resistance, etc.

I am at present of the opinion that the data so far available do not indicate the advisability of departing appreciably from present basic standards such as the temperature rise of 50 degrees by thermometer now specified in AIEE Standard No. 1 for class *A* insulation. In some cases this might be supplemented or replaced by the practically equivalent rise of 60 degrees by resistance. The practice of having more liberal commercial standards with temperature rises of 40 degrees by thermometer (possibly supplemented or replaced in some cases by a value of 50 degrees by the resistance method) probably should be continued to take care of cases where service conditions are more severe or where a greater margin of safety is desirable for some reason or other. Similarly, it may not be found advisable to change existing values materially for conventional class *B* applications, or those values which have proved satisfactory for transportation work and applications with similar economic and service conditions. However, final recommendations on all this should be withheld until some further investigational work which is now under way has been completed. During this period of study, it would be very helpful if operating engineers would assist to a greater extent in supplying pertinent and reliable data gathered from actual service experience.

Loading Transformers by Copper Temperature

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MEMBER AIEE

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FELLOW AIEE

THE PURPOSE of this paper is to describe a new practical method of utilizing latent short-time overload capacity to the fullest extent, dependent upon actual copper temperature and automatically taking into account the factors that affect the life of a transformer.

It seldom happens in service that all the conditions established for the rating of a transformer exist concurrently and continuously and it is seldom the case that its maximum capacity is actually used for any great length of time. For these reasons it is realized that under many conditions transformers have an unused latent load capacity over and above their ratings, particularly for short time periods. That latent capacity depends upon the temperature to which the insulation can be safely exposed and how long it is exposed to it. Obviously ambient temperature plays its part for copper temperatures rise and fall with ambient temperature and the latent capacity is greater when the ambient temperature is low.

Users of transformers are naturally interested in making use of this latent overload capacity if a safe way can be devised for doing so. It is desirable for at least two reasons: first, the short-tine overload capacity may be needed in emergencies due to the failure of some part of the system; second, short-time overload capacity can be used to carry peak loads and in that way reduce the average size of transformer required for a given load. The proper utilization of short-time overload capacity is therefore desirable in order to reduce costs and improve quality of service.

But how can the short-time overload capacity of transformers be utilized in practical system operation? The customer's load usually determines the loading of any particular unit and as a rule loads cannot be varied at will. In the case of distribution transformers, considerable effort on the part of the utility company is required in the direction of load surveys to determine the extent to which transformers are loaded. In the case of power transformers, graphic records of actual loads may be available, and in some cases hot-spot temperatures, but ordinary overload protection schemes will relay out a power transformer long before its maximum short-time overload capacity has been reached.

There are two parts to the problem of making use of latent overload capacity: first, the safe limits of temperature for different loads and time periods must be known; second, a practical method of operating up to but not beyond these temperature limits must be available.

Operating Temperature and Useful Life

Attempts have been made to establish a system of relationships between operating temperature and the useful life of a transformer. While they have been based upon data which are accepted as reliable under the conditions of test and upon reasoning which is not generally disputed, they are so far largely of academic interest as reports of progress rather than as tools of complete practical value in making use of the latent overload capacity of a transformer. Obviously, the positive and direct way of determining accurately the relation between life and temperature would be to provide a large number of transformers and load them variously for long periods of time. That is clearly impracticable on a large scale, but the tests reported in this paper reflect this positive and direct method of obtaining such results.

In 1930, V. M. Montsinger presented a comprehensive paper[1] before the Institute in which was stated the theory that the rate of deterioration of insulation in oil is doubled for each increase in temperature of eight degrees centigrade and, conversely, is reduced one-half for each decrease of eight degrees centigrade. This theory is widely accepted as trustworthy. Based upon this theory and a number of calculations of temperature under various conditions of loading, Mr. Montsinger built up a relationship between temperature and the useful life of a transformer in years. One conclusion reached was that a self-cooled transformer having an average temperature rise of 55 degrees centigrade and a maximum temperature of 105 degrees centigrade, actually operating at full load continuously in an ambient temperature of 40 degrees centigrade, would have a useful life of about seven years. Tests made on insulating materials in open oil formed the starting point for his curve of relationships and for this conclusion.

In 1934, L. C. Nichols presented a paper[2] in which a method of relating temperature and life was proposed. The

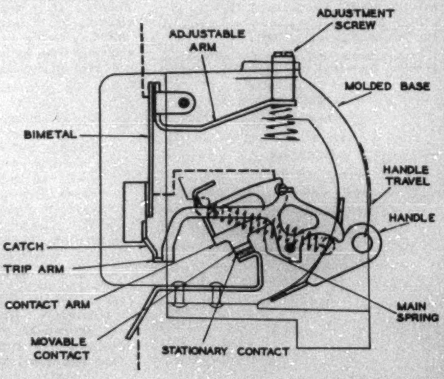

Figure 1. Schematic diagram showing the mechanism of the type "FR" thermal relay and the path of the load current through the contacts and the bimetal by dot-and-dash line

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Paper number 38-122, recommended by the AIEE committees on electrical machinery and protective devices, and presented at the AIEE Southern District meeting, Miami, Fla., November 28–30, 1938, and at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted August 31, 1938; made available for preprinting October 20, 1938.

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1. For all numbered references, see list at end of paper.

starting point for this system was based upon a five-year test made on insulating materials in open oil at 90 degrees centigrade. This system indicated a useful life of $1^{1}/_{2}$ years for a self-cooled transformer having an average temperature rise of 55 degrees centigrade, maximum temperature of 105 degrees centigrade, and operating continuously at full load.

Systems such as these can be built up acceptably so far as relationships between different operating conditions are concerned but they are vulnerable in that they finally must be tied into fundamental time-deterioration data of unquestioned reliability obtained either from tests which correctly represent operating conditions or else from actual operating experience. The difference between 7 years and $1^{1}/_{2}$ years of useful life derived from the two systems mentioned is an illustration of the point. A comparatively small change in the starting point of either system would result in considerable change in indicated periods of useful life.

The recommendations for short-time overloading prepared by the AIEE transformer subcommittee[3] and the ASA sectional committee on transformers are based upon the eight-degree rule and upon actual experience in service, with perhaps a rather liberal interpretation of that experience. It is assumed, a little questionably perhaps, that certain relationships between maximum temperature limits and time have been demonstrated as correct because they have been used satisfactorily for many years for purposes of standardization. These time-temperature relationships are:

250 degrees centigrade for five seconds—used as a limit for short-circuit conditions

160 degrees centigrade for one minute—used for grounding transformers

95 degrees centigrade for continuous operation—this is the hottest-spot winding temperature of a 55-degree transformer operating in 30 degrees centigrade ambient

A curve drawn through these three points gives a complete relationship between temperature and useful life. It may be said of each of the three points that they perhaps have not been actually and consistently reached in average operating experience, and for that reason, they do not accurately represent actual experience. If so, they merely err on the conservative side.

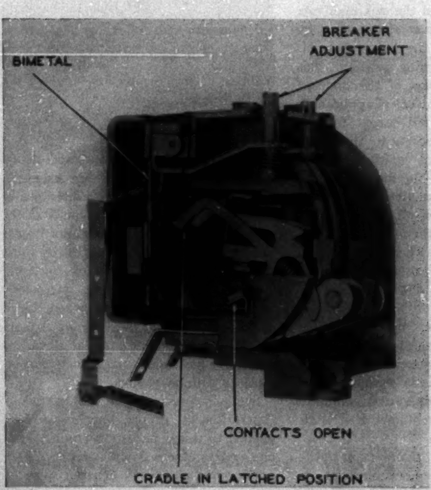


Figure 2. The type "FR" copper temperature relay showing the mechanism in the tripped position

Connected in series with the transformer winding, either directly or through a current transformer, it provides the means for overload relaying directly by copper temperature, thus permitting overloads of any size or duration until the limit of copper temperature is reached

The type "*TR*" relay, a somewhat more elaborate device, gives an advance warning light signal at a copper temperature about 30 degrees centigrade under the maximum safe temperature

Operation With Inert Gas

Temperature-deterioration data so far published have all been acquired in tests of materials in open oil with the oxygen of the air contributing a certain measure of deterioration to the materials tested. In contrast with such tests, elaborate long-time tests of coils and of the commonly used insulating materials have been made in oil protected against the harmful effects of air by means of "inertaire." As was to be expected, these tests showed that insulating materials suffer appreciably less deterioration and have longer useful life for a given temperature. In other words, they show that insulating materials in oil protected against contact with air will withstand appreciably higher temperatures than in open oil. The conclusions drawn from these carefully made tests indicate clearly that there is practically no deterioration of insulating materials at the limits of temperature suggested by the Institute and the American Standards Association.

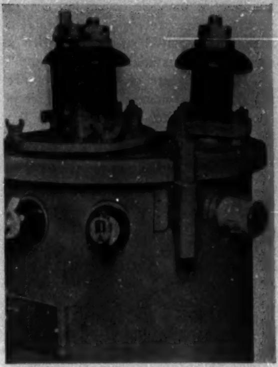


Figure 3. The indicating lamp as used with distribution transformers

When the copper temperature reaches the AIEE limit of temperature, this warning signal comes on. Even though the load decreases, the lamp remains lighted until it is reset, thereby indicating that at some time the load on the transformer reached its maximum safe value

The practical benefits accomplished with inert gas in maintaining the oil in the best possible condition and in reducing the rate of deterioration of the insulation have come to be very generally understood and appreciated. The trend in transformer practice has been toward operation with inert-gas protection or, as the next best thing, toward the use of gas-tight cases which limit the amount of air in contact with the oil and prevent the addition of more air. The best practice, except for inert-gas protection, is to make use of rugged gas-tight cases that do not allow the inbreathing of fresh air, with enough air space to limit the internal pressure to a reasonable value at the maximum operating temperature. In making cases gas-tight, the gasketing of joints between covers and tanks and at bushings and handhole or manhole covers has always been a serious problem. That problem seems to be solved successfully with a new gasketing material called "corprene."

Today designers of transformers are thinking not of name-plate ratings alone but are giving careful consideration to the design of bushings, leads, tap-changers and the like, to see that they will not constitute limits which would prevent the complete utilization of whatever latent overload capacity is otherwise available.

The useful results of making transformers gas tight are perhaps not generally recognized. The practice has an appreciable effect in reducing the deterioration of oil and insulation and in giving the transformer longer life. It contributes a measure of increased latent overload capacity. It has been proved that considerable short-time overload capacity is available in distribution transformers of gas-tight construction and that it can be utilized with the method of loading by copper temperature about to be described.

Loading by Copper Temperature

The term "loading by copper temperature" has been applied to the practical utilization of short-time overload capacity which has been developed and applied to distribution transformers of the "*CSP*" type, and more recently to "*CSP*" power units. Briefly, the method consists of relaying directly by copper temperature in such a way that the transformer will carry any useful overload until its maximum safe temperature is reached before it automatically disconnects the load. Under short-circuit conditions, however, it will disconnect the load immediately.

The dividing line between useful overload range and short-circuit range is largely an arbitrary one which can be changed by adjustment of the relay. Experience shows that for distribution transformers the useful overload range should extend up to 5 or 6 times normal load, while for power transformers a range up to $3^1/_2$ to 4 times normal load proves satisfactory.

The relay which accomplishes these desirable results consists of a suitable contact mechanism actuated by a bimetal or thermostatic element which is immersed in the same oil as the winding and which carries the same current or a proportional current obtained with a current transformer. Figures 1 and 2 show a schematic diagram and a picture of the simplest type of this relay.

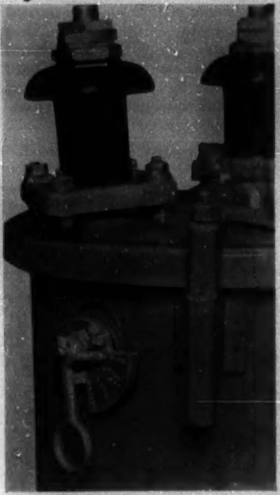


Figure 4. Operating handle of the relay

Used with the distribution transformer to reset the indicating lamp and breaker after tripping. The handle can also be used to open and close the breaker, a convenience during the installation of the transformer

It is shown in the appendix that the temperature of the bimetal can be made to follow the copper temperature in such a way that it always arrives at the tripping temperature whenever the copper of the winding reaches the maximum safe temperature. It is also shown that, by proper correlation of the design of the bimetal with the winding gradient, this will be true regardless of the ambient temperature. This statement applies only to the useful overload range, for the breaker trips immediately in the short-circuit range without waiting for the copper to arrive at its maximum safe temperature, as the analysis of the appendix clearly shows. The higher the short-circuit current the faster will be the tripping. The point where this immediate tripping begins can be changed at will by adjusting the relay.

The type "*TR*" relay, which is somewhat more elaborate than the simple "*FR*" type shown in figures 1 and 2, embodies an arrangement whereby a signal gives an advance warning of an approaching high-temperature condition which might cause an outage if it is allowed to continue.

In the small "*CSP*" distribution transformer, the relay element operates an internally mounted circuit breaker directly and the warning signal is an indicating lamp (see figure 3). In the case of the power transformer, the relay is connected in the control circuit of the main circuit breaker and the same warning signal is used.

Advantages of "Loading by Copper Temperature"

The simple arrangements described above can be very helpful in system operation for minimum cost and maximum service continuity. For example, consider their application to distribution transformers. Smaller units can be safely selected initially because it is not necessary to provide for future growth since there is little possibility of burnout. As loads gradually build up on certain transformers to the point where large units are required, the warning signals will automatically indicate the situation and those particular transformers can be replaced with larger sizes.

Similar advantages are realized when "operation by copper temperature" is applied to power transformer units. On one system where a number of "*CSP*" power units similar to figure 5 were in

Figure 5. A 1,000-kva "CSP" power transformer

This transformer is provided with a thermal relay for operation by copper temperature. In an emergency, this transformer carried 190 per cent load for two hours, safely and without a service interruption—a good example of the practical use of latent short-time overload capacity operation, it was necessary because of emergency repair work to maintain service, to place a 1,900-kva load on a 1,000-kva three-phase transformer. This condition existed for two hours before the load could be reduced. Toward the end of that period the warning lamp signal operated indicating an approach to a dangerous temperature, but the point of breaker operation was not reached. The load temperature curves for this unit indicated that it would be expected to carry such an overload following full-load temperatures for somewhat over two hours before tripping out on copper temperature. This illustrates clearly the value of operation by copper temperature in eliminating unnecessary service interruptions.

In establishing limits of copper temperature for the "*CSP*" transformer, the signal light is set to operate at about 95 degrees centigrade average copper temperature by resistance. This is the limit recognized in the AIEE Standards. It is the purpose to set the tripping point of the breaker as high as possible without burning out the windings in order to eliminate all but absolutely necessary interruptions due either to short circuit or to really dangerously high temperatures. For the same reason it is felt desirable to design the bimetal so that it will permit higher temperatures for short periods of time than for long periods rather than to trip at the same maximum temperature regardless of the duration. This accounts for the "hump" at about one hour in the curves of winding temperature in figure 7. To obtain this characteristic a somewhat lower bimetal resistance is used than is given by equation 7 in appendix I.

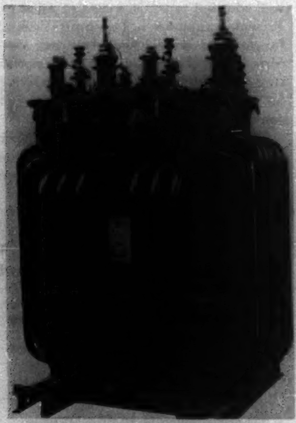


Figure 6. A 333-kva 7,200-volt single-phase transformer equipped with a thermal relay for operation by copper temperature

The maximum average temperatures reached for different overloads on a five-kva "*CSP*" transformer are shown in figure 7, together with the corresponding overloads. It will be found that for overloads of several hours duration the maximum temperature permitted is about 120 degrees centigrade, while at approximately 328 per cent load for $1^1/_4$ hours the maximum temperature is 145 degrees centigrade. For still larger loads and up to the short-circuit range, maximum temperatures are less.

Tests of the New System

If the breaker is set so that a maximum average copper temperature of 145 degrees centigrade is reached under some conditions, it would be natural to inquire if such a temperature would not damage the winding and how many times a winding could be subjected to such a temperature before failure.

To answer these questions, a program of temperature-cycle tests was started on four five-kva "*CSP*" transformers on June 1, 1936, and is still in progress. These transformers have been operated back to back in two groups, the units of the first group being designated as *A* and *B*, and in the second group as *C* and *D*. A summary of the tests is given in table I.

The procedure has been as follows: Starting at room temperature, a load of 278 per cent was applied to units *A* and *B*, and 350 per cent to units *C* and *D*. These loads were maintained until the breakers tripped on copper temperature in each case. Figure 7, which gives the characteristics of these particular transformers, shows that the 275 per cent load would trip the breaker in about two hours at average copper temperature by resistance of about 132 degrees centigrade. Similarly, the 350 per cent load would be carried about one hour and would result in an average copper temperature of approximately 145 degrees centigrade at the tripping point. These values assume an ambient temperature of 25 degrees centigrade, while during the tests the actual ambient temperature varied roughly from 17 degrees centigrade to 32 degrees centigrade during the summer and winter seasons. The durations of the overloads were correspondingly affected, but the attained copper temperatures were not measurably affected. These cycles of overload and trip-out were repeated continuously during the complete runs.

At the end of the first run, transformers *A* and *B* had been subjected to 100 cycles of operation at 275 per cent load to the tripping point, and trans-

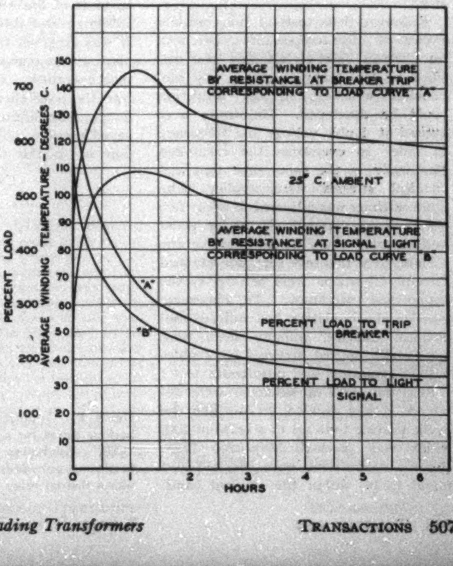


Figure 7. Typical short-time overloads for a five-kva distribution transformer

Curve *B* shows the load required to light the signal light for various time periods and curve *A* correspondingly shows the loads required to trip the breaker formers *C* and *D* had been subjected to 100 cycles at 350 per cent load. At this point the regular AIEE dielectric tests were applied to the transformers, the dielectric strength of the oil was measured, and the breaker calibrations checked. The transformers passed their dielectric tests, the dielectric strength of the oil averaged about 30 kv, and breaker calibrations were found to be within the proper bands.

Table I. Summary of the 660 Cycles of Short-Time Overload Operation of Five-Kva "CSP" Distribution Transformers

Run Number	Transformers	Load (Per Cent)	Cumulative Number of Cycles	Attained Maximum Copper Temperature by Resistance (Degrees Centigrade)	Average Duration of Each Overload (Minutes)
1	*A* and *B*	275	100	132	120
2	*A* and *B*	390	192	142	48
3	*A* and *B*	390	500	142	48
4	*A* and *B*	390	660	142	48
1	*C* and *D*	350	100	145	58
2	*C* and *D*	450	192	135	34
3	*C* and *D*	450	500	135	34
4	*C* and *D*	450	660	135	34

In starting the second run it was decided to increase the severity of the tests and, accordingly, the loads were increased to 390 per cent and 450 per cent, respectively. After 92 additional cycles, standard AIEE dielectric tests were successfully applied, the dielectric strength of the oil was found to average 35 kv, and a check of the breaker calibration showed that it was still in the band.

Following these tests of 192 complete cycles of high-temperature operation, an ignitron short-circuit test was applied to two units to see if any mechanical weakness in the insulation could be developed. The units were excited at double voltage and frequency in order to overstress the insulation electrically at the same time that mechanical stresses were applied. The ignitron timer was adjusted to apply five short circuits spaced six cycles apart and repeated every 15 seconds for one minute. By this test very definite and forceful vibrations were set up in the transformer windings. To determine whether the insulation had suffered mechanical damage, impulse tests were successfully applied, followed by double-voltage excitation and ratio tests.

Since no signs of weakness were detected, it was decided to continue the cyclic loading tests up to a total of 500, which was reached May 25, 1938. Breaker calibrations were checked and found to be within the original band.

Dielectric strength of the oil was measured on the four units and found to be 33.7 kv, 36 kv, 34.3 kv, and 30.7 kv. The oil was reported dark red wine color and cloudy, but oil ducts were clear and temperature measurements at this point checked the original measurements. Impulse tests were repeated successfully, also the AIEE dielectric tests, including a one-minute induced test at 400 per cent of normal voltage.

The cyclic loading tests were again continued and by June 15, 1939, a total of 867 cycles will have been completed. The tests were started June 1, 1936, and, as stated before, have been run almost continuously ever since.

Consequences of the Tests

Frankly, the results are surprising, in view of life tests on samples of insulating materials which have been made. It was expected that failure would take place after a comparatively few cycles of such operation. The tests seem to indicate the possibility of higher short-time temperature limits than have been believed possible. They also raise a question as to the reliability of methods heretofore used for determining maximum temperature limits. As has been stated, limiting temperatures have generally been established by testing individual samples of insulating materials over extended periods in oil under various conditions and measuring the depreciation in mechanical strength.

In view of these results, it may be in order to suggest that the proper committees of the AIEE review the whole subject of maximum temperature limits of insulating materials.

In conclusion, it can be said that transformers designed for operation by copper temperature and meeting, at least approximately, the gradient conditions set forth in equation 13 of the appendix, do possess desirable short-time overload capacity which can be used for greater economy and reliability in system operation.

The simple method of operation by copper temperature described here seems well adapted to transformers of all sizes. Present equipment has no limitations so far as size or voltage class are concerned. Operation by copper temperature does not preclude the use of relaying on ground faults, or reverse power, or of conventional reclosing practice.

The advantages of operation by copper temperature are so obvious that one might predict its almost universal use in the not distant future.

Appendix—Characteristics of a Transformer Bimetal Element Under Oil

Let

T_o = oil temperature or initial temperature of the bimetal

T_t = trip temperature of the bimetal

T = temperature attained by the bimetal at any time t

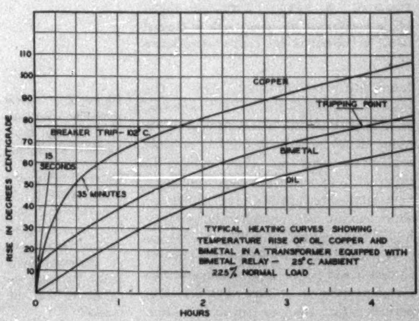


Figure 8. Typical heating curves for a "CSP" distribution transformer provided with a thermal relay

T_{oL} = oil temperature corresponding to load L

T_{RoL} = oil temperature rise corresponding to T_{oL}

I = current through the bimetal

I_n = normal full load current

L = steady-state load required to give the maximum desired hot-spot copper temperature

W = watts input to the bimetal

R = total resistance of the bimetal

N = number of times full-load current required to produce immediate tripping

p = specific heat of the bimetal—watt seconds per gram per degree centigrade

m = mass of bimetal in grams

q = coefficient of heat convection of the bimetal in watts per square centimeter per degree centigrade

a = area of the bimetal in square centimeters

Q = aq = watts dissipated from bimetal per degree centigrade gradient

t = time period

If a current is suddenly caused to circulate through a bimetal immersed in oil at temperature T_o, the I^2R loss will partly heat the bimetal and partly be lost to the oil. Thus

$$Wt = pm(T - T_o) + \int_0^t (T - T_o)aq\,dt \quad (1)$$

Differentiating with respect to t gives:

$$\frac{dT}{dt} + \frac{aq}{pm}T = \left(\frac{W}{pm} + \frac{aq}{pm}T_o\right) \quad (2)$$

which is the differential equation for the temperature of the bimetal as a function of time. In solving this equation for an actual transformer it can be assumed that the oil temperature is constant since the bimetal transient is very rapid and will have disappeared entirely before there is any substantial change in the oil temperature.

The solution of this equation is easily shown to be

$$T = T_o + \frac{W}{aq}\left(1 - \epsilon^{-\frac{aq}{pm}t}\right) \quad (3)$$

In figure 8, heating curves are shown for an actual transformer. The sudden rise in the temperature of the bimetal near $t = 0$ is the transient given by equatioh 3.

As soon as the transient is over, that is, for large values of t, the temperature of the bimetal is given by

$$T = T_o + \frac{W}{aq} \quad (4)$$

and further increase in the bimetal temperature results only from the gradual rise in the oil temperature as can be seen from the curves of figure 9. It is apparent, therefore, that considerable time will ordinarily elapse before the bimetal attains the tripping temperature or the temperature at which it unlatches the breaker—the time being determined by that required for the oil to heat up.

However, it is apparent from equations 3 and 4 that if W is very large, as in the case of a short circuit, the temperature which the bimetal would attain almost immediately from equation 3 could be high enough to unlatch the breaker without waiting for the oil to heat up. Or

$$T_o + \frac{W}{aq} \text{ could equal } T_t$$

if W were sufficiently great.

The load or current at which this immediate tripping begins can be established arbitrarily by the proper selection of the bimetal resistance. The range during which immediate tripping takes place is designated here as the short-circuit range, while the range during which the tripping time is dependent on the heating of the oil is designated as the useful overload range.

Equation 3 is necessary for the calculation of the tripping characteristic of the bimetal relay in the short-circuit range.

Experience has taught that the useful overload range for distribution transformers should extend up to five to seven times normal current since transformers may be subjected to currents of this order due to motor starting or similar short-time overloads. In other words, N should be between five and seven. To bring about immediate tripping at any particular value of N, the bimetal resistance can be determined as follows:

$$T_t = T_o + \frac{N^2I_n^2R}{aq_1} \quad (5)$$

But under steady-state conditions the trip temperature must be reached at load L. Hence

$$T_t = T_{oL} + \frac{L^2I_n^2R}{aq_2} \quad (6)$$

q is not quite constant because the oil convection increases with increased oil temperature. q_1 is therefore used in equation 5 and is determined for T_o while q_2 in equation 6 is determined for oil temperature T_{oL}.

Equating (5) and (6) and solving for R gives

$$R = \frac{Q_1T_{RoL}}{I_n^2\left(N^2 - \frac{q_1}{q_2}L^2\right)} \quad (7)$$

where

$$Q_1 = aq_1$$

which is the bimetal resistance required to give a useful overload range up to load N and immediate tripping above N.

A highly desirable characteristic of the bimetal relay is that it compensates automatically for changes in ambient temperature permitting higher loads at low temperatures and less load at high temperatures in accordance with the thermal capability of the transformer. Obviously this comes about if the bimetal trips the breaker at the same maximum copper temperature regardless of ambient temperature. To bring this about the winding gradient must fulfill a condition determined as follows:

As the permissible steady-state load L changes with ambient temperature, if the bimetal trip temperature is to be reached at the same maximum copper temperature, then any increase in the bimetal gradient (W/aq) with increasing load must be offset by a corresponding decrease in the oil temperature with increasing load—or

$$\frac{dT_{oL}}{dL} = \frac{-dW}{dLaq} \quad (8)$$

But

$$W = L^2I_n^2R \quad (9)$$

$$\frac{dT_{oL}}{dL} = \frac{-2LI_n^2R}{aq_2} \quad (10)$$

But the transformer characteristics determine the relation between oil temperature and the maximum permissible steady load and varying ambient from which dT_{oL}/dL must be evaluated.

In general

$$T_{oL} = T_c - KL^x \quad (11)$$

where

T_c is the copper temperature corresponding to load L

KL^x = gradient

Usually x ranges from 1.5 to 2, but the heating curves of the transformer determine the exact value to use.

Differentiating (11) at constant T_c gives

$$\frac{dT_{oL}}{dL} = -xKL^{x-1} \text{ or } = -2KL \text{ (for } x = 2) \quad (12)$$

Equating (10) and (12) and solving for K gives

$$K = \frac{I_n^2R}{aq_2} \quad (13)$$

which is the gradient coefficient required for ambient temperature compensation, the value of R having been obtained from equation 7.

The gradient required by equation 13 will be found quite low, and if it cannot be obtained it may be desirable to change the bimetal resistance to give ambient temperature compensation with the K which can be obtained. In this case

$$R = \frac{KQ_2}{I_n^2} \quad (14)$$

and the maximum overload before immediate tripping would be

$$N = \sqrt{\frac{Q_1}{Q_2}\left(L^2 + \frac{T_{RoL}}{K}\right)} \quad (15)$$

which is obtained by substituting (14) in (7) and solving for N.

References

1. LOADING TRANSFORMERS BY TEMPERATURE, V. M. Montsinger. AIEE TRANSACTIONS, volume 49, April 1930, page 776.

2. EFFECT OF OVERLOADS ON TRANSFORMER LIFE, L. C. Nichols. AIEE TRANSACTIONS, volume 53, 1934, page 1616.

3. OPERATING TRANSFORMERS BY TEMPERATURE, W. M. Dann. AIEE TRANSACTIONS, volume 49, April 1930, page 793.

Discussion

F. M. Starr (General Electric Company, Schenectady, N. Y.): This paper is of considerable interest and importance because it presents new data on the ability of transformers, particularly of the distribution class to carry heavy overloads. Considerable has been written in recent years as to the proper thermal overloads which should be tolerated in transformers. Conclusions reached vary widely in degree, but in principle they are all in agreement that repeated overloads reduce transformer life and that there is a threshold of loading beyond which the reduction in transformer life is so pronounced as to make such overloads extremely uneconomical. This point of view limits its economics pretty largely to the life and cost of a transformer.

The distribution engineer must necessarily have a broader point of view since his problem is somewhat greater than getting the maximum possible usage out of his equipment. It is his job to design a distribution system to deliver a kilowatt-hour to the consumer within specified criteria of voltage regulation and service continuity at the lowest possible cost. In making his broad economic analysis to this end he finds that energy losses and voltage drop in the transformer and perhaps even revenue are just as important factors in determining the loading of a transformer as its thermal capacity. For example it has been found in certain localities that the most economical design of secondary networks, considering voltage regulation and energy losses as contributing factors, results in transformer loading of only 75 per cent of name-plate capacity.

The reaction of the distribution engineer to the conclusions in this paper is likely to be that he is getting and paying for thermal capacity in transformers that he cannot possibly use because of excessive voltage drops or transformer losses. He might justifiably ask, "Why not sacrifice some of this thermal capacity and give me a lower cost transformer or a lower impedance transformer." Because of basic design limitations it is difficult to obtain what might be called an ideally balanced design of transformer for the economic distribution system. There has been considerable progress in this direction, however. It seems to me important and significant that if a more perfectly balanced design of transformer embodying maximum economy as well as the desirable operating characteristics is to be attained in the future, distribution engineers must not impair such progress by specifying unusable and unnecessary thermal capacities which have characterized some designs of the past.

J. H. Christensen and **J. P. Hamilton** (both of Tennessee Valley Authority, Wilson Dam, Ala.): The temperature-cycle tests described by the authors were made to determine whether or not a maximum average copper temperature of 145 degrees centigrade would harm the winding insulation. The transformers subjected to this test were of the five-kva "*CSP*" type. The authors have shown that these tests, which were made continuously over a period of two years without an insulation failure, indicate that the present maximum copper temperature of 105 degrees centigrade may be entirely too low. If so, transformers of higher ratings could be designed with only slightly increased costs, provided the relays under discussion give satisfactory operation under field conditions.

In this connection, a question arises concerning the practical application of this relay, namely, what provision is proposed to facilitate the adjustment, maintenance, or replacement of these units in large power transformers.

The outstanding feature of this work is the novel means used to obtain, with safety, the fullest utilization of conventional insulation in transformers. It is interesting to note the similarity in the design and application of this feature to that of the type "H" Sentinel breaker used for small-motor overload protection.

W. C. Sealey (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The tests described in this paper show that a modern transformer will stand a great amount of abuse without actually failing. The previous tests and experience on the deterioration of insulating materials due to time and temperature are not challenged by the results of the tests. Very high overloads have been carried and the transformer has been able to operate successfully after being subjected to the high temperatures obtained. However, it has been demonstrated by these tests that the oil and insulation are not in first-class condition because of the high temperatures to which they were subjected. The case is a good deal like that of automobile tires. There are tires in operation in which the tread has worn smooth and even tires in which the fabric is showing. These tires operate successfully, but, if reliability and safety of service is a factor, they cannot be considered to be in satisfactory operating condition.

These tests show that even with insulation in poor condition the transformers may still operate successfully. Since the oil deterioration was considerable for these tests and the insulation next to the copper must have been at a considerably higher temperature than the oil temperature, there is little doubt that the insulation had been damaged.

The attempt to set up certain definite safe temperatures for transformers is fundamentally difficult because there is no such thing as a safe temperature unless the time element and the permissible damage is also considered. Reliability of service is generally a first consideration, so much so that it may be desirable to work the transformer up to the absolute limit of failure before allowing service interruption. Any protective device which is put in for the purpose not of insuring continuity of service but for the purpose of protecting the transformer must reduce the total load which can be carried by a transformer without service interruption, otherwise no protection for the transformer is obtained. If the protection is set so high as to allow considerable damage to the transformer insulation, it may be well to omit the protection and allow more damage to the transformer insulation. The particular amount of damage which can be allowed before the transformer is tripped off is different for different applications but, in general, the best continuity of service is obtained by protecting the transformer only against real short circuits and not for overloads caused by useful load on the system.

It has been found that thermal devices which protect the transformer for the generally considered safe operating temperatures provide undesirable service interruptions. The next natural step is to either omit the thermal device or to operate the thermal device with a less margin between its point of operation and the actual point of failure of the transformer and so that the point of operation of the thermal device exceeds generally accepted safe temperatures for the transformer. If it is necessary to raise the level of protection to such a point that considerable damage is done to the transformer before the device operates, it is questionable whether the device provides useful protection to the transformer. Proof that the transformer will still operate does not constitute proof that the transformer is in a safe operating condition. Transformers should be so operated that the insulation remains in good condition unless an emergency condition arises where it is necessary to damage the transformer in order to secure continuity of service. In order that transformer insulation remain in first-class condition, the normal loading of transformers should be such that serious damage to the insulation does not occur. Overloads which damage the insulation should be on a strictly emergency basis. For service continuity it seems self-evident that if all transformers on the system have their insulation maintained in good condition the service continuity will be better than if some of the transformers on the system are so operated as to have damaged insulation even if they do continue to operate. A device which offers protection only after considerable damage has been done may be desirable but it should be recognized that such a device offers very little protection against damage to the transformer and may leave the transformer in an unsafe operating condition.

R. B. George (Tennessee Valley Authority, Norris, Tenn.): During some tests which I made to determine the short-time temperature limits of transformer insulation, I recognized that there was a good margin which could be used for short time overloads if the proper precautions were taken to protect the oil from oxidation and provisions were made to remove the load from the transformer when the magnitude or duration of overload reached limits which would injure the transformer.

The paper by Mr. Putman and Mr. Dann describes tests that were made to determine these limits. A device has been proposed to disconnect the transformer from the circuit when these limits are reached.

It is suggested that the proper committees of the AIEE review the subject of maximum temperature limits of insulating materials. It is always desirable to bring the AIEE rules up to date to include new developments in the art.

The results reported in this paper have an application for electric heating loads, particularly in the southern states. There are parts of this region where electric heating would be required for a short portion of the year.

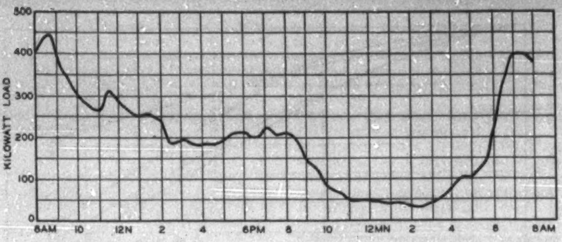


Figure 1

This load may be more economically served by using transformers with nominal kilovolt-ampere rating much lower than the peak load during the heating demand, and operating by copper temperature because electric heating is required only when the ambient temperature is low. The peak demand for electric heating usually occurs early in the morning and tapers off before the electric range load for noon cooking builds up its small peak.

Figure 1 of this discussion is a 24-hour curve of the total load on a circuit which serves 40 houses which are equipped with electric refrigerators, ranges, water heaters, and the usual appliances in addition to being electrically heated. The 17-hour average temperature from 6 a.m. to 10 p.m. was 19.9 degrees Fahrenheit. The 24-hour average temperature was 18 degrees Fahrenheit. This curve has the load characteristics which were described above. Since this curve also gives information concerning the duty on transformers for this type of load, it may be useful to a committee for reviewing the subject of maximum temperature limits of transformer insulation. Loads of this type can certainly be served more economically by taking advantage of operating by copper temperature during the peak loads which occur only at low ambient temperature.

A. C. Monteith (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The two outstanding factors brought out in this paper are the greater overload capacity of a transformer when the oil is kept free from contact with air and the fact that a reliable device is available to allow taking full advantage of this overload capacity without fear of burnout. Where regulation is not the determining factor in the transformer application, this overload capacity should allow fitting the transformer more closely to the load, thus allowing a reduction in initial investment. Since the "*CSP*" power transformer is equipped with step-type regulators, heating becomes the main factor in applying the transformer.

A secondary network system is designed so that the transformers will be called on to carry full load with one feeder out of service and carry some overload with two feeders out of service. Regulation is usually not a problem in this type of system, so that taking full advantage of a transformer having a tight tank and increased overload capacity should allow reducing the ratio of installed transformer capacity to full load. This is particularly true where networks are installed for supplying the lighter-load areas, as a greater chance can be taken as compared to the application for the heavy-load areas. Quite often the overload capacity given in the paper would allow carrying certain types of loads through the peak, which would mean applying the transformers based on their overload capacity rather than the continuous rating. Such factors should allow extending the economical use of the secondary network system.

The use of the thermal trip device applied to network transformers also presents a method of protecting against burnout in case of prolonged overload. Considerable time has been spent in trying to design a fuse that will leave the transformer on the system up to the danger point. The thermal device described in the paper will give this protection, the closing of its contacts tripping the network protector. If this type of protection is adopted, consideration could be given to eliminating the fuses from the network protectors.

These are a few of the changes in application that have occurred to the writer, if advantage is taken of the data presented in the paper. The new thought presented should go far to revolutionize our approach to the distribution problem.

T. H. Mawson (The Commonwealth and Southern Corporation, Birmingham, Ala.): The paper by Messrs. Putman and Dann is undoubtedly a valuable contribution to transformer operation. Previous studies by Nichols, Montsinger, Dann, and others have substantiated the operator's belief that transformers could be operated successfully without major reduction of life at ratings in excess of that shown on the name plate. In other words, the nameplate rating was only one point on the operating curve of the particular transformer.

It is, indeed, gratifying to know that the American Standards Association has prepared a "Guide for Loading Oil Immersed Distribution and Power Transformers," following the work of these men.

Since there are no satisfactory figures available on the life of transformer insulation due to the mass of test data, as yet unavailable, required for any sort of comprehensive averages, it is difficult to determine an actual life span for any transformer. The various methods mentioned for increasing the life of the transformer insulation such as inert gas in contact with the oil, reduced area of air contacting the oil, etc., play a part, but the oil temperatures, if allowed to remain at high levels for prolonged intervals must ultimately impair the condition of the transformer.

Loading to the limits set up in the ASA standards appears conservative in view of the test data in the present paper. While the test conditions for the five-kva transformers were unusually severe, how can the conclusions drawn from the tests be interpreted in terms of actual service conditions?

If transformers are to be installed on a basis of continuous and satisfactory service, the maximum use can be obtained from a given bank when the load curve is such as to permit overloading during peak hours to the point where the maximum temperature will not cause excessive deterioration. At the same time the possibility of excessive regulation in the transformer must be considered. Since this peak load would be considered as recurrent and not an infrequent emergency, the regulation in the transformer that would occur at these peaks would tend to be excessive. If this type of loading is to be considered for future installations then the design of the system components will have to be adjusted to operate within satisfactory voltage levels.

The use of some device to determine with reasonable accuracy the "hot-spot" temperature would give data that could be used in conjunction with typical load curves for various areas. This would provide a better method of determining the best size of transformer at that location, as well as the behavior of a particular transformer.

There is again the question of transformers now in service. Lack of knowledge as to the past history of these transformers would tend to raise questions as to the remaining life. Yet every effort should be made to work these transformers to their service limit—be it kilovolt-amperes or voltage drop.

The use of a lockout device for protection would be satisfactory, provided there was some control of the load, or that there was no great time lag between operation and reclosure. Therefore, operation of transformers at levels close to the limit would tend to increase the number of interruptions from this cause.

It would seem advisable to select transformers for both power and distribution service on a basis of the guide as set up by ASA, adjusting this selection where load data warrants, rather than depend entirely upon inspection of signal-light operation.

W. R. Brownlee (The Tennessee Electric Power Company, Chattanooga): Methods of securing maximum use of capital equipment as a means of reducing the over-all cost of electric power are of most vital interest to operating companies, to manufacturers, and to the entire industry. Contributions to such methods applied to distribution transformers are of particular value at this time.

For transformers of small size and value the bimetallic relay should find a most wide application, since it is a reasonably good device and can be secured at relatively low cost. They are much superior to fuses both in available characteristics and in reasonable adherence to predetermined curves. I wonder if the authors are advocating the use of such devices for larger and more expensive transformers whereas, many operating companies have for years made use of more reliable (and more expensive) devices such as thermocouples or calibrated resistance units for operating power transformers by copper temperature. Sometimes these devices are permitted to trip breakers but particularly in case of the larger units they provide a graphic record of temperature, permitting the system operator to exercise his judgment in balancing the probable shortening of life of the transformer unit due to the overheating, against the seriousness of the effect on service or on other equipment involved in taking the unit out of service.

Apparently the authors have relied on dielectric tests to determine whether the transformers under test suffered any damage due to overloads. Have they actually taken any of the tested units apart to determine the exact condition of the inside insulation? If such critical examination should confirm their preliminary findings, then it might be in order for the proper AIEE committees to review the subject of maximum safe temperature limits.

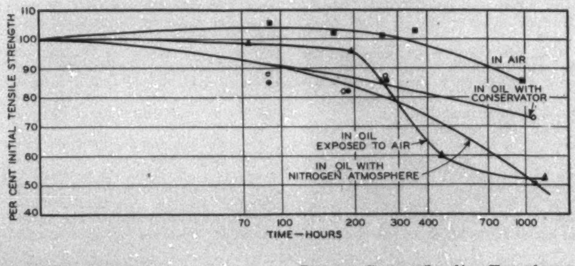
Probably the authors intended to include power transformers with conservators along with transformers equipped with "inertaire" equipment in contrasting the effects of over-temperature of materials tested in open oil and deterioration in oil protected from open air. Naturally gas-tight cases are preferred for small distribution transformers or for any underground installation.

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): The authors have added a real contribution to the art of loading transformers by temperature under various service conditions.

There are, however, some statements made in the paper to which I must take exception. The paper states that the signal light is set to operate at about 95 degrees centigrade (permitting continuous operation at 95 degrees average copper temperature) which limit they say is recognized in the AIEE Standards. This is correct, but it has generally been agreed by the industry for the past three or four years that for continuous operation the average temperature should not exceed 85 degrees or 55 degrees rise in a 30-degree ambient. They may intend that the 95-degree average temperature limit be used only for short-time operation. If so, what is there to prevent the transformers operating continuously at average copper temperatures close to 95 degrees centigrade? This point is not clear.

Figure 2. Aging of 0.031-inch pressboard at 105 degrees centigrade

Tests made in 1935



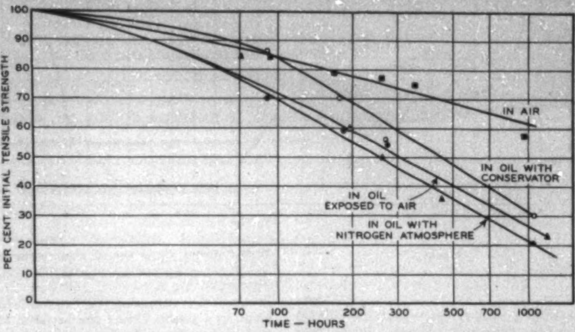


Figure 3. Aging of class A insulations at 105 degrees centigrade

Materials used: (1) 0.031-inch pressboard; (2) 0.010-inch kraft paper

Tests made in 1935

As a matter of fact, AIEE Standards No. 13 will be superseded within a few months by the Proposed American Standards and Recommended Practices for Transformers, which recommend that for continuous operation the average copper temperature not exceed 85 degrees centigrade.

If the conditions are such as to permit continuous operation at 95 degrees average temperature by the signal light, I would like to suggest that the authors either recommend the temperature limit of 85 degrees centigrade as given in the new guides for operation of transformers, which are a part of the proposed American standards mentioned above, or give a valid reason for (or data to support) setting the temperature limit ten degrees higher than recommended for continuous operation in the proposed guides.

The authors make the statement that when the oil is protected against the harmful effects of oxygen by "inertaire," there is practically no deterioration of the insulating materials at the limits of temperature suggested by the Institute and the American Standards Association. I think the authors should state whether they mean the Institute or ASA since, as stated above, the temperature limit recommended by ASA is ten degrees lower than that recommended by the Institute.

The authors state that all aging tests of class *A* insulations in the past have been made in oil exposed to air and by inference say that most, if not practically all, of the aging of insulation in oil at 95 degrees average temperature (with the hottest spot ranging from say 98 to 105 degrees depending on the design) is due to the presence of oxygen in the oil. This does not agree with what I have found.

In 1935 our laboratory made two separate series of aging tests to determine the difference in the aging (tensile strength) of various class *A* materials immersed (1) in air, (2) in oil exposed to the air, (3) in oil with a nitrogen atmosphere, and (4) the oil protected by a conservator.

Both untreated and treated materials were tested. The treated samples were given a 30-minute varnish dip, centrifuged to remove excess varnish, and baked to set up the varnish. The materials were cut into pieces approximately ten inches square and then assembled in packs, part of the materials being spaced with pressboard spacing strips to allow circulation of the oil or air, and the remaining materials assembled in a tightly-packed mass.

The test conditions were made to represent as nearly as possible the conditions of transformers in service.

The materials which were aged in oil were immersed in oil in steel containers and placed in an oven maintained at a constant temperature of 105 degrees centigrade plus or minus one degree.

In the case of aging in oil with conservator, the connecting pipe flush with the cover was connected to the conservator on the outside of the oven.

The tank with the nitrogen atmosphere was connected to a bomb of oxygen-free nitrogen through a pipe leading to the bottom of the tank. Another pipe flush with the cover was connected to a pressure release valve set to a pressure of one-half pound. After the insulation and oil were placed in the tank, the oil being at a level of about two inches below the cover, nitrogen was flushed through the oil for a period of one hour, then the nitrogen was shut off, and the aging started. Each time the tank was opened to remove samples, the air was thoroughly flushed from the oil with nitrogen before resuming the test.

The container in which the material was aged under oil with air over the oil was an open tank.

The aging in air was accomplished by placing bundles of the insulation in an oven maintained at a constant temperature of 105 degrees plus or minus 1 degree centigrade, taking care to keep the material from contact with metal in the oven.

All samples were conditioned in a standard atmosphere four hours before testing. A set of standard samples was immersed in the aging medium one hour at room temperature, conditioned four hours under standard humidity conditions, and tested to obtain standard data for the aging comparison.

The results of the aging tests are shown in figures 2 and 3 of this discussion which indicate:

1. That insulation aged in air has a longer life than insulation aged in oil.

2. That insulation aged in oil plus air, or in oil plus conservator, or in oil plus nitrogen atmosphere, shows approximately the same amount of mechanical deterioration.

There was practically no difference between the rate of aging of the materials spaced or unspaced in oil. In air, the spaced materials showed approximately ten per cent less aging.

The authors state that to determine whether the heavy overloads had injured the transformer, AIEE dielectric tests were applied. I do not feel that these tests mean anything, since insulation can be greatly weakened mechanically before its dielectric strength is affected. In fact, the dielectric strength of oil-immersed insulation does not decrease until it is well carbonized and cracked. I note that a few short-circuit tests were made, which were probably a better test on the insulations than the dielectric tests.

I would like to suggest that a better method of determining the effect of short-time overloads on the insulation would be to integrate the heating curve area in some such manner as I used in my AIEE paper entitled "Temperature Limits for Short-Time Overloads for Oil-Immersed Neutral-Grounding Reactors and Transformers" published in AIEE TRANSACTIONS, volume 57, 1938, pages 39–44 (January section). This method of analysis ought to give some idea of whether 500 or 600 load cycles has appreciably weakened mechanically the insulation.

H. V. Putman and W. M. Dann: The discussions on the paper "Loading Transformers by Copper Temperature" have brought out a lively interest in the subject. The purpose of the paper was to give emphasis to the recognized fact that transformers inherently have a substantial overload capacity for short-time periods and to describe a practical way of taking advantage of this capacity automatically in service. The prevailing views of those who discussed the paper seem to be that the utilization of this short-time overload capacity is of vital importance to the operating companies and to the industry in general and that in the main the method of obtaining its full utilization is effective and practical.

Messrs. Christensen, Hamilton, George, and Brownlee suggest either that the AIEE temperature limit of 105 degrees centigrade may be too low or that perhaps the Institute should review the subject of limiting temperatures. As pointed out in the paper, we believe these suggestions should be carried out, particularly in connection with limiting temperatures for short time periods.

Mr. Mawson speaks of the "Guide for Operation of Transformers," which is about to be published by the ASA, and comments that it would seem advisable to select transformers on the basis of this guide rather than to rely entirely upon signal-light operation. However, the overloads suggested by the ASA guide are quite conservative; they had to be because they apply to transformers of more than one type and to units that have been in service for the past ten years. Modern transformers operated by copper temperature with the relay and signal device will, of course, carry the short-time overloads of the ASA guide, but they will go further; the signal light will give a warning when a definitely established temperature is reached and will cut the transformer out if the operation is continued until a temperature is reached at which appreciable shortening of life would result. Furthermore, ambient temperature is automatically taken into account and it is unnecessary to consult tables of permissible overloads.

Mr. Montsinger points out that the signal light is set to operate at about 95 degree centigrade and he asks whether there is anything to prevent operating a transformer continuously at an average copper temperature close to 95 degrees centigrade. It is a fact that the transformer, just like an ordinary unit, could be artificially loaded so that its temperature rise plus the ambient temperature would be continuously just inside the limit of signal operation and its average winding temperature would be continuously close to 95 degrees centigrade. But it is hardly conceivable that a transformer would be called upon to carry its full load continuously in actual service with an ambient temperature which is continuously 40 degrees centigrade. Load conditions and ambient temperatures in actual service are largely uncontrollable; they normally vary throughout the day and the season, and in cases where a transformer is so small with regard to its load conditions that its average copper temperature occasionally reaches 95 degrees centigrade or thereabouts, it seems obvious that it would be better to know of the conditions through signal-light operation than to be unaware of them. A very natural question arising from Mr. Montsinger's inquiry is—what is there to prevent *any* distribution transformer from operating continuously at 95 degrees centigrade?

Mr. Mawson asks how the conclusions drawn from the tests on the five-kva transformers may be interpreted in terms of actual service conditions. It is not possible in a laboratory to simulate a large number of the overload conditions that can exist in actual service, but the tests do represent very severe cases of certain types of overload, and operation at other overloads in service may be sized up by analysis and comparison.

Mr. Mawson speaks of the life span of transformer insulation, having in mind the effect of temperature, while Mr. Starr reminds us that repeated overloads reduce transformer life and that there is a threshold of loading beyond which overloads are uneconomical. Mr. Sealey introduces a striking comparison to automobile tires and says that the oil and insulation in the transformers under test are not in first-class condition because of the high temperatures to which they have been subjected. It is of course too early to say what the condition of the insulation actually is, but there is nothing in the tests themselves to indicate that the oil and insulation have been seriously damaged. At the time the paper was written, after 660 cycles of severe overloading, the transformers had more than once withstood the standard AIEE low-frequency and impulse tests and the oil, while darkened in color, had withstood even higher breakdown tests than before the cycle tests were started. These dielectric tests and the fact that the transformers are still going through similar cycles of tests indicate that they are still good for actual service.

Mr. Montsinger questions whether the successful application of Institute tests means anything, and he points out that insulation can be greatly weakened mechanically before its dielectric strength is affected. That was the very reason for subjecting the transformers to a series of short-circuit tests with an ignitron timer. The violent vibrations of those tests failed to develop any mechanical weakness of the insulation, for the transformers immediately afterward withstood impulse tests and tests at double-voltage excitation. When the cycle tests are finally discontinued the units will be dismantled and the insulation minutely examined. The results of this examination will no doubt form a contribution to the study of the life of transformer insulation.

Mr. Brownlee asks whether the relay and signal devices are advocated for large transformers, and Messrs. Christensen and Hamilton raise the question of adjustment, maintenance, and replacement of the devices in large power transformers. These devices are right now being used in service with complete satisfaction on transformers as large as 1,500 kva. They can be used effectively for power transformers of any size. A convenient adjustment of the relay is provided for, but field experience with more than 200,000 "transformer-years" in service indicates that adjustment has not been necessary and that maintenance attention will be rarely needed. If a relay were to fail it would have to be replaced.

Mr. Monteith comments on the considerable time that has been spent in trying to design a fuse that will leave the transformer on the system up to the danger point, and he points out the value of the relay for this kind of protection, while Mr. Brownlee remarks that the relay is much superior to the fuse.

Mr. George speaks of the early morning peaks caused by electric heating loads. This is an excellent illustration of a peak that can be taken care of with transformers having ratings much lower than the peak

Effects of Temperature on Mechanical Performance of Rotating Electrical Machinery

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THE EFFECTS of temperature on electrical machinery are usually considered in connection with the deterioration of insulation used on the active conductors themselves. There are, however, many other parts of these machines where temperature effects are important and vitally affect the design of the machine. Unless provision is made in the design of machines to take care of these effects, unsatisfactory operation or reduced life may result. These effects on machine construction may be considered in three groups.

1. Effects of Temperature on Insulating Parts

Machines with class *A* insulation on the windings have class *A* materials on other parts of the machine as well as on the windings proper. On d-c machines, fullerboard insulation is used around the main and commutating poles to give ground insulation and creepage to ground. On a-c salient-pole machines, fullerboard or fish paper is commonly used for a similar purpose on the rotor poles. Where higher temperature rises than those permitted for class *A* materials are encountered, these materials must be abandoned in favor of class *B* materials such as asbestos, mica, or woven-glass formed shields. To eliminate formed channels and insulating pieces around the stationary pole pieces, railway motors and other propulsion equipment requiring maximum output with minimum space and weight have the necessary insulation protection to ground incorporated in the coil insulation itself. Mummified coils using class *B* conductor insulation are provided with sufficient mica or asbestos insulation on the outside of the coil itself to give normal protection to ground.

If temperature limits are pushed very high, such machines as enclosed self-ventilated motors would get extremely high temperatures on all internal parts and fibrous materials, such as used for insulation between brush holder brackets and rocker rings, would no longer be satisfactory, and special porcelains or molded mica materials as used on railway motors for brush-holder-stud insulation would have to be used.

On both a-c and d-c machines, fish-paper cells of class *A* material are used in the slots, not from an insulation standpoint but to provide, during winding, protection to the coil sides against the edges of the laminations in the slots. With higher temperatures such cells are abandoned or mica cells can be substituted.

Wedges for class *A* machines are universally made of fiber. Excessive temperatures cause such materials to soften, shrink, and sometimes split or crack. When wedges become loose or crack, they generally work endwise out of the slots, although sometimes they push radially out of the slots, especially near the ends of the core. Substitutes of Micarta or other phenolic base materials may be used. High temperatures are also detrimental to the fiber winding strips that are used under the wedges to provide a sliding base for the wedge when being driven in the slot grooves and to provide a means of securing a tight coil in the slot. Mica-base material must then be substituted.

All d-c machines, wound-rotor induction motors, and rotating-armature a-c machines use bands of wire to hold the end windings of the rotors in position. On almost all except the smallest machines, coil supports are used under the end windings and the coils are held down against these coil supports on the end portion. Channel or other types of insulating material are used on the coil supports to provide insulation and creepage to ground. Layers of insulating material and fullerboard are placed around the armature coils on the ends to provide insulation and protection to the coils from the steel band wire which holds the end windings in place. High operating temperatures cause this insulating material, as well as the insulation on the coil supports, to shrink and thus the coils and bands will become loose on the end windings, unless special materials and precau-

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Paper number 39-17, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 22, 1938; made available for preprinting December 8, 1938.

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load, when loaded by copper temperature with the relay and the signal device.

The results of Mr. Montsinger's tests extended over a period of only about 40 days, which is hardly sufficient to give reliable conclusions. To state it simply, Mr. Montsinger's conclusions are that cellulose insulating materials immersed in new transformer oil at 105 degrees and fully protected against oxidation and moisture deteriorate just as rapidly as when the oil is subjected to oxidation and moisture by exposure to the atmosphere. This conclusion does not agree with the findings of our own research engineers and the results obtained by others, notably Stager (*Elektrotechnische Isoliermaterialien*, Stuttgart, 1931).

In a paper "Temperature Limits Set by Oil and Cellulose Insulation" by Doctor C. F. Hill (AIEE TRANSACTIONS, volume 58, 1939, pages 484–91) it is shown by carefully conducted tests extending over a period of approximately 650 days, that the deterioration of cellulose materials eventually flattens out and ceases, but that it takes more than 40 days to reach this condition. His tests show that electrically such materials maintain their initial characteristics even when above 100 degrees centigrade and they may even show improvement; this is true up to 140 degrees centigrade when the materials and oil are protected with an inert gas. Mechanically, untreated cellulose in air deteriorates rapidly above 100 degrees centigrade, or at 95 degrees centigrade when the oil is exposed to air. However, his opinion is that many transformers are operating satisfactorily in service in which the insulating materials have deteriorated to one-fourth of their original mechanical strength.

The conclusions drawn from Doctor Hill's tests are that the deterioration of insulating materials is greatest when the oil is exposed to air, and least when it is protected by an inert gas. Mr. Montsinger's tests show that the deterioration of pressboard mechanically at the end of about 40 days at 105 degrees centigrade is about the same as in oil exposed to air and in oil protected by nitrogen. This is not thought to be the case and it is not substantiated by Doctor Hill's tests. Mr. Montsinger's tests also show considerably less deterioration in oil with a conservator than in oil protected with nitrogen. It is probable that in the conservator tests there was no breathing of air and the results obtained were really due to a tightly sealed tank, which is the case with the transformers discussed in the paper on "Loading Transformers by Copper Temperature."

tions are taken to eliminate this shrinkage of the insulating materials.

Successful operation of commutators depends more on their ability to go through a temperature cycle than upon any one other feature. By this is meant their ability to keep a smooth surface, and not have bar to bar roughness with an increase in temperature. Eccentricity of commutators up to 0.001 of an inch even on high-speed machines will not affect the operation, but bar-to-bar roughness or unevenness of greater than 0.0001 inch will usually cause sparking and commutation trouble. Obviously for a given commutator, the less the temperature range the less chance there will be for bar-to-bar roughness. This does not mean that commutators should not operate at fairly high temperatures, nor does it mean that commutators should be specified to operate at excessively low temperatures.

The temperature resulting on a commutator is dependent upon the losses occurring at the commutator, the brush contact I^2R loss, and the brush friction. The I^2R loss of the current flowing in the commutator bars to the brushes is insignificant and never measured nor considered due to the large cross-section area of the commutator bars themselves and because the current flows in the bars only during the short time they pass under the brushes, during the commutation period. The loss at the brush contact due to the voltage drop of the current in passing between the commutator and brushes does vary somewhat with the material of the brush and is dependent upon the surface condition of the commutator. However, for all practical purposes, the voltage drop can be considered constant at one volt per contact. This loss, therefore, varies directly with the load current. The brush friction loss depends upon the number and size of brushes, the material of the brush and the condition of the contact surfaces, the brush pressure and the peripheral speed. The brush area for a given current rating is limited and cannot be reduced below an accepted value so that the total brush loss for a given set of conditions cannot be reduced except by decreasing the commutator diameter. Roughly, as the diameter is reduced the brush width must also be reduced, so that to maintain the required brush area more brushes must be added. This requires a longer commutator with resulting larger spans between supporting rings or greater overhang, both of which result in greater stresses and deflections with an increase in bar-to-bar roughness for a given temperature rise. Obviously then, it is not desirable to specify too low an operating temperature for the commutator as this will require a greater heat dissipating area, with resulting greater stresses, deflections, and roughness, with accompanying poorer commutation.

What then limits the temperature of a commutator in an upward direction? Not limitations of temperature of the mica between commutator bars and between the bars and the ground parts, that is, the vee-ring or shrink-ring mica, as this mica will withstand temperatures considerably higher than permissible or acceptable for successful commutator operation. Extremely high-temperature operation of the commutator could result in damage to the insulation on the armature coils due to heat flow from the commutator up through the necks and into the armature coil. However, the thing that limits commutator temperature is the ability of the commutator to go through a temperature cycle without bar-to-bar roughness and this depends primarily on the characteristics of the insulating mica used in the commutator and the processes used in building the commutator.

At the present time, full-load continuous-operation acceptable temperatures for commutators of class *A* insulated machines are 105 degrees centigrade and of class *B* insulated machines 125 degrees centigrade, with temperatures measured by thermometers, and with no allowance for hot spots, as obviously the maximum temperatures can be measured directly. Based upon present experience and designs, higher temperatures than 125 degrees centigrade for continuous operation are permissible, probably in the neighborhood of 150 degrees centigrade, still permitting successful operating commutators.

2. Influence of Heat on Materials Other Than Insulation

There is another limitation on the operating temperature of commutators due to the nature of the material used. Commutator copper itself must not be heated to too high a temperature or it will become annealed and thus lose its mechanical strength. The annealing temperature is not definite for copper but is influenced by time. For instance, commutators can operate indefinitely at a temperature of 150 degrees centigrade without any chance of annealing the copper. If the temperature is raised to 175 degrees centigrade, copper will not anneal immediately as it takes hours and days to anneal the copper thoroughly. If the temperature is increased appreciably above 200 degrees centigrade, the copper can be damaged in a short space of time.

Another limit in commutator temperature operation is the solder used in attaching the armature coils to the commutator necks. Ordinary solder melts at 180 degrees centigrade. Hard or tin solder melts at 220 degrees centigrade. These solders if used in commutator bar construction can be used without permanent damage to the commutator copper when soldering. However, if the commutator is subjected, for short durations of time, to very high temperatures, the solder may melt and be thrown out due to centrifugal force. This then forms high resistance joints or open circuits in the armature winding and sparking results.

Commutators for high-temperature-operating machines, such as railway motors, are usually made of silver-bearing lake copper as this material has better temperature creep characteristics and will better stand the stresses due to higher-temperature operation. The use of higher-temperature solders, melting at temperatures between 250 and 300 degrees centigrade, will eliminate the problems of the joints between the commutator necks and armature coils opening up due to high temperatures of operation and stresses of rotation. Its use, however, introduces problems of soldering without annealing the copper bars during the soldering operation, especially on those commutators having solid necks. Brazed or phosphorous solder joints which will stand relatively high temperatures could be used, but such joints do not permit satisfactory opening for removal of armature coils in case of repairs or replacements and there is the possibility of damage to the coil insulation in its use.

Brushes of carbon, carbon graphite, and copper graphite types are baked at temperatures far above those experienced in service. However, these brushes have shunts of stranded flexible cable attached to them by riveting or soldering and operation at too high a temperature will result in selective action and unequal current distribution, with resulting overloading of some brushes. This will cause overheating of the brush shunts, giving discoloration and brittleness to these shunts as well as the melting of the solder used in the shunt attachments, causing still further detrimental operation. Too high an increase in temperature operation of current collecting parts always results in poorer operation, more rapid brush wear, and higher maintenance costs.

Higher temperatures will adversely affect the insulating treatments used on the punchings. In some cases varnish treatments will have to be supplanted by other forms of insulation. It can also detrimentally affect varnish treatments applied to various types of insulating pieces.

Oil-lubricated sleeve bearings may be affected by machine temperature and in extreme cases with high operating temperatures, external cooling of the bearings must be used. In the past oil cooling of the bearings by circulating water through pipes embedded in the bearing shell was used. Today the tendency is to use external coolers and circulate the oil through these coolers.

3. Problems of Expansion and Contraction Due to Temperatures

Method of bearing support as well as bearings themselves are affected by the temperature of operation. Antifriction bearings must not bind, neither must they have too loose a fit or they will be noisy and eventually give trouble in operation due to this looseness. However, if the machine operates at relatively high temperatures, the antifriction bearings must be of the loose-fit type having greater than normal clearance between the balls and races when at room temperature so that while there may be some extra looseness when the bearing is cold, normal tolerances will be secured at operating temperatures. Antifriction-bearing machines must have at least one bearing free to move endwise with temperature expansion. This means that where provision must be made for end thrust, end thrust in both directions must be taken by the bearings on one end of the machine only.

Most difficulties on induction motors due to temperatures are in connection with squirrel-cage windings. On those motors having wound rotors the problems are similar to those of d-c machines, commutators of course excepted.

On the former types of windings, the bars in the rotor slots are usually of copper and project beyond the ends of the punchings, where they are attached to the end rings. The end rings are made of copper, brass, or bronze. In the usual construction, the bars are brazed to the end rings. The difficulties encountered are due to the temperature expansion of the end rings. As they get hot, they expand in diameter and in so doing, bend the ends of the bars outward. On cooling, the reverse bending occurs. Repeated cycles of heating and cooling, due to repeated starting and stopping and changes of load, can cause these bars to break. The breaks occur in the bars between the laminations and the end rings. A break of a brazed joint is a rarity.

It can be shown that the loss produced in the squirrel-cage winding during starting is equal to the kinetic energy stored in the rotor and its connected load at rated speed. Since the starting periods are of short duration, 10 to 15 seconds on large machines, most of this loss is absorbed in the squirrel-cage winding, since this short time does not permit dissipation of the loss in the surrounding air. On slow-speed motors the WR^2 of the rotor itself is quite high and the WR^2 of the load may be several times that of the rotor, in many applications such as induced draft fans, grinders, choppers, and saws. In all these instances the large amount of loss raises the squirrel-cage-winding temperature to a high value with corresponding untoward results. By using brass or bronze end rings of higher resistance, larger cross-section rings can be used with no change in total resistance but with increased heat absorbing capacity. This results in less temperature rise, less expansion, and less breakage of bars.

On the smaller sizes of induction motors, where very small air gaps are used, very high temperatures will result in the rotor scraping on the stator at times. The rotor will run hotter than the stator so that the expansion will be greater, especially on high-slip motors which have many applications to take advantage of the flywheel effect that the high slip permits. Since production dictates noncircular outside-punching peripheries to get the maximum number of punchings from a given sheet of steel, resulting in only a portion of the punching periphery being held in the stator frame, the expansion of the stator punchings will not be symmetrical. This results in unequal expansion of the inside diameter of the stator punchings so that with the expansion of the rotor, the air gap will be so small at some point around the periphery of the motor that the rotor will rub on the stator. Even where the expansion does not actually cause the rotor to rub the stator, the air gap at some one point will be so small that the unbalanced magnetic pull will deflect the shaft enough to cause actual rubbing of the rotor on the stator punchings. Higher temperature rises will also require larger press fits of the punchings on the shaft so that the increased unequal heating of the punchings and the shaft will not cause the punchings to become loose on the shaft.

If higher-temperature-rise motors were used, especially in the larger sizes, the motor dimensions would be decreased, resulting in still higher temperature rises of the squirrel-cage winding with again unfavorable characteristics.

Collectors as used on the a-c end of synchronous converters, wound-rotor induction motors, and rotating-armature a-c machines are also subjected to temperature limits. These limits, since there are no bar-to-bar roughness conditions to be met, can be somewhat higher than for commutators. A safe ultimate temperature of 150 degrees centigrade could be used for collector-ring operation. The connections to collector rings are usually made by means of rods threaded into the collector material below the body of the ring surface. These threaded rings are usually sweated into position and too high a temperature operation of the collector rings, even for short periods of time, will result in the melting of this solder. High temperatures on collector rings have a tendency for the rings to become out of round, particularly on the large sizes, resulting in a roughened surface and accompanying poor operation and faster brush wear. On the larger size collector rings used on synchronous converters the spokes or arms can be made S shaped instead of radial so that the stresses due to expansion will not tend to distort the ring from a true circle. The ring body can also be made of relatively heavy cross section, even T shaped, in order to hold the ring surface as concentric as possible with an increase in temperature.

On relatively long-core d-c machines the ends of the field coils are not supported other than by the material itself and being free to move due to expansion, no chafing of the insulation results.

In the largest sizes of turbogenerators, with lengths of 20 to 25 feet between bearings, temperature rises up to 85 degrees will give elongations of approximately $^1/_8$ inch to $^5/_{16}$ inch in the various parts. The steel parts of these long-core machines operate at lower temperatures than the copper conductors, and since copper has a greater temperature coefficient of expansion than steel, there can be a total difference of endwise expansion of approximately $^3/_{16}$ inch of the copper conductors over that of the steel rotor, in which the copper conductors are embedded. Overload requires increased excitation and gives a greater increase in copper losses than increase in the iron loss, thus further accentuating this difference of expansion.

On these high-speed turbogenerators, the centrifugal force, due to the weight of the conductors in the rotors, exerts such enormous forces against the underside of the slot wedges that the conductors are partially restrained from total movement in respect to the iron, throughout part of the total length due to those different temperatures and coefficient differences. Thus, most of the relative movement takes place at the ends of the rotors. This has in the past resulted in the insulation and bracing in the end rings of the rotors being chafed and can result in ultimate failure. However, improvements in insulation and the design of the end ring bracing in present designs give a construction that operates satisfactorily. A further increase in temperature rises would again extend the expansion beyond the limits of satisfactory operation.

On the stators, with vent ducts spaced in the iron for adequate ventilation, the insulated conductors will be exposed in the vent ducts. The insulation, being unrestrained by any slot sides in the vent ducts, will bulge with time, into these vent ducts. Since, as in the rotors, the copper conductors expand more than the core iron, a relative movement of the former will cause chafing of the coil insulation at the vent ducts. A large number of machines having a 60-degree-centigrade rise of insulated coils have operated for 15 years without trouble due to this elongation. An attempt, however, to increase this temperature rise from 60 degrees centigrade to 100 degrees centigrade on comparable size machines gave only a few years life due to failure of insulation at the vent ducts due to the endwise expansion differential. Failures of this nature are due to the pulverizing of the mica flakes due to the repeated cycles of chafing caused by heating and cooling with load changes.

Thus, it can be seen that excluding temperature limitations of the insulation on windings there are many features of machines, both electrical and mechanical, that place temperature limits on the operation of rotating electrical machinery. There are still other problems in connection with temperatures that vitally affect the design and operation of the machines.

From the performance standpoint, an increase in temperature rises, permitting more output from a given size and weight of material, reduces the margin, particularly of overload characteristics, even on short-time overload ratings. For instance, applying a higher-temperature-class insulation to a given design d-c machine with a redesign in the loading ratios of flux capacity to current capacity, in order to obtain a balanced design for the higher permissible temperature rises, will not permit increased overload commutating capacity in proportion to the increased capacity secured at the expense of increased temperature rise. Except for working the iron harder—that is, increasing the flux densities, giving a relatively lower armature-current loading—the overload commutating capacity is unchanged. In fact, the higher temperature operation of the commutator at the increased full-load rating, resulting in a somewhat higher bar-to-bar roughness, decreases slightly the commutating ability at any given short-time overload rating. Commutating overload ability is usually limited by the flux-carrying capacity of the commutating pole, which obviously is not increased by higher permissible temperature limits. Thus when higher ratings at higher temperatures are specified and secured, overload ratings must be decreased. However, on those applications where heavy overloads are not required or are of infrequent occurrence, and not of very great magnitude, higher-temperature machines of smaller size and weights can be produced.

Inherent voltage regulations, especially of d-c machines, drop off quite rapidly above normal full-load ratings. Increasing the ratings with accompanying greater temperature rises is secured at the expense of poorer voltage-regulation performance.

Many times extraordinarily good performance in the line of efficiencies is required from machines by the purchasers, while at the same time high temperature rises are permitted. Obviously if materials are worked very hard to get minimum material for maximum output, with resulting high temperature rise, efficiency must be sacrificed. Extremely high efficiencies can only be secured by not working all materials up to the maximum and some class *B* rated machines may have temperature rises falling in class *A* ratings simply because sufficient material had to be used to secure high efficiencies, resulting in minimum losses and relatively low temperature rises.

Machines may sometimes be purchased by customers specifying class *B* insulation, where the actual requirements could be met with class *A* insulation features. This is done intentionally so that for emergency operation the equipment can operate at higher temperatures without failure. In this connection, distinction should be made between the temperatures encountered in frequent short periods of cycles of operation and those encountered in continuous operation. Obviously high temperatures are considerably more detrimental for continuous operation.

Railway motors and other propulsion equipment require maximum output in minimum space and with minimum weight. This equipment, therefore, logically belongs in high-temperature classification and some sacrifice of operating life, increased maintenance, and limitations on some performance characteristics are permissible to get maximum output rating with minimum space and weight.

It is not the purpose of this paper to discourage the use of higher temperatures in rotating electrical machinery as in many cases such a step is very desirable, but it is the purpose to indicate the many factors that are vitally affected by such increase in temperatures. The use of higher temperatures will bring new problems along many lines, especially in large size machines. Caution in procedure, based upon experience gained, is advisable. Evolution rather than revolution of existing standards upward should be the trend.

Discussion

Felix Konn (General Electric Company, Erie, Pa.): I would like to emphasize Mr. Lynn's very appropriate statements about the higher operating temperature of traction motors and about the temperature of commutators.

If we assume that, by increasing the current and the speed, we obtain more output from a given piece of commutating machinery (by accepting higher temperature rises) this will result not only in higher commutator temperatures because of the increased losses but also in an increase in the commutating duty.

If we think of the commutating duty in terms of the reactance voltage, determined as the product of:

commutated current × rpm × K

(where K is determined by the design of the machine) we see that whether we increase the current or the speed or both, we increase the commutating duty. This will result in increased commutation loss, causing an additional temperature rise of the commutator and of the armature winding, but, more important still, if the commutating capacity of the machine is inadequate for the increased load, this higher commutating duty will endanger the performance of the machine by causing excessive sparking at the brushes resulting in burning of commutator segments and rapid brush wear.

It is, therefore, very important that, in the design and manufacture of light-weight commutating machinery operating at high temperature rises, considerable attention be given toward providing the most favorable commutating conditions (electrical and mechanical) in order to maintain the proper balance between commutating and heating capacity. This consideration has governed the design of d-c and a-c traction motors and, far from hindering the progress toward higher outputs per pound, the fact that each machine has, so to speak, two ratings which should be matched one to the other has resulted in continued advances in both directions of heating and commutating capacity.

E. F. Dissmeyer (The Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Lynn's discussion of "Effects of Temperature on Mechanical Performance of Rotating Electrical Machinery" presents a number of important items which should be given serious consideration when contemplating revision of our present standards. Mr. Lynn's paper covers in detail certain of the comments which the writer outlined in his discussion of Mr. Hellmund's paper "Rating of Electric Machinery and Apparatus" (AIEE TRANSACTIONS, volume 58, 1939, pages 499–503).

A number of failures of synchronous-condenser and frequency-changer rotors have occurred due to mechanical damage resulting from thermal effects. Expansion or movement of conductors has resulted in turn-to-turn and other types of rotor-insulation failures. Inspection of rotors has also disclosed many other troubles resulting from thermal effects, such as distortion or creep of amortisseur windings. The design of such rotors should not present any particularly difficult mechanical problems and consequently many of the troubles which we have with large machine rotors can be directly attributed to the fact that we permit rotors to operate at relatively high temperatures. There consequently appears to be considerable justification for reducing the permissible temperature rise of large machine rotors.

F. W. Gay (Public Service Electric and Gas Company, Newark, N. J.): The mechanical limitation imposed on a machine due to the fact that the lineal expansion of steel is only two-thirds of the lineal expansion of copper becomes most acute in very long 3,600-rpm turbine generators.

As an instance of this limitation it was at first expected that a 50,000-kw 3,600-rpm synchronous generator rated on the basis of 87-degrees Fahrenheit cooling water could carry 55,000 kw in the winter with a cooling water temperature of 50 degrees. With this idea in mind, a large refrigerating manufacturer was asked to submit a proposition to refrigerate the summer cooling water of 87 degrees Fahrenheit to 45 degrees Fahrenheit, well below the average winter temperature. The figure submitted to cover the cost of refrigerating machinery to give the expected 5,000 kw of incremental capacity with a power consumption for this machinery of approximately 125 hp was approximately $15,000. This looked like an attractive proposition, but the generator manufacturer quickly explained that even if the cooling water were cooled close to zero degrees centigrade no incremental kilowatts could be obtained. Apparently the limitation on this machine was the unequal mechanical expansion of its component parts rather than any hot-spot temperature.

It would appear that the time has come when copper conductors must be directly refrigerated and maintained at a temperature two-thirds that of the surrounding iron. When this can be achieved the expansion of the copper will equal that of the steel and the mechanical bugbear outlined in Mr. Lynn's paper can no longer frighten us. New refrigerating materials now available may soon make this practicable.

C. Lynn: Mr. Dissmeyer's and particularly Mr. Gay's experiences in higher-temperature operations of rotating electrical machinery stress the difficulties on insulated conductors due to expansions encountered in such operations. Obviously, these difficulties can be minimized and the limits extended somewhat by proper design. Too high-temperature operation will be at the expense of shorter life of the machine.

The point of balanced design between commutation and heating capacity in commutating machinery, as brought out by Mr. Konn, is well taken as the one of these which first reaches its limit determines the capacity of the unit. In this same connection it should be noted that a requirement of too low a temperature operation can lower the output capacity of a machine. For instance, for a given amount of loss on a commutator, the temperature rise can only be lowered by increasing the heat dissipating surface. If this is accomplished by increasing the length of the commutator bars, additional rotational stresses will be set up in them due to the greater spans between vee-ring supports. This gives a poorer mechanical operating commutator and can give inferior commutation even at reduced temperature rises.

The Rating and Application of Motors for Refrigeration and Air Conditioning

PAUL H. RUTHERFORD
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Synopsis: This paper describes the procedure and practical results to date in applying single-phase motors to refrigeration compressors. The applications are primarily torque applications, temperature considerations being of secondary importance.

A line of high-torque motors has been developed to meet the high starting and accelerating torques demanded by the compressors. Utilization of these torques results in running loads considerably higher than rated name-plate loads, without exceeding safe operating temperatures for the insulation life requirement.

To meet these requirements a revised method of rating refrigeration motors on a starting and accelerating torque per horsepower and a starting efficiency basis is described. This method more clearly specifies a motor so that a more satisfactory application would be obtainable both from the standpoint of the user and the public utility company or code authority.

THE continued growth of mechanical refrigeration in the past 15 years has greatly increased the number of single-phase electric motors used. There are more motors in the range of one-third horsepower to three horsepower being manufactured and applied to refrigeration compressors than for any other use. Practically all of these are connected to the lines of the public utility systems, and their installation is subject to the rules of the National Electric Code, Underwriters Laboratories, etc., besides the rules of the public utilities. From the standpoint of the user, the initial and operating costs are important as well as dependability and quietness of operation. Since the horsepower rating and temperature rise as stamped on the name plate have grown to be a poor description of the motor, it is thought advisable to consider better ways of rating motors as applied to refrigeration compressors. Inasmuch as the starting and accelerating torques rather than the temperature rise at name-plate horsepower are the determining factors in the selection of a motor, it may be necessary to include these in the future standards. The purpose of this paper is to show how particular motor characteristics have come to be used and to suggest a possible means of rating this group of motors for the future.

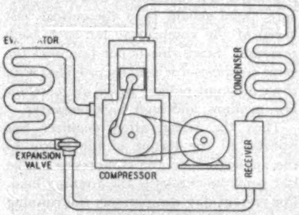

Figure 1. Diagrammatic sketch of refrigeration system

Selection of Motors

All mechanical refrigeration today is performed by vapor-compression machines. Figure 1 shows diagrammatically such a system. The liquid or refrigerant (usually Freon, methyl chloride, or sulfur dioxide) is alternately liquefied and vaporized. Refrigeration is produced by the latent heat of vaporization of the refrigerant. The vapor resulting from this vaporization in the evaporator or cooling element is drawn into the suction or low-pressure side of the compressor. The compressor then converts this low-pressure gas into high-pressure gas and forces it into the condenser. Here it is liquefied through cooling by means of water or air. The liquid refrigerant is then allowed to return to the evaporator through an expansion valve or restricted orifice.

The function of the electric motor in this system is to drive the compressor which compresses the low-pressure gas into high-pressure gas. The pressure in the crank case or at the low-pressure side of the piston is referred to as "back pressure" and the pressure on the high-pressure side or condensing side as the "condensing" or "head pressure." In a given system, the condensing and back pressures and the speed at which the compressor is driven are the determining factors in the amount of starting and accelerating torque required of this motor, as well as the operating load.

The motors used on these systems are the single-phase repulsion-start induction-run type in sizes of one-fourth horsepower, to three horsepower. Capacitor motors are beginning to be used in the one-fourth-horsepower, one-third-horsepower, and one-half-horsepower sizes but seldom little above these ratings because of the high starting currents compared with the other type motor. Practically all the refrigeration systems now using these sizes of motors are expansion-valve systems and the "pull down" is so short that it can be said to be no more severe on the motor from a temperature standpoint than the regular loads during cycling. By "pull down" is meant the first running period after installation or extended shutdown.

The application of the motor to the refrigerating unit is usually in the hands of the refrigeration engineer or a field installation engineer. Because of their desire to keep down the initial cost (as the cost of the motor is usually a large part of the cost of the condensing unit), the refrigeration engineer naturally will use all the available torque and carrying capacity of the motor at hand. In a refrigeration unit we shall show that the limiting load is not the running load, but starting and accelerating under the most adverse conditions. This is another way of saying that the motors are selected on a starting and accelerating torque basis rather than on a temperature rise at name-plate horsepower basis.

Suppose, for example, a new unit is to be manufactured. A compressor having a suitable bore and stroke based on previous experience of cost, etc., is designed and built. The condenser is selected in the same manner. The evaporator or cooling unit is designed to meet the demands of the load. This gives the engineer a unit on which to base his tests. As the majority of compressors are belt-driven, the speed is regulated by pulley sizes on the motor.

In any given unit the condensing pressure is a function of the effectiveness of the condenser. This pressure varies in the same manner as the pressure-tempera-

Paper number 39-18, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 30, 1938; made available for preprinting December 9, 1938.

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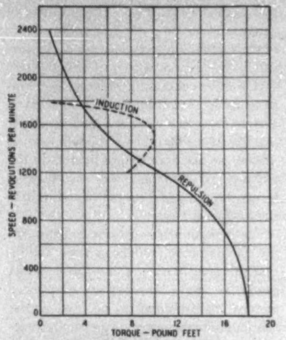

Figure 2. Speed-torque characteristics curve of one-horsepower repulsion-start induction-run motor

ture curve for saturated vapor for the refrigerant used. For various cooling-water and air temperatures that are likely to be encountered, and by a few tests on the effectiveness of the condenser, the range of condensing pressures can be determined. Further, by knowing the type of load on the system and the temperature of the evaporator a range of back pressures can be determined. With this range of condensing and back pressures defined the unit can be tested for motor requirements. A pulley is selected giving an average speed and tests are started.

For starting or breakaway in a given compressor the greatest torque required of the motor is at the condition of greatest difference between the condensing and back pressures. This condition occurs on certain types of loads and usually when the back pressure is very low. A pulley is then selected that will start the compressor under this worst condition at some arbitrary voltage limit from 10 per cent to 20 per cent under the name-plate rating.

This worst condition of condensing and back pressure usually occurs sometime during the running cycle and not after the unit has been off and is ready to start. If there is a momentary power failure and power returns there must be sufficient torque available to start the compressor.

The accelerating of the compressor requires a torque which is nearly proportional to the absolute back pressure. The pull-in or minimum accelerating torque is then the limiting factor in the amount of load the motor will accelerate. The high accelerating torque is necessary because of the work which is being done during each stroke of the piston. This work is the compression of the gas and it will be seen that with greater back pressure more gas is compressed during each stroke of the piston. Usually, it is found that the pull-in or accelerating torque of the motor is the limiting factor and not the starting torque. This is more noticeable on repulsion-start induction-run motors because of the high ratio of starting to pull-in torques.

Since the back pressure at which the unit runs is determined by the nature of the load, the range of back pressures will be great. If the evaporator or cooling coil is to cool an ice-cream cabinet the temperature of the coil will be relatively low, around ten degrees Fahrenheit. If the cooling coil is to be used for air conditioning, its temperature will be much higher, probably 40 degrees Fahrenheit. The back pressure in the former case is low and in the latter case high (assuming a given refrigerant). Since the accelerating torque is a function of this back pressure, there should be a theoretical best speed of the compressor for each back pressure. Since this is not practical the same compressor is usually provided with three different pulleys which are used for low, medium, and high back pressures.

Thus the refrigeration engineer has utilized all the torque of the motor in the application to the compressor. The last point he checks is the running load. For present-day compressors the running load when the torque is completely utilized is usually beyond the name-plate horsepower rating. Although a large number of cases require intermittent operation of the motor, the design of the whole unit from a temperature standpoint must allow for continuous operation of the motor.

For example, take a typical unit driven by a one-horsepower 110–220-volt 1,750-rpm 60-cycle single-phase repulsion-start induction-run motor. The speed-torque characteristic curve is shown in figure 2 and the running characteristics are shown in figure 3. The compressor was tested as in the procedure above and it was found that three pulleys for low, medium, and high back pressures were satisfactory, giving compressor speeds of 635, 570, and 510 rpm. The characteristics of the compressor are partly shown on figures, 4*a*, 4*b*, and 4*c*. Freon (*F*-12) is used as a refrigerant and a water-cooled condenser is shown. Similar data are available for the same compressor with an air-cooled condenser.

A typical application of the condensing unit would be made in a manner as described above. For selected values of 50-degrees-Fahrenheit refrigerant temperature and 70-degrees-Fahrenheit condensing water, the data given in table I appear.

The motor on the installation was found to accelerate the load at 90 volts on a 110-volt system. Satisfactory starting conditions were also assured. The temperature rise of the motor as measured by resistance at this load was 46 degrees centigrade in the rotor windings and 38 degrees centigrade in the stator windings. Thermocouple measurements for the same load gave 44.6 degrees centigrade and 40.4 degrees centigrade.

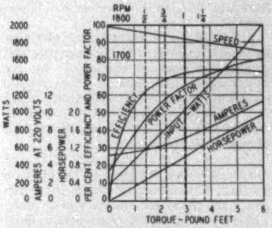

Figure 3. Running characteristics of one-horsepower repulsion-start induction-run motor

If, however, the load is increased because of an increase in the condensing water temperature from 70 degrees Fahrenheit to say 80 degrees Fahrenheit, the horsepower required rises to 1.57. At this new shaft load the temperature rise by resistance is 46 degrees in the stator windings and 54.5 in the rotor windings. At an increase in line voltage from 110 to 120 volts (which is very common in service today) the temperature rise at 1.57 horsepower load will be 58 degrees centi-

Table I

Condensing Water (Deg F)	Btu per Hour Compressor Output	Back Pressure (Pounds per Square Inch)	Motor Input (Watts)	Motor Shaft Output (Horsepower)	Temperature Rise by Resistance (Deg C)	Temperature Rise by Thermocouple (Hot Spot) (Deg C)
70	23,500	46.7	1,395	1.40	46 (rotor) 38 (stator)	46.6 40.4
80	22,600	46.7	1,530	1.57	54.5 (rotor) 46 (stator)	53.3 48.9

grade by resistance and 60 degrees centigrade by thermocouple in the rotor windings. (All the thermocouple values are the hot-spot values obtained from a large number of locations.)

The temperature rise of 58 degrees centigrade continuous at the 1.57-horsepower load does not seem excessive because the load is a maximum. The load on the system cannot be changed unless the temperature of the condensing water rises. This is an ideal application of the motor however, as the user is getting the most refrigeration per dollar from the installed apparatus because he is using all the torque available in the motor and because the running point is at the point of maximum efficiency of the motor.

There are of course, a large number of other considerations in the selection of the motor for this refrigeration system. The starting current of the motor should be low enough to comply with the regulations of the public utility systems. The noise level of the whole system and particularly of the motor must be low as the installation is made where quietness is necessary. The magnetic noise of motors for refrigeration units has been given considerable attention. Although a design which gives low magnetic noise is essential, it has been found that improvements of manufacture to hold the air gap concentric have greatly assisted in producing a consistently quiet product. Due to the nearly universal use of V-belt drive it is necessary to build motors with end-play takeup or cushioned for end-bump. In general, the large production of refrigeration motors has helped the motor manufacturer to improve the quality and decrease the cost of motors in these sizes. The characteristics and ratings of motors used on refrigerating systems, because of the large quantity of motors used for this purpose, have greatly influenced the characteristics and ratings of other motors. It is therefore logical to discuss the ratings of motors as used on refrigeration systems.

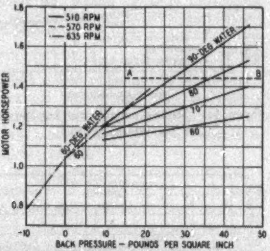

Figure 4a. Condensing-unit characteristics

A-B—Line of maximum loading

Motor Ratings

With the use of the torques and overloads described above, other problems are introduced. The horsepower marking along with the corresponding full-load current as placed on the name plate are used by various code authorities for selecting the proper wiring and fusing. The control engineer uses these currents in selecting the proper overload control. The public utility uses the horsepower rating as stamped on the name plate to set the maximum allowable starting current. Further, the whole electrical industry refers to the motor in terms of the marked horsepower. However, a newer use of the horsepower markings has arisen in the refrigeration industry and that is the naming of the refrigeration unit by the horsepower of the motor used on it. This latter practice transfers the variations of compressor applications, designs, and inequalities of the whole system to the motor when comparing various units.

Gradual development and improvement of design and manufacture of refrigeration motors without an increase in the physical dimensions has further raised the torque obtained from a given quantity of materials without increasing the name-plate rating. It is felt that here we have a typical case of a motor rating based on carrying capacity at full load as now shown by AIEE Standards, not coinciding with actual practice.

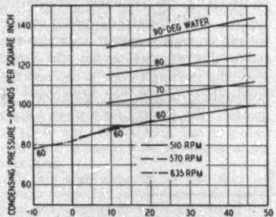

Figure 4b. Condensing-unit characteristics

When we suggest formulating a new method of rating for these motors, we immediately enter new territory.

It is felt that a service factor is a makeshift way of showing the overload of these motors. It is not all-inclusive in that it does not give a true picture of the higher starting and accelerating torques and does not allow for starting current. It is felt that too much education of the code authorities would be necessary to get the proper wiring and fusing to accommodate the increased load without the name-plate stamping being accordingly altered.

It is pointed out that for refrigeration applications the required life of the motor seldom is greater than 15 years. Usually, in this length of time the unit is replaced by more up-to-date equipment. The running time for the average unit on a refrigeration application is much less than full time and actually nearly half time. On an air-conditioning unit the running time during the year is one-fifth to one-fourth of the total time. There are also a great number of cases where the on cycle for a large part of the life of the unit is so short that the motor does not reach its ultimate temperature during each cycle. The low ambient temperatures usually encountered give a further factor of safety for motor temperatures. An ambient temperature above 90 degrees Fahrenheit is recognized by a suitable decrease in compressor ratings and motor loadings by the refrigeration unit manufacturers. Further, on air-cooled condensing units the use of the fan on the motor shaft extensions to pass air through the condenser, usually cools the motor in addition to the fan within the motor itself. Therefore, on an insulation-life basis we have an excess of useful life of the insulation for a hot-spot temperature of 105 degrees centigrade. A further important point to consider is that the difference between the hot-spot temperature as measured by thermocouple embedded in the windings on motors of these sizes and the temperature as measured by an adjacent thermometer is very little. This leads us to the suggestion that for refrigeration applications, motors be rated 50 degrees centigrade. As the published tables of the refrigeration manufacturers show the

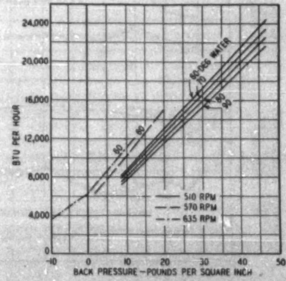

Figure 4c. Condensing-unit characteristics

permissible overloading, and as this overloading is not allowed beyond a point where there will be a temperature rise greater than 50 degrees centigrade, there would be no need for a service factor if the horsepower of the motor used has a 50-degree-centigrade rise as its name-plate stamping. If 40 degrees centigrade rise is used by the electric industry as standard, the refrigeration engineer will still load his motors to a 50-degree-centigrade rise and we will have loads out of line with name-plate ratings.

Specifying the temperature rating of motors used on refrigeration systems will not accurately define the motors as used today. This alone will not stop the practice of having a horsepower stamping on the name plate different from the actual horsepower load on the motor. However, since the motors applied and used on a torque and temperature basis and from experience over the past years torque values have been found which are satisfactory for refrigeration applications it is entirely possible to specify for motors on refrigeration applications the following:

(*a*). The accelerating torque per horsepower

(*b*). The starting torque per horsepower

(*c*). The starting efficiency in pound-feet per ampere locked-rotor current

The curves in figure 5 show the accelerating and starting torque per horsepower as found today. If, however, another curve of torque per useful horsepower is drawn, it will coincide closely with the torque per horsepower if the horsepower ratings were all moved up to the next higher horsepower ratings.

Figures 6*a* and 6*b* show the locked-rotor currents per horsepower for the repulsion-start induction-run and capacitor type motors. In figure 6*a* the currents are taken from present-day motors and based on the horsepower stamping on the name plate. Figure 6*b* shows the locked-rotor current per horsepower for the proposed name-plate stamping. For the repulsion-start induction-run motors the horsepower used was the useful horsepower as shown in figure 5 which is practically the same as the next higher horsepower rating. For the capacitor motor, the horsepower used was that obtained from present motors when rated 50 degrees centigrade rise. These starting currents per horsepower would be obtained if the present motors were designed for 50 degrees centigrade rise. Figure 6*c* shows the starting efficiency in torque per ampere locked rotor current. If then, 50 degrees centigrade rise is accepted the torques and starting efficiency could be selected as follows:

5.5 pound-feet per horsepower minimum accelerating torque

11.0 pound-feet per horsepower starting torque

0.5 pound-foot starting torque per locked ampere at 220 volts

As was pointed out previously, the starting torque available on the repulsion-start induction-run motor was usually much higher than necessary and the limiting torque of the motor on nearly all applications is the minimum accelerating torque. With the capacitor-start capacitor-run or capacitor-start induction-run motor this situation is not the same. Here the starting torque is less and the accelerating torque per horsepower is greater. See figure 7 for a speed torque curve of a capacitor motor. The starting torque per ampere or starting efficiency is much less. At the present time capacitor motors are not used in many places above three-fourth horsepower but from the data available the torques are as follows:

5.5 pound-feet per horsepower minimum accelerating torque

10.0 pound-feet per horsepower starting torque

0.3 pound-foot starting torque per locked ampere at 220 volts

It is therefore felt that here we have torque values which specify the motor and which can be tied up with the proposed name-plate rating. They are satisfactory from the standpoint of the user, the starting currents agree with those required by the public utilities, and the control engineer can select proper control within the limits of the National Electric Code. These also prevent the false marking of name plates.

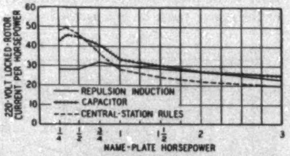

Figure 6a. Starting currents per horsepower for present-day motors

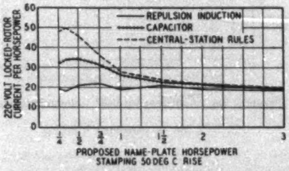

Figure 6b. Starting currents per horsepower for proposed 50-degree-centigrade motors

It may be argued that specifying of the torques per horsepower will tend to limit new developments or improvements in the present motors. There is obviously nothing to prevent the engineer from increasing the starting or accelerating torque per horsepower if there is no increase in starting current. There is also nothing to prevent the engineer from increasing the horsepower carrying ability of a certain motor if he does not change the torque relations as set.

The increasing use of hermetically sealed compressor units makes the motor

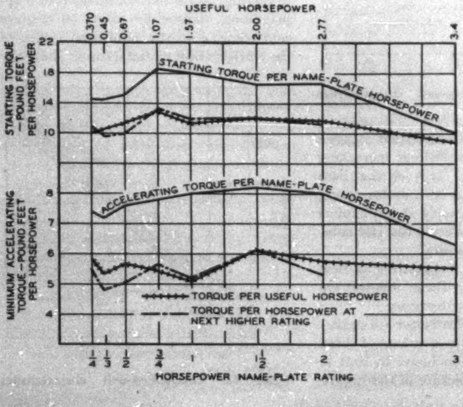

Figure 5. Starting and accelerating torques for various horsepower ratings

ratings for these units a pertinent subject. The temperature rise of the motor here is dependent upon the refrigeration manufacturer. If, however, the horsepower ratings are not set on a similar basis of torques per horsepower the same bad practice of false name-plate stamping will creep into the ratings of these motors. It is not the purpose of this paper to state what these should be at the present time.

It will, of course, be argued by some that this basis of rating may be satisfactory for refrigeration applications, but not for others. From the standpoint of the public utility company the starting current per horsepower could not change and if less starting torque is necessary there would probably be a decrease in starting current.

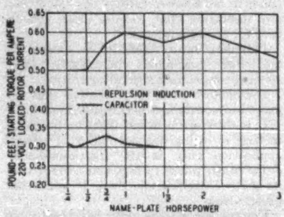

Figure 6c. Starting efficiency for capacitor and repulsion-start induction-run motors

It must always be kept in mind that standards or ratings set by the electrical industry should allow the large-scale user to get the fullest possible use of the motor for the requirements peculiar to its application. If the standards do not allow this, large users of motors will either break the standard or manufacture motors for their own use.

Conclusion

It has been shown that in the application of refrigeration motors, the accelerating torque and starting torque consistent with starting-current limitations with a 50-degree-centigrade temperature rise are the determining factors.

The recommendations for setting values of starting torque per horsepower, accelerating torque per horsepower, and starting efficiency in torque per ampere of locked-rotor current offer a logical and definite basis of rating for these motors. These tie together the present two types of high-torque single-phase motors so that the same horsepower rating on the name plate can be used on both for any given compressor application. These also offer a method of rating motors for use in hermetically sealed units which are increasing in use for all sizes of refrigerating units.

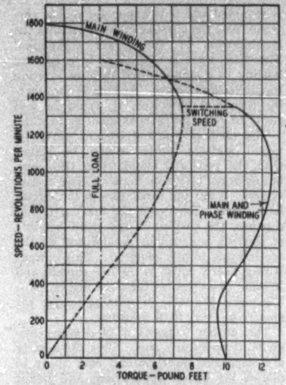

Figure 7. Speed torque characteristics curve for one-horsepower capacitor-start capacitor-run motor

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Discussion

D. F. Alexander (nonmember; General Motors Corporation, Dayton, Ohio): This timely paper outlines an application problem which should be of great interest to those who design or create standards for small single-phase motors. The refrigeration and air-conditioning industry has become a larger user of these motors, and has special requirements which the familiar general-purpose motor does not meet. Manufacturing competition has forced an economy of motor materials to suit the job at hand, and the resulting line of motors calls for a reconsideration of existing rating standards.

It has been apparent in this field for several years that motor name-plate markings and application codes are out of line with practice. This has handicapped the manufacturer, the salesman, the contractor and the purchaser, not to mention the power companies and the sponsors of the National Electric Code. Attempting to find a suitable compromise among the various codes and standards now in force, it appears that all these interested parties have cause for complaint.

The selection of proper controls, wiring overload protection, and fusing for these motors is often based on the name-plate horsepower and current, despite the fact that such markings today give no indication of the actual motor overloads. Wiring and fusing are often inadequate, or must be replaced by the contractor before code authority approval can be obtained. Summing up, we have today a varying-duty motor load often far in excess of the motor rating, with fictitious name-plate markings. A revision is in order, to bring the marking more nearly in line with the facts. As Mr. Rutherford points out, a revision of these motors to a 50-degree-centigrade rise basis, from the present 40-degree-centigrade basis, would go far to remove the present confusion in this respect. In addition, suitable wording might appear on the name plate to distinguish these motors from the general-purpose type.

The paper describes the steps necessary in selecting a motor for a refrigeration compressor. You will note that the torque requirements, in general, determine the motor design, and not the temperature-rise at name-plate horsepower. For that reason, it would be preferable that the motor name plates be rated in terms of torque and starting currents. This is not practical, since the horsepower rating is the common term for identification of the complete unit as well as for the motor. As the next best thing, then, let us specify torque and current limits for the 50-degree-centigrade-rise refrigeration motor, in the manner described in the paper. It has been suggested elsewhere that a new series of service factors for 40-degree-centigrade motors be used, rather than a straight 50-degree-centigrade rating. This may be satisfactory for the motor manufacturer, but for those who must select control equipment and wiring for which there are no service factors, the problem might become more complicated than at present. The use of such service factors for *polyphase* motors five horsepower and larger may be justified, but does not come within the scope of these comments.

The original basis for selecting 40 degrees centigrade rise for general-purpose motors did not allow for an application such as this one. A number of important factors permit a higher temperature rise, as listed in the paper. New information on the life of class *A* motor insulation, as reported in other papers at this convention, leads me to believe that a 60-degree-centigrade rise rating for refrigeration motors might not be excessive. However, we must not overlook the effect of high temperatures on motor-mounted capacitors, the excessive copper losses due to the cumulative effect of heating at prolonged overloads, or the danger to the lubricating system. Severe overloads in some motor designs will also result in poor speed regulation. Everything considered, and noting successful experience during the last few years at 50 degrees centigrade rise, it would seem that this value would be best.

With the spread of commercial refrigeration and of air-conditioning units, the next few years will see a large increase in the use of single-phase motors from one-third to three horsepower. In this connection, it would be profitable to note the progressive and realistic attitude toward motor ratings as expressed in Mr. Hellmund's paper (AIEE TRANSACTIONS, volume 58, 1939, pages 499–503). It would be very helpful if any changes in rating could be made promptly.

L. C. Packer (Westinghouse Electric and Manufacturing Company, Springfield, Mass.): This is an excellent paper outlining the application of single-phase motors for refrigeration compressors and the author's recommendations for rating of motors for this type of application are of considerable interest.

In the application of single-phase motors to refrigerating compressors, there are, as the author states, many things to consider.

For instance most domestic refrigerators use the hermetically sealed type compressors, using split-phase-type induction motors, whereas the commercial-type refrigerator units use both belt-driven and hermetically sealed type compressors.

The belt-driven type used in most cases uses the repulsion-type motor. The hermetically sealed type, as a rule, uses a capacitor start and run motor from approximately $^1/_4$ horsepower to $1^1/_2$ or 2 horsepower. Small air-conditioning compressors of the hermetically sealed type use capacitor motors of a little higher rating.

As the author states in his paper, it is essential that the motor have sufficient accelerating torque so that it will not pull out at the maximum back pressure and discharge pressure at the lowest expected voltage.

The starting torque required depends a great deal upon the type of compressor. The starting torque of a repulsion motor is greater than split-phase or capacitor-type motor; therefore it is adaptable in most cases for belt-driven-type compressors. It is well to have reserve starting and accelerating torque in this case to take care of cases where the belt may be tightened too much.

In the hermetically sealed compressor, which is gaining prominence rapidly, the starting torque required, at the lowest expected voltage, depends upon whether or not it is desired to use an unloader to equalize the pressures, before restarting after stalling. It is the designer's duty to get the best starting conditions at the lowest cost of the equipment and at the lowest possible starting current. Also keep in mind that he wants the highest possible efficiency with the necessary accelerating torque.

Assuming that no unloader is used. The motor of capacitor type must have sufficient capacity in the auxiliary-winding circuit to get the starting torque required. However, sufficient torque may not be obtained in some cases without impairing the performance under running conditions because the maximum starting torque is obtained when the reactance of the starting capacitor is equal to the reactance of the auxiliary circuit. The current in the auxiliary winding and the capacitor is also the maximum under this condition. If this condition does not give sufficient starting torque it does not pay to increase the capacity beyond the point where the reactance of the capacitor is less than the auxiliary winding reactance, as the starting torque starts to decrease because of the decreasing current in this phase and smaller phase angle between the main and auxiliary windings. The designer will then most likely put in an unloader to equalize the pressures. The starting torque required in this case is much less than without an unloader and the motor will require less capacity; in most cases at a saving and at a slightly lower starting current. It is even possible in many cases to use a single capacitor for both running and starting, thus effecting a further saving.

In the case just cited, the minimum cost is obtained which is important to the customer and the starting current satisfies the requirements of the power companies. Therefore, there is nothing to be gained by holding to a standardized ratio of starting torque to horsepower. As long as the starting torque is sufficient, it would be less costly to keep this ratio as small as possible.

The ratio of starting current per foot-pound starting torque would be higher than the proposed standard in this case, but as noted above no higher torque is necessary. While the capacitors in the circuit have some influence on the line current at starting, the current in the main winding at starting is definitely established by the accelerating torque required. This current is very nearly equal to the line current and in some cases greater than the line current. Therefore, the ratio of starting torque per ampere has very little meaning.

The author suggests a 50-degree-centigrade temperature rise. This apparently is a debatable question and may apply to motors for belted applications and might also come within the suggested values by Messrs. Alger and Johnson in their paper on "Rating of General-Purpose Induction Motors" (AIEE TRANSACTIONS, volume 58, 1939, pages 445–59). However, in hermetically sealed compressors, there are, as noted before, many conditions to meet. fifty degrees centigrade rise is too high with some types of permissible insulation and in some cases the higher temperature is too high for other reasons. There are some cases where the transmission of motor heat does not increase with load. There is then no definite relationship between temperature rise and horsepower. Thus, it appears that to set up a standard of rating motors for refrigeration and air-conditioning compressors without careful consideration of the problems of hermetically sealed units, may handicap the manufacturers of this class of apparatus with no advantages to the user. Of course, no one has to design the motor for a 50-degree-centigrade rise. But without some basis there is nothing definite from which to establish the horsepower rating.

Messrs. Alger and Johnson noted in their paper just mentioned that temperature rating standards do not apply to this class of motors. There is considerable merit to this thought, as well as to adhering to breakdown torque and starting-current rules, but the ultimate user may be paying in efficiency for a breakdown torque that is not required. Thus in the case of hermetically sealed compressors the application can be satisfactory with breakdown torque and starting torque below suggested values for a standard, resulting in economy in the size of the motor and yet with satisfactory starting current. Rotary-type compressors will require special studies concerning the application of motors to them.

Therefore, where hermetically sealed units are concerned, if the name plate is stamped with the horsepower and load current representing some standardized working load, and in line with the starting current rules, the desires of National Electric Code, Underwriters and power companies, and control engineers will be met. The name-plate current, however, may not tie up with the horsepower rating because auxiliary apparatus, fan motors for cooling, etc., will take some of the current.

R. A. Fuller (General Electric Company, Fort Wayne, Ind.): Experience with the application of motors to refrigeration and air-conditioning compressor drives indicates that the limiting factor will be one of the following:

1. Maximum continuous permissible load
2. Starting torque
3. Maximum, or pull-out torque
4. Accelerating torque

Repulsion-induction motors tend to be limited by accelerating torque or maximum continuous permissible load. The principal limiting factors for capacitor motors are starting torque and maximum continuous permissible load. Only in the one-sixth and one-fifth horsepower ratings has there been any indication that torques alone can be used for rating purposes.

Polyphase motors tend to be limited by maximum continuous permissible load, starting torque, or accelerating torque.

D-c motors tend to be limited only by maximum continuous permissible load.

It is therefore believed that the proposed method of rating has possibility of general application only in the fractional-horsepower single-phase ratings and further that its general application there is somewhat questionable.

Compressor designs enter into the torque requirements. For example, a four-cylinder compressor will tend to have a starting torque requirement approximately the same as a two-cylinder compressor of one-half the capacity. Thus, the construction of the compressor will have considerable influence in determining the limiting factor in the particular motor application.

The refrigeration engineer has considerable test data and empirical procedure on motor torque requirements and motor loading. Fundamentally, however, motors are still applied to these refrigerant condensing units by cut and try methods. The technical facilities of the present day should permit us to superimpose a motor speed-torque curve on a compressor speed-torque demand curve and thus accurately and readily determine the suitability of the motor. Work already done along these lines has shown some promise.

It is suggested that the torque elements of these applications might best be met by comparing speed-torque curves of the motors with speed-torque demand curves of the compressors. It is recommended that any rating method should include maximum continuous permissible load.

A. F. Lukens (General Electric Company, Lynn, Mass.): Mr. Rutherford has brought out the essential points of applying motors to refrigeration and air-conditioning compressors. The more important points are:

1. Motor size is determined by starting and pull-up torque rather than motor heating.
2. The pull-up torque is usually more important than starting torque.
3. Motor noise and end-bump.

He also states that capacitor motors have not had wide use in the sizes above three-fourths horsepower.

However, integral-horsepower capacitor motors can be designed that meet the requirements pointed out by Mr. Rutherford of high pull-up torque, quietness, and end-bump and in addition have the advantage of high efficiency and power factor and high maximum running torque. The last is important as it prevents lowered efficiency and light flicker brought about by overloads, undervoltage, or inadequate flywheel, or the combination of the three.

Tests of integral-horsepower capacitor motors on compressors of different manufacture have proved that 300 per cent starting torque is ample, and that more than this is unnecessary. This checks Mr. Rutherford's twice repeated statement that for the repulsion-induction motor the pull-up torque and not the starting torque is the limiting factor. In other words, the repulsion-induction motor has an excess of starting torque.

Since pull-up torque and starting current are the limiting factors in starting ability, it is suggested that the third specification, namely, "the starting efficiency in pounds-feet per ampere locked-rotor current" be changed to the ratio of pull-up torque divided by locked-rotor amperes. This value is then a measure of the starting efficiency of the motor.

The pull-up torque of a well-designed capacitor motor should be greater than 200 per cent of full load torque on the present basis of rating and can be 220 per cent. The starting current of the capacitor motor is not more than ten per cent greater than the repulsion-induction motor as shown by figure 6. Using the most adverse values the ratio is 0.25 foot-pounds pull-up torque per locked-rotor ampere for both types of motors.

The application of motors to use the full extent of their torque ability obviously reduces the factor of safety of the motor under load, so that it is felt that protection of the motor should be supplied. An automatic reset overload relay actuated at least in part by line current provides excellent protection under all conditions of abuse without completely stopping the refrigeration. Such a device prevents damage to the motor under severe overloads without completely interrupting the refrigeration which in turn prevents wholesale spoiling of the refrigerated product. In the case of failure to start, the device removes the motor from the line for a time long enough for it to cool down before restarting. In the meantime, the head and back pressures have had a chance to equalize and the starting duty is easier. This "limping" operation of the compressor will eventually come to the attention of the user and can be fixed without total loss of refrigeration.

Chester Lichtenberg (General Electric Company, Fort Wayne, Ind.): Standards for electrical machinery and particularly electric motors may be grouped according to usage.

Dimension standards, including frame sizes, afford design convenience. They present a hazard since any design restriction hampers imagination and progress. Design standards may be developed by individual groups of engineers promptly responsive to new conditions, but do not appear to have a place in industry standards sponsored by trade associations such as National Electrical Manufacturers Association, Radio Manufacturers Association, and American Cotton Manufacturers Association.

Application standards are essential for the effective and economical usage of apparatus including electric motors. Application standards might include items such as maximum starting torque, maximum starting current, and rotor flywheel effect. These might well be included in trade association standards since they affect the manufacturer, the buyer, the user, and the public utility. Recommended practices or adopted standards for these and similar items would be genuinely helpful.

Rating standards, however, are broader in usage, more fundamental in concept, and should be more rigidly defined than either design or application standards. They should be so fundamental that they will accurately reflect wide varieties of designs and application. They should be so simple that ordinary folks can use them. They should be so definite that all users will understand them. They must be unbiased and therefore the result of joint deliberations. Hence rating standards for electrical machinery, as distinguished from design standards and application standards, are a unique standardizing function of the AIEE and the refrigeration industry as an example is watching, certain that the AIEE will do a good job of rating standardization.

B. M. Cain (General Electric Company, Lynn, Mass.): Every engineer recognizes that the approach to an application problem should be based largely on the requirements of the load without preconceived ideas as to the type of drive or its possible limitations. By such an approach the limitations of available types of drive can be clearly and carefully analyzed.

Mr. Rutherford has clearly demonstrated that for the refrigeration compressor there is a definite ratio between the accelerating torque and the torque at average load. He has also shown that the refrigeration engineer applies the motor largely on the basis of its ability to provide the accelerating torque required by the load. The only other essential requirement is that the motor must not fail due to overheating.

Tests have shown that the ratio of accelerating torque to useful running torque of a compressor is about 1.65. This means that, after allowing for ten per cent low voltage, the pull-up torque of the motor must be 1.65 divided by $(0.90)^2$ or 204 per cent.

An extra safety factor, advisable in accelerating torque to guard against a long accelerating period, brings this to about 220 per cent.

The breakaway torque of compressors is shown by test to be about 50 per cent greater than the torque at 80 per cent speed. Thus the starting torque of the motor must be 204 per cent × 1.5 or about 300 per cent. No safety factor is needed.

The torque requirements of any driving motor have thus been established at:

1. Pull-up torque—about 220 per cent of full-load torque.
2. Locked-rotor torque—about 300 per cent of full-load torque.

Any torque in excess of these values will not be useful and conversely any motor having less than these values cannot, in most cases, develop its useful capacity.

Examination of the speed-torque characteristics of various types of single-phase motors shows that for these starting and accelerating torques a limitation of starting current will always limit acceleration torque and not starting torque.

The minimum accelerating torque of well-designed capacitor motors or repulsion-induction motors is substantially the same. It can be conservatively as much as 0.25 pound-feet per ampere of locked-rotor current at 220 volts. This means that the locked rotor current of motors for driving compressors must be $1 \times 5{,}250/1{,}725 \times 2.20$ divided by $0.25 = 26.9$ amperes per horsepower.

That is, regardless of type, the starting current must be at least about 27 amperes per horsepower in order for the motor to carry its useful capacity.

By limiting the starting current to less than this amount the user is forced to sacrifice useful capacity which he has paid for on a horsepower basis.

On the other hand extending the starting current limit to higher values enables the manufacturer to pass on to the user economies in cost and performance obtainable with higher starting currents.

Recognition of this fact by some of the leading power companies has already started the trend toward higher allowable starting currents.

C. G. Veinott (Westinghouse Electric and Manufacturing Company, Lima, Ohio): Mr. Rutherford is to be complimented for presenting a useful and educational paper. He gives data on the application of motors to refrigeration and air-conditioning apparatus, thereby showing the underlying reasons for the specifications set up by this class of manufacturer.

His paper brings out into the open a fact long recognized by those familiar with this application of motors—namely that the horsepower rating stamped on the name plate does not adequately describe the capacity of the motor since the latter frequently carries from 30 per cent to 50 per cent overload continuously. The severity of this service is enhanced by the fact that the motors have to be able to carry these overloads in ambient temperatures appreciably above the established standard of 40 degrees centigrade. Thus, a one-half-horsepower motor used in refrigeration service is really a three-fourth-horsepower motor.

The author proposes to increase the horsepower stamping on the name plate to a value more nearly commensurate with the useful horsepower that the motor will develop, at the same time increasing the rated temperature rise from 40 degrees centigrade to 50 degrees centigrade. Without a doubt this honest straightforward proposal has unquestioned merits. By also increasing the ampere stamping to correspond with the new horsepower rating, much confusion among the control people and code authorities who specify the wiring, will be eliminated. Such a method of rating is more in accord with the objectives of an ideal rating structure outlined by Mr. Hellmund ("The Rating of Electrical Machinery and Apparatus," AIEE TRANS-

ACTIONS, volume 58, 1939, pages 499–503). Unfortunately, however, this proposal may be opposed by the refrigeration manufacturers, particularly since some of them name their unit after the horsepower rating of the motor. Moreover, some of them seem to feel that, for a given unit of any size, the smaller the horsepower rating on the motor name plate, the more efficient the unit from a thermodynamic standpoint.

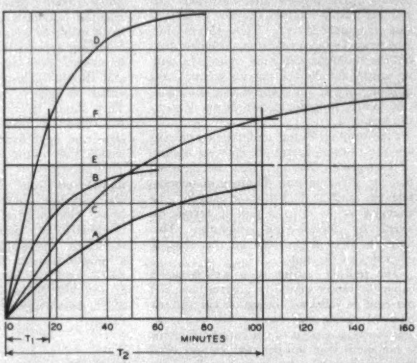
Also, if Mr. Rutherford's proposal were accepted, the motor manufacturers would have to be particularly vigilant to see that the refrigeration manufacturers didn't revert to their previous habit of applying overloads of 30 per cent to 50 per cent.

The author brings out the importance of adequate torques. He suggests certain torque specifications which are conservative and in accordance with present commercial motors. Why he specifies torques in terms of "pound-foot per horsepower" is rather puzzling. The present accepted method of specifying torques is in per cent of full-load torque. When specified in per cent, the figure becomes independent of the units used for measuring torque ("ounce-feet" is commonly used for fractional-horsepower motors and "pound-feet" for integral-horsepower motors) and is more nearly the same figure for motors of odd frequencies or different numbers of poles. Does Mr. Rutherford claim some unusual advantage for his new method of specifying torques?

Discussing the importance of locked-rotor amperes, the author proposes to specify them in terms of starting torque. If locked-rotor amperes are to be specified in terms of torque, we believe, concurring with Mr. Lukens, that they should be specified in terms of pull-in torque, which is generally the limiting torque, as pointed out by Mr. Rutherford. However, I believe locked-rotor current should be specified in amperes as at present; involving this specification with a value of torque can only be confusing to the power companies.

R. E. Hellmund (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): In Mr. Rutherford's paper and other papers,[1,2] motor applications are discussed in which the motor intermittently operates at relatively high load with prolonged intervening periods of no load or standstill. If in such cases the idle periods are relatively long compared with the operating periods, the root-mean-square load is, of course, well below the running load. On the other hand, the requirements for starting, pull-in, and pull-out torques are governed essentially by the character of the running load and therefore may have little relation to the root-mean-square load or to the continuous rating of the motor. Thus the latter obviously loses some of its practical significance and suggests the idea of making the pull-in, pull-out, and starting torques a more prominent part of the rating structure. However, while the conventional continuous rating in some cases loses in value on account of this, the motor temperature nevertheless is an important limiting factor in the operation of the motor. For this reason and also because rating by temperature-rise is so firmly established, it seems desirable to retain the temperature limits in some way or other as the basic factor for rating, but to supplement them by additional provisions for torque and starting-current values. Another reason for retaining temperature limits as a basic factor is that they are common to nearly all types of electrical apparatus, including generators, transformers, control devices, etc., while torques are of particular significance with motors only. It is believed that neither Mr. Rutherford nor the authors of the related papers previously referred to will disagree with this point of view, but their remarks might be misinterpreted because they have outlined methods for the application and selection of motors, in which, for practical reasons, the torque values are given consideration first and the temperature limitations are checked subsequently.

Figure 1



For some applications of motors, provisions for larger ratios of pull-in, pull-out, and starting torques have been made in the past and have been found to be a simple way out and one that is usually understood by everybody because it does not change the well-established basic method of rating but merely uses somewhat different ratios for certain torque values. When the load cycles are short, so that neither the motor nor any of the wiring or auxiliaries have a chance to vary much in temperature during the load cycle, there apparently is no reason for changing this well-established practice. However, there are some applications of motors with longer load cycles where the established practice would be satisfactory for the motor but where the heating effects might prove to be harmful to other parts of the circuit having a smaller time constant than the motor. This can best be illustrated by reference to figure 1 of this discussion. The condition shown here may be representative of a practical case of compressor equipment starting cold and requiring a heavy load during a prolonged pull-down period after installation or an extended shutdown such as referred to in Mr. Rutherford's paper. In the circuit of such a motor, there is always some likelihood of some parts (either the control or the wiring) having a much smaller time constant than the motor. In figure 1, curve A represents the time-temperature curve for the motor, while curve B is intended to apply to some part in the circuit or control having a much smaller time constant than the motor. In this case it has been assumed that during continuous operation both curves A and B reach a temperature-rise which is safe for continuous operation, as indicated by line E. It is further assumed that both the motor and the other circuit parts can be operated safely for short periods at a somewhat higher temperature, as indicated by line F. If we now assume that the load during the pull-down period is about 40 per cent higher than the rated load, it may well happen that the total motor losses, which are composed of constant losses and I^2R losses, will be increased by about 50 per cent, thus resulting in a time-temperature curve C. On the other hand, the other parts in the motor circuit represented by curve B may be heated by I^2R losses only, which with the assumed overload would then about double, and, furthermore, because of the small time constant of such parts, corresponding temperatures will be reached quickly, as indicated in curve D.

Examination of the figure shows that there are three distinctly different conditions. If the pull-in period is less than T_1, or about 18 minutes in this particular case, neither the motor nor other parts of the circuit will reach unsafe temperatures and no special precautions or change in practice seems necessary. If the pull-in period is very long, exceeding T_2, or about 103 minutes, both the motor and circuit parts will reach unsafe temperatures, which simply means that larger apparatus of higher rating has to be applied. If the pull-in period is between the values T_1 and T_2 shown in the figure, the application is perfectly safe for the motor but not for some of the circuit parts. Naturally, if it is desired to obtain maximum economy, the motor of the smaller rating can and should be retained, but the wiring or control apparatus, or both, for a motor of a higher rating will have to be used. This is the condition referred to in Mr. Rutherford's paper which is of interest not only to motor engineers but also to engineers interested in the power supply and auxiliary apparatus. It is for such conditions that it may be advisable to give the motor a short-time or intermittent rating higher than its continuous rating, as this would then automatically lead to the proper selection of conductors and auxiliary apparatus.

The advisability of establishing intermittent ratings has, of course, been considered frequently, but so far no standard methods of rating along these lines have been established in the United States. As pointed out by Rutherford, the rather broad field of applications for refrigeration and air conditioning, makes a review of this problem in connection with standardization highly desirable if the best and simplest method for handling these applications is to be selected. Further study will be necessary to determine which method is best suited for this purpose and whether the standardization of certain intermittent ratings is more desirable. The application of motors in railway work has always been one of the most important examples of motors for intermittent operation, and here the problem has been solved in a practical way by assigning both a short-time (one hour) and a continuous motor rating. In view of the necessity for close application of apparatus in railway work, various methods for applying railway motors have been worked out. Therefore, it may be advisable to follow railway practice as closely as possible in some other applications. The proposal in a paper by Hildebrand[2] comes rather close to railway practice. Incidentally, a review of the methods of rating for intermittent loads seems timely, not only on account of the increased use of compressors for refrigeration, air conditioning, etc., but also because economic conditions surrounding these applications are different from those applying to other uses of motors for intermittent loads in industry. In many industries, the cost of motors and their power consumption is a very minor part of the total operating cost of such industries. Therefore, industrial engineers were perhaps inclined to do some overmotoring in doubtful cases because it gave them extra safety margins, which often were more important than maximum economy in first cost and operating costs. Contrary to this condition, the first cost and operating costs of refrigerating and air-conditioning equipment are an important factor to the owners of small commercial establishments or of homes, and in order to broaden the field of application, more attention may have to be given to economies possible.

While the condition illustrated in figure 1 may seem alarming, little trouble has been experienced in the past from overheating of circuit parts. Very likely the designers of control and other circuit devices have simply strengthened certain bottleneck parts whenever there was any indication of overheating in actual service. Some of this was perhaps also made necessary by the starting conditions of squirrel-cage motors. In other words, the co-ordination of the various parts of circuits has in the past been carried out successfully by providing rather liberal margins. If closer and more economical application of apparatus is to be accomplished, a more systematic co-ordination of standards will be necessary. Certain co-ordinating committees to be appointed by the AIEE standards committee will give this matter due consideration.

REFERENCES

1. RATING OF GENERAL-PURPOSE INDUCTION MOTORS, P. L. Alger and T. C. Johnson. AIEE TRANSACTIONS, volume 58, 1939, pages 445–59 (September section).

2. DUTY CYCLES AND MOTOR RATING, L. E. Hildebrand. AIEE TRANSACTIONS, volume 58, 1939, pages 478–83 (September section).

Paul H. Rutherford: Mr. Lukens has amplified the statements regarding the application of the motor to a refrigeration compressor on the basis of starting and accelerating torque but confines his comments to integral-horsepower capacitor motors which were not included in the paper. He does not state, however, whether he is referring to 40-degree or 50-degree-rise motors which is necessary when considering any torque comparison, between repulsion-start induction-run motors and capacitor motors. He states that the pull-up torque and starting current are the limiting factors which is not always correct as the starting torque of a capacitor motor is the limiting factor in most applications, particularly in the fractional-horsepower sizes. It is hard to agree with the value of 0.26 pound-feet pull-up torque per locked ampere for this reason.

It is certainly agreed that an inherent heating-overload protector is necessary and advisable on all capacitor-start motors for refrigeration applications but it is not yet fully determined that an automatic device is the best type and preferable to a manual-reset device on all sizes of motors. This will only be determined by experience. Mr. Lukens' statement that high maximum running torque is important does not coincide with his other statements and it will be found that in well-designed motors of either the capacitor or repulsion-start induction-run type there is ample maximum torque when the necessary starting and pull-up torque are obtained for the application.

Mr. Packer confines his comments to hermetically sealed compressors for commercial refrigeration use. It was not intended that the paper should include this type and we therefore, cannot see any direct bearing on Mr. Packer's comments on the paper.

Mr. Packer states that there is nothing to be gained by holding a standardized ratio of starting torque to horsepower. It was the author's point to try to select the horsepower as a ratio to the starting torque per ampere or the accelerating torques. We feel Mr. Packer has tried to apply the statements of the paper to sealed units. The paper did cover only belted units for commercial refrigeration and air-conditioning applications.

It is felt that all those discussing the paper with the exception of Mr. Fuller agree that a motor can be selected for a belted refrigeration compressor on a basis of starting and accelerating torque which Mr. Fuller states is impossible. He does not give any reason for selecting motors on the basis of maximum torque except that from the meager data on the experimental torque-recording device he speaks of, there is some evidence of the maximum torque being important. No specific sizes of compressors are mentioned but from the reference to a four-cylinder compressor it is probable that he is referring to a motor larger than five horsepower. It has been the experience of the author as well as many other refrigeration engineers after examining several thousand motors returned from the field during the last 15 years that failure of these motors was due to their not coming up to speed. This is evidence that these motors did not fail for lack of maximum torque but because of an insufficient starting or accelerating torque.

Mr. Fuller's comments on d-c or polyphase motors may be correct but these were not included in the paper.

Mr. Cain does not say whether the value of 26.9 amperes per horsepower is based on a 40-degree or 50-degree-rise motor. If this value is for a 40-degree motor, it is felt advisable to go to a 50-degree rather than raise the starting current limits as now used by the power companies.

Mr. Hellmund has commented on the intermittent character of the load taken by a refrigeration compressor. This is perhaps true of sealed-in units for household use but not so much with the conventional type unit as described in the paper.

Mr. Veinott's comments on the naming of the refrigeration unit by the horsepower rating of the motor was touched on slightly in the paper. Since current practice is to overload belted refrigeration motors to 50 degrees centigrade rise or more and successfully, it is felt that standards should be made that will acknowledge this fact. It was also suggested that since a refrigeration motor application is a "torque" application motor, torque should be tied up with name-plate horsepower stamping. It is felt that these two points are of utmost importance.

Mr. Veinott feels that the torques should be shown in per cent of full load torque rather than in pounds per horsepower. These figures seemed more convenient at the time but could be converted to percentages or ounce feet by simple arithmetic calculations.

Simplified Precision Resistance-Welder Control

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Synopsis: The types of resistance-welding applications requiring precision control heretofore characterized by electronic equipment are well-known. Attempts to extend the field of application to borderline operations have not met with complete success because of the initial expense and complicated nature of the control available. A simplified form of precision control consisting of a synchronized magnetic contactor and motor-driven timer overcomes these objections without compromise on results obtained.

ALTHOUGH many new types of spot-weld timers have been announced within the past two or three years, nearly all belong to a group that is characterized by a series of magnetic relays operated in conjunction with one or more time delay units of the electromagnetic, electrostatic, or mechanical types.[1] In each instance a magnetic contactor of considerable size is required to make and break the circuit to the primary of the welder transformer.

Relative simplicity and low initial cost have prompted the use of these nonsynchronous devices for all applications where extreme accuracy is not required. Operating limitations are well known and errors equivalent to *at least* a plus or minus one-half cycle of current are certain to occur although the actual timing period is perfectly metered. These errors result from inconsistent operation of standard a-c magnetic contactors and relays as well as more serious discrepancies introduced by failure to close and open synchronously the power line leading to the welder transformer.[2]

Effect of Nonsynchronous Control

The first specimen in figure 1 illustrates the effect of nonsynchronous control on welding of light-gauge stainless steel. Section *b* is a series of four good welds made with a regular load-current wave form and a one-half-cycle timing period. The welds are strong and the surface of the work is only slightly discolored. The welds in section *c* were made with the same pressure, current, and timing period but a transient wave form was obtained by changing the point on the reference voltage wave at which the welder transformer was energized. All four welds are overheated, testifying to the fact that the character of the wave form does affect the quantity of heat delivered to the weld. Section *a* contains a third group of four welds made on the same piece of work with equivalent current, pressure, and time setting. The power circuit was closed at a point which gave relatively normal wave form but it was opened slightly after zero, allowing nearly a full one-half cycle of arcing at the tips of the magnetic contactor. The overheated welds which resulted are a clear indication of the additional heat delivered to the work when an arc was allowed to occur as the power circuit was broken.

Assuming that an absolutely accurate timing period could be obtained with any one of the nonsynchronous units already mentioned, the variation in results evidenced by the three sections of specimen 1 could be expected. Of course, the effect of these variations is less noticeable when the nature of the work is such that longer timing periods can be used or the fusion point of the material is less critical. However, consistent results using a timing period as short as one-half cycle can be obtained when suitable synchronous control is used. Specimens 2 and 3 of figure 1 illustrate this fact in connection with 24-gauge stainless steel. Notice the consistency of the welds on specimen 2. Their strength is demonstrated by the "slug" of metal which has been pulled from specimen 3.

Previous Synchronous Equipment

At least three general types of synchronous control equipment have been proposed to overcome the variations just described. The first of these—full electronic control using mercury-pool-cathode-type power tubes instead of magnetic contactors—has served its purpose well.[3,4] The chief objections are high initial cost as well as complicated operational and maintenance problems.

Synchronous-motor-driven contactors have also been offered as a solution.[5] Although reasonably priced, their adjustment is critical and difficult to maintain.

A more recent development is the so-called "impulse contactor."[6] Energy stored in a capacitor is discharged into the operating coil of a magnetic contactor causing its contacts to close and open at points determined by the inertia of the moving parts as well as the character of the contactor finger spring and

SPECIMEN 1
SECTION A
SECTION B
SECTION C
SPECIMEN 2

Figure 1. Stainless-steel specimens showing effect on weld of nonsynchronous control compared with consistent results obtainable when suitable control is used

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Paper number 39-65, recommended by the AIEE committee on electric welding, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 14, 1938; made available for preprinting December 29, 1938.

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[1] For all numbered references, see list at end of paper.

the quantity of energy delivered to the operating coil. The equipment required involves considerable expense and the operating range is definitely limited. Proper adjustment is sometimes difficult because of the several related factors which influence the final setting.

Simplified Precision Control

This paper deals with a form of precision control unit consisting of a synchronous magnetic contactor and motor-driven timer. Two new developments make the system possible. The first is a special magnetic contactor consisting of two separately actuated poles connected in series (figure 2). One pole is closed early in the operating cycle. The closing of the second pole energizes the power circuit and opening the first de-energizes it. The actual period during which the transformer is energized is measured by the interval during which both poles are closed. Operating speed of the individual poles is not a limiting factor. Timing periods as short as one-half cycle are easily obtained.

The system just described is old but construction features of the contactors used are new. The closing contactor is adjusted to take advantage of a little-known operating characteristic. When properly designed a contactor's magnet coil can be energized at any point within a rather wide band and yet the tips of that contactor will close at essentially the same point on the reference voltage wave. The speed at which the contactor closes is influenced to a considerable degree by the amount of transient in the operating coil circuit. When correctly adjusted the energizing of that circuit earlier than normal results in a decreased transient and a slower closing speed. Thus, the contact tips touch at a point equivalent to that of a much later energization of the coil circuit. Likewise, the increased transient which results from late energization of the coil compensates for the delayed start in that the contactor closes faster. This characteristic can be employed to insure consistent closing of the power circuit, thereby avoiding various degrees of transient and resulting variations in heat delivered to the work.

Figure 2. Precision contactor consisting of closing and opening poles operated in sequence. Synchronizing equipment for opening pole is mounted on right-hand side of panel

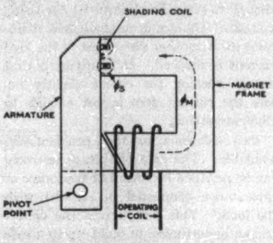


Figure 3. Sketch of magnet frame showing typical shading-coil circuit

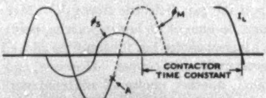


Figure 4. Phase relationship of main (ϕ_M) and shading-coil (ϕ_S) fluxes

The opening contactor is self-synchronized to insure the parting of contact tips at the minimum arcing point. Because of the nature of the device there is no need for synchronous interruption of the contactor operating-coil circuit. Regardless of the time at which the circuit is broken the contactor opens at the point for which it is set.

Synchronous Magnetic Contactor

Synchronization to within a plus or minus ten electrical degrees or better is rather easily obtained by separately energizing the shading-coil circuit of the contactor. All a-c contactor magnets are equipped with a shading coil to insure quiet operation. The conventional design (figure 3) consists of a short-circuited turn of some conductive material encircling about two-thirds of the magnet pole face. Flux (ϕ_M) set up by the operating coil generates a voltage in the shading-coil circuit and the resulting current provides a flux (ϕ_S) which is out of phase with the parent flux (figure 4). If this supplementary flux did not exist the magnet surfaces of the contactor would part momentarily at each period during which the main flux passes through zero and an objectionable vibration would result.

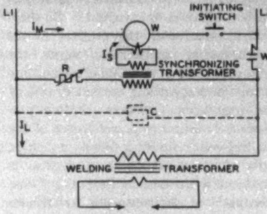


Figure 5. Schematic diagram of synchronized contactor circuit

By separately energizing the shading-coil circuit, synchronized opening of the contactor can be obtained without sacrificing quiet operation. Refer to the schematic diagram shown in figure 5. When the push button is closed the operating coil of contactor *W* is energized. The closing of the contactor energizes the welding transformer and simultaneously applies power to the primary of the small transformer which separately energizes the shading-coil circuit. Because of the difference in power factor between the operating-coil and shading-coil currents, the two out-of-phase fluxes necessary to quiet operation are obtained. Assume that the push button is released at the point marked *A* in figure 4. The main flux (ϕ_M) goes to zero but the shading coil flux (ϕ_S) is sufficient to keep the magnet surfaces sealed until it approaches zero value. Because the main flux is not there to supplement it, the magnet surfaces part. Although the shading-coil flux resumes in the negative direction before the armature of the contactor has moved enough to part the contact tips the contactor continues to open because the shading coil alone is not strong enough to reclose it.

The separately energized shading coil insures that the magnet surfaces always part at a point equivalent to the zero value of the shading-coil flux but the armature of the contactor must move an appreciable distance before the contactor tips part. The interval required for the movement is called the "time constant" of the contactor. Since the zero point of the load current bears no relation to the shading-coil flux the "time constant" of the contactor must be equal to the difference in time between a zero point in the shading-coil flux and a later minimum value in load current. Adjustment can be made by changing wear allowance (sometimes called "follow-up") or spring tension. However, a more convenient method is available. A rheostat in series with the primary of the synchronizing transformer varies the voltage applied to it. The corresponding change in magnetizing current drawn by the transformer primary winding provides a variation in power factor of the shading-coil current. Since a phase shift of about 60 electrical degrees is practical with this method the "time constant" of the contactor can remain the same while the phase relationship of the shading-coil circuit may be changed to locate the minimum arcing point. Once adjusted, the contact tips part at the selected point independent of the time at which the operating-coil circuit is broken.

Many attempts to synchronize the opening of a power circuit in conjunction with magnetic or motor-driven contactors have failed because of a tendency for the arc to restrike after it presumably had been extinguished. The oscillograph trace represented in figure 6 indicates the voltage drop across the contactor tips. When they are closed only the reference line represented by the sweep of the oscillograph is recorded. As the tips part an arc is drawn and the drop across the arc is recorded in the form of a small voltage in phase with line current. When the load current passes through zero the arc voltage must also go to zero. Since the contact tips have actually parted prior to this time the oscillograph must instantly record line voltage across the gap. Such is the case in test *A*.

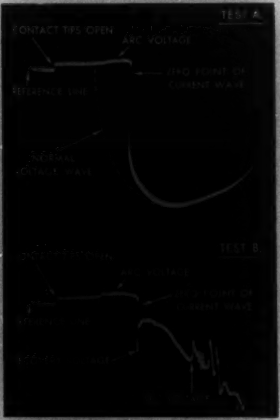


Figure 6. Oscillograms showing (A) arc interrupted, (B) arc restriking

For test *B* the contactor tips are parted at a point equivalent to that represented in test *A*. A similar arc voltage is recorded. It is reduced to zero with the load current. The voltage across the contact gap instantly approaches line voltage but the dielectric strength between the contact tips has not built up rapidly enough to resist breakdown by the rising voltage. The arc is restruck and maintains until another zero point of the load current is reached. It is apparent that merely opening the circuit slightly before the current zero is not enough to eliminate arcing.

Two solutions to this problem are available. The rate of voltage recovery can be retarded by means of resistance or capacitance connected in parallel with the load.[7] This delay gives the dielectric an opportunity to build up to a safe value. A second course of action is to alloy the contact tip material with elements intended to hasten the building up of the gap dielectric strength.[8] Mercury, because of its low vaporizing point, is a logical choice but it cannot be alloyed successfully with copper. However, cadmium, another choice of material with good deionizing properties, can be obtained in copper alloy form at reasonable cost. A combination of both corrective factors has proved to be the practical answer to the problem.

The synchronized magnetic contactor is an important development in connection with simplified precision control but it has an even broader field of application as an improved magnetic contactor for use with all kinds of welding machines involving heavy loads and frequent operation. The elimination of arcing at the contact tips overcomes the most pronounced limitation in contactor application. Mechanical life is above reproach but until now electrical ratings have been determined primarily by the heat liberated during the arcing period, and contact-tip life has been reduced proportionately. Figure 7 illustrates a 300-ampere (nominally rated) single-pole synchronized unit which has been successfully tested on a frequent operating cycle at 1,800 amperes, 220 volts, 47 per cent power factor.

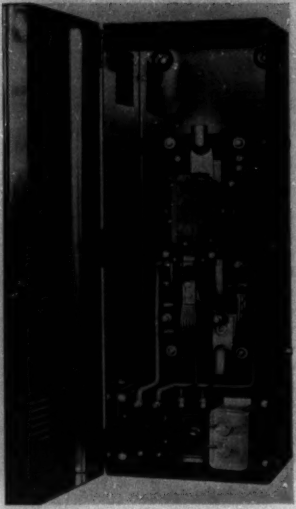


Figure 7. Single-pole 300-ampere (nominally rated) synchronized contactor for general application

Motor-Driven Timer

The second development contributing to the simplified precision control is a new form of synchronous-motor-driven timer. Devices of this kind in the past have involved one of two distinct design problems. When a continuously operating motor was used, a magnetically operated mechanical clutch was required to begin and end the welding operation. Positive synchronization could not be maintained through such a mechanical linkage. When an attempt was made to start and stop the motor at the beginning and end of each operation, poor synchronization resulted because of the tendency to "hunt" on starting and to coast in stopping.

The motor of the timing device under discussion is allowed to run continuously but a clutch is not required. The schematic circuit diagram shown as figure 8 illustrates this fact. Although the pilot contact can be closed at any point, the cycle of operation is not begun until the cam actuated contact TR_1 is momentarily made, energizing synchronized contactor W_1 and control relay CR_1. Since TR_1 closes at only one point in the operating cycle, all timing intervals are measured from that point. Relay CR_1 forms a holding circuit about TR_1. Additional rotation of the cam shaft closes contact TR_2 which energizes contactor W_2 and completes the circuit to the welder transformer. Relay CR_2 is

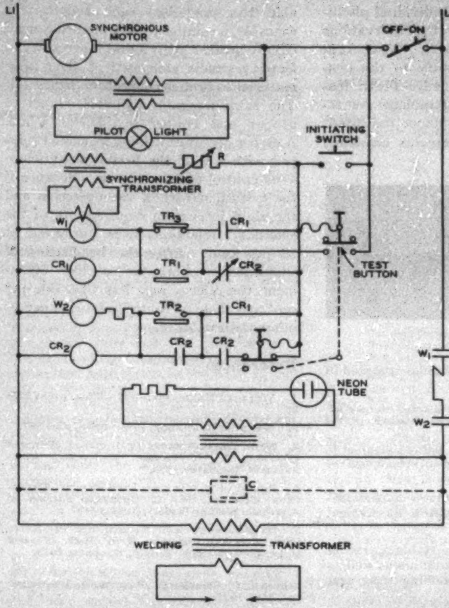


Figure 8. Schematic diagram of motor-driven timer circuit

closed at the same time to form a holding circuit around TR_2. Final rotation of the cam shaft opens contact TR_3 in the circuit to the operating coils of contactor W_1 and relay CR_1. The synchronized welder contactor then opens at the minimum arcing point. Relay CR_2 and contactor W_2 remain closed until the initiating switch is released. A normally closed contact on relay CR_2 prevents unauthorized repeat operation.

The weld period adjustment is made by turning knob *C* in figure 9. Its movement is transmitted through a pinion to a large gear on which contact TR_2 is mounted. The angular position of contact TR_2 with respect to the motor cam shaft determines the point at which welding contactor W_2 is closed. A pointer actuated by movement of the gear indicates the welding time selected.

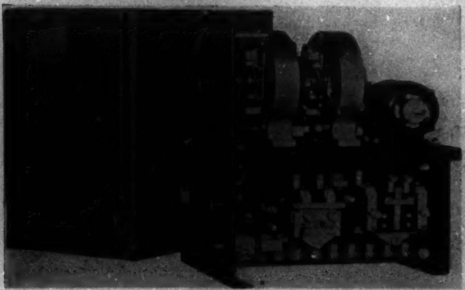
Contacts TR_1 and TR_3 are mounted on steel plates which can be rotated manually to permit initial adjustment. Ball bearings are used throughout to insure precise operation for the entire mechanical life of the device.

Figure 10 is a front view of the timer. A specially designed neon tube energized from a closely coupled transformer connected in parallel with the load indicates the exact welding time and assists in selecting the proper operating points for the magnetic contactors. During the period the welding transformer is energized, the bright glow of the tube is visible through a slot in the scanning disk which is attached to the end of the motor cam shaft. The resulting streak of light *A* includes dark bands as the supply line voltage passes through zero. Therefore, each section of the trace represents one-half cycle. Counting of the sections is not necessary since the timer dial is directly calibrated in cycles.

A test button *B* is provided to make possible repeat operation of the control equipment at a rate determined by motor speed. By closely observing the beginning of the light trace on dial as knob *C* is rotated, the stable operating point of the "closing" contactor can be selected prior to placing the equipment in operation. Proper adjustment will result in a consistent beginning of each successive trace.

Rheostat *D* is connected into the "opening" contactor synchronizing circuit and controls the point at which the contactor tips open. If the electrodes

Figure 9. Timer with interior removed to show mechanical construction



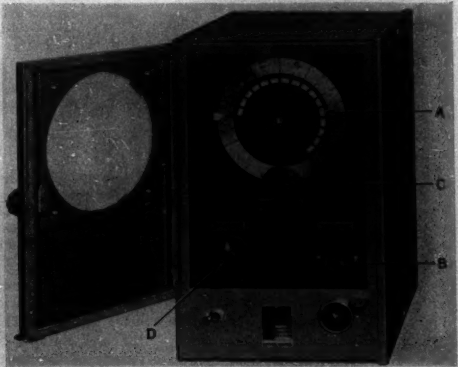


Figure 10. Front view of motor-driven timer showing adjusting means and neon light trace on revolving disk

of the machine are closed before the test button is operated the rheostat can be adjusted for minimum arcing before the welding operation is begun. Arcing will be indicated by an additional half-cycle band on the end of trace *A*.

Additional motor-operated contacts and a slight rearrangement of the circuit would permit the control of all four timing intervals (squeeze, weld, hold, and off periods) required in connection with fully automatic air-operated welding machines. Three knobs on the front of the panel would permit individual adjustment of all periods. The number of operations per minute is determined by the speed of the motor-driven cam shaft and could be varied by changing reduction gears between motor and cam shaft, by means of a simple transmission operated from the front of the panel. A dial calibrated in percentages and referred to the total number of cycles equal to a single revolution of the cam shaft at the prevailing speed would provide at a glance the relationship to each other of the component parts of the operating cycle.

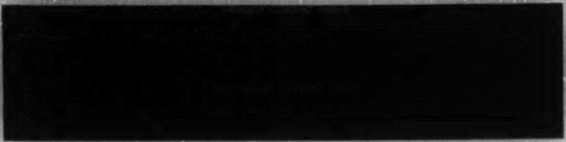


Figure 11. Oscillogram showing accuracy of precision control equipment breaking a load equivalent to 300 per cent of contactor nominal rating

The equipment just described is relatively inexpensive and easy to operate. Its mechanical construction is such that continued service without attention can be expected. One installation studied for a period of more than eight months was attended only by an untrained operator. Adjustments were readily made and no change was required until the end of the production run.

The accuracy obtainable with the simplified equipment is illustrated in figure 11. A cathode-ray oscillograph screen recording, in turn, the primary and secondary current wave forms of a welding transformer drawing about 300 amperes on the primary side was photographed at 30-second intervals during an 18-minute test. Throughout the entire operating period the circuit was established 150 times per minute for a duration of one cycle. The individual photographs apply in order to observations spaced 75 operations apart. Wave form and accurate timing testify to the consistent operation obtained. These features of the control equipment are responsible for the performance indicated:

1. Special magnetic contactor consisting of two separately actuated poles operated in sequence and having

(*a*). The circuit-closing pole adjusted to take advantage of natural response characteristic of the contactor.

(*b*). The circuit-opening pole self-synchronized to open at minimum arcing point independent of timer setting.

2. Motor-driven timer with a continuous-running motor but without a mechanical clutch.

3. Means for selecting the stable operating points of the magnetic contactors as well as determining the actual welding time and consistency.

Conclusion

It is reasonable to believe that many welding operations made on machines not now equipped with precision control could be made much less expensive if proper control were installed. Likewise, many parts previously fabricated by other means could now be welded. Only the high initial cost and relative complexity of the control heretofore available has prevented such action. For example, a company manufacturing small circuit-breaker parts found it necessary to fasten contacts composed of silver alloy material to bronze connectors (figure 12). The same assembly included copper to copper and copper to bimetal joints. A motor-operated welding machine equipped with one of the better nonsynchronous control units was used. Because of the critical nature of the operation and the rigid inspection requirements of the assembly, rejections were estimated at 23 per cent. After the installation of simplified synchronous control equipment the rejects were less than one per cent and those were attributable to causes other than welding.

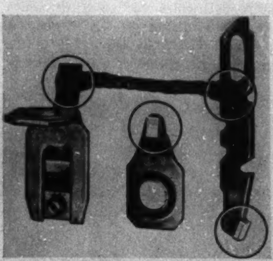


Figure 12. An assembly of small parts involving copper to copper, copper to bimetal, and copper to silver-alloy joints is successfully welded in production using simplified precision control

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Discussion

L. G. Levoy and **G. W. Garman** (General Electric Company, Schenectady, N. Y.): Mr. Roby has given an interesting paper and has described an ingenious method of improving the accuracy of contactors as applied to resistance spot-welding machines. This improvement in accuracy should result in better welding from contactor-controlled machines and will expand the field of contactor control. The impression is given that this contactor is equivalent in price and accuracy to precision electronic control and that it is less complicated. Electronic control without question is more accurate and in most applications does not require any adjustments except for the occasional replacement of a tube, which can be quickly and easily changed. While it is true that some electronic controls are relatively more complicated, it is only because the demand has been such as to require the flexibility and accuracy which they provide.

With reference to these more complicated control circuits, it has been interesting to observe the trend in the various requirements with increased use and confidence in this type of control. Several years ago it seemed that the time had arrived for simplification, but to our surprise accurate and consistent welding had led to the desire of welding methods and processes hitherto believed impossible or too expensive. This trend has led to the development of the interrupted type of welding and to the increased use of "heat control" by the phase-shifting method. In addition, there is a strong tendency toward the use of complicated time and heat control cycles. Thus the design engineer is faced with two conflicting trends, one toward simplification in an effort to reduce costs, which has led to the development of the ignitron contactor, and second, toward more flexible controls which inherently leads to higher costs.

The author states that the contactor tips open consistently within ten electrical degrees, but does not give specifically the accuracy of closing. It would appear that there are several factors which would affect the closing time and the opening time. It is expected that a reduction in line voltage would not only decrease the welding current in the normal manner but would retard the closing of the contactor, thereby reducing still further the welding current, particularly for short welding periods. Also, since the mechanical time constant of the contactor is fixed and does not necessarily agree with the power factor of the welder, a starting current transient may be present in the welding circuit. This transient is not necessarily detrimental, but will decay exponentially, producing variations in the current zero for different time settings. It is also well known that while the thickness of the material being welded does not affect appreciably the secondary current in low-power-factor machines, it does affect the power factor of the primary current. We would therefore like to ask Mr. Roby how critical is the adjustment of the shading-coil rheostat and what is the effect of these variations on this setting.

Variations in the phase angle of closure of the contactor tips with respect to the alternating voltage wave materially affect the root-mean-square current and therefore the heat delivered to the weld. The heat delivered to the weld is directly proportional to the resistance of the work, the time of application of the current, and to the square of the root-mean-square current. The magnitude of the variations in heat caused by small angular changes in the phase of closure can best be shown by a typical example as given in table I of this discussion. These results apply to a welder having a 0.30 power factor.

In this table, the angle of closure is measured from the power-factor angle. Positive angles are later in time corresponding to closure $^1/_{2,160}$ second too late on the wave. Negative angles correspond to closure too early by the same amount. In each case it is assumed that the contactor opens precisely at the current zero, so that the variations shown are entirely due to erratic closure of plus or minus ten electrical degrees. The heat delivered resulting from closure at the power-factor angle is taken as 100 per cent as a point of reference for each case. These variations, which amount to more than 50 per cent in the heat for a one-half-cycle spot length and 16 per cent for one-cycle spot length, are too large to be tolerated in many applications. In view of these variations, we would like to ask the author what precision, as regards phase angle of closure, can be maintained in practice with this device.

Furthermore, if the timing is adjusted for an odd number of half cycles, the resulting residual flux in the welding transformer may cause excessive sparking when the welding electrodes are lifted unless the shunt capacitor, which is connected across the primary of the welding transformer, can absorb this energy or unless antipolar starting be used.

Table I

Spot Length	Closure Angle, Measured From Power Factor (Degrees)	Per Cent Heat
One-half cycle	+10	73.1
One-half cycle	0	100
One-half cycle	−10	128
One cycle	+10	92.5
One cycle	0	100
One cycle	−10	109

The oscillograms shown in figure 11 show a small discontinuity in the primary current wave such as might occur if the contactor bounced and arced, or started to drop out. Will the author please explain this discontinuity? Unfortunately, the scale of these oscillograms is so small that variations of current of a magnitude which might give unsatisfactory welding would be imperceptible.

One disadvantage of the motor-driven timer is that there is a variable time delay after the closure of the initiating switch before TR_1 closes, starting the cyclic process. The time delay depends upon the phase of closure of the initiating switch with respect to the position of the cam actuating TR_1. It may therefore be as great as one complete cycle of operation, or in other words the time delay may be as great as the time required to make a spot weld. In high speed production, allowance would have to be made for this maximum time delay, which may in some cases limit the rate of production. Electronic spot-welding control eliminates this variable time delay. In addition, electronic control permits the use of phase control, giving a smooth variation in heat without resorting to taps on the welding transformer. In many applications phase control has proved to be a most desirable feature. Contactor control eliminates the possibility of using phase control.

Because of its relative inflexibility and inaccuracy, this control is limited to certain spot-welding applications. Good seam welding requires more precise timing such as is obtained electronically because of the possibility of cumulative transients during the seam, so that the error may be much greater than the error of any single spot. Furthermore, because of life considerations, a contactor would not give adequate life on most production seam-welding jobs. Repeat operation is limited to the number of gear changes available. A recent control has been developed which provides the equivalent of at least 60 gear changes on this unit and maintains electronic accuracy. The cost of incorporating this large number of gears to make the device equivalent in flexibility with this new electronic control would probably be prohibitive. On many production jobs this great flexibility is not required. However, frequently production jobs change and it is obviously an advantage if the control is adaptable to any such possible change.

In closing, we would like to ask the author to present some actual life figures in terms of the number of operations possible. Operation in terms of time is of little value unless rate is also specified. Electronic controls have been in use for many years and the number of operations possible without changing tubes soars into astronomical proportions—hundreds of millions.

F. H. Roby: A discussion of the pape[1], "Simplified Precision Resistance Welder Control," prepared by D. G. Levoy and G. W. Garman, is typical of an attitude that has prevailed whenever magnetic contactors are mentioned with reference to accurate or precision control of a resistance weld. The author has no quarrel with electronic equipment. Such devices have, without question, performed to specification. Nevertheless, it is a fact that the least expensive standard electronic control costs more than many users are willing or able to pay. The result has been the continued use of nonsynchronous devices for applications where their inherent errors are objectionable. Since a device of the type described in technical paper 39-65 can be manufactured and sold for a price much less than that of the equipment now available, precision control can now be applied to more jobs.

It is true that an electronic tube is a simple switching device when judged by an electrical engineer. However, the typical electrician, maintenance man, or operator finds any electronic device something of a problem. When not understood, the very characteristics which seem simple to the engineer result in complications to the individual charged with using and maintaining the equipment. Because he is usually acquainted with mechanical motions and magnetic forces, a system composed of several such parts is readily understood and, therefore, simple. It will require many years of instruction to dispel this attitude.

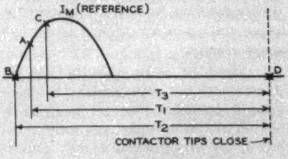

Figure 1

Precision welding cannot be satisfactorily accomplished in the face of widely fluctuating line voltages. It has been stated by experienced users that ten per cent variation may seriously affect results. Standard contactors are equipped with magnet coils designed to operate at 15 per cent under- and 10 per cent overvoltage. Welding contactors are equipped with intermittent duty coils that are even less affected by undervoltage.

Experience has indicated that a contactor can be closed more consistently than opened because of the stronger forces and faster accelerating rates involved. Using this experience as a basis, early investigators provided one normally open and one normally closed contactor operated in sequence respectively to close and open the circuit. Thus both functions were accomplished by energizing a magnet.

Accuracy in closing is improved considerably by utilizing the "flat operating characteristic" of a magnetic contactor. As pointed out in the paper, the closing speed of a contactor is influenced to a considerable degree by the transient drawn in its operating coil. Assuming a normal wave form, the pull of the magnet is proportional to the product of the pole face area and the *flux density squared*. Under transient conditions the flux in the iron circuit may be as much as *double* normal value. Since the area of the pole face remains constant, the pull could be as much as four times normal. It is not difficult to believe that the closing speed of the contactor is changed accordingly.

Refer to figure 1 of this discussion for a graphic explanation of the "flat operating characteristic." Assume that the magnet is energized at point A on the coil current wave. A transient wave form will be obtained and the magnet pull which results will cause the contact tips to touch at point D. Time T_1 is consumed in closing. However, if the coil circuit is energized at point B no transient exists and the lessened pull causes the contactor to require T_2 time for closing thereby compensating for the early coil energization. Likewise, energizing the coil circuit at point T provides a greater transient and increased pull. Closing time T_3 results, thus compensating for the late energization in the magnet circuit. Proper adjustment of the closing contactor is obtained when its magnet is energized at point A (the middle of a 60-electrical-degree band). The neon tube indicator assists in obtaining the correct setting.

Under perfect operating conditions there will be small variations in the closing point. However, large-scale oscillograph records indicate that these are not enough to affect results in most instances. The oscillogram reproduced as figure 11 is not enlarged enough to serve this purpose but it does indicate a consistent wave form which, in turn, is the result of consistent operation. The small "break" in the oscillograph trace is caused by contact tip bounce in closing. This bounce is the result of energy stored in the contact tip and is characteristic of all magnetic contactors.

The tips of the opening contactor are usually set to part at a point approximately 30 electrical degrees before current zero. Because the current value is rapidly approaching zero, no appreciable arcing can be noticed. Assuming a ±15-electrical-degree error under the least favorable operating conditions, the power factor of the load would be required to increase from 50 per cent to 80 per cent to cause arcing. A decrease in power factor would have no effect other than to cause the tips to open slightly higher on the current wave, thus drawing a little more arc. The adjustment is not critical.

An odd one-half cycle is included in the time required for the timer cam shaft to make one revolution thus giving the effect of antipolar starting.

The elimination of a mechanical clutch in the timer system through the use of an "initiating relay" has introduced a variable time delay in starting the weld. This delay is objectionable only when extremely high production rates are involved. Since most precision jobs are necessarily of low production because of care in mechanical alignment and frequent dressing of electrodes, this objection is not a valid one. It would be possible to employ a clutch in the system without materially affecting the accuracy of the timer but the advantage gained does not warrant the introduction of an additional mechanical link.

The authors of the discussion are under the mistaken impression that mechanical equipment is to be recommended for seam welding. A satisfactory seam weld may require from 10 to 16 interruptions per inch. Assuming a feed rate of 90 inches per minute, the equipment would be required to operate at the rate of from 900 to 1,440 complete cycles per minute. Mechanical contactors should be limited to about 400 operations per minute since their mechanical life is determined somewhat by the speed at which they operate. Thus seam welding is not considered a suitable field of application.

Heat control is of value on some applications where extremely fine increments of adjustment are desired. The many applications falling outside of this group represent a field broad enough to justify the development of a precision controller which does not include the heat-control feature.

Magnetic contactors designed for welder service have long since been perfected to the extent that service in excess of 15,000,000 operations is obtainable without prohibitive maintenance. Under severe operating conditions automobile plants use the same equipment for several programs. In lower-production shops the contactors will serve indefinitely. It is only natural that concerns recently associated with electronic devices are not acquainted with current contactor performance data.

A fully automatic welder control unit is practical without considering 60 gear changes. Flexibility of the type mentioned would probably be necessary in connection with a laboratory installation but normal production facilities would involve only six or eight.

Experience over the past 18 months has demonstrated that many difficult welding jobs can be readily accomplished with the equipment under discussion. It is not intended that it be used to the exclusion of all other types of control. Instead, it should be considered as a supplement to control now available, bringing into reach of the average user the advantages of precision control.

Load Ratings of Cable

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Synopsis: Operating and test data concerning the maximum safe loading of impregnated-paper-insulated lead-covered cable are presented. The results of the study may be summarized as follows:

1. The occasional operation of cable at higher temperatures than are permitted by present temperature rules effects considerable economy.

2. During emergencies, temperatures of 5 to 35 degrees centigrade (depending on kind of cable) above those permitted by the rules are safe for the insulation.

3. For extra-high-voltage solid-type cable, void formation in insulation and expansion of lead sheaths may limit allowable temperatures and temperature ranges.

4. Cracking of lead sheaths due to reciprocating cable movement into manholes may limit the temperature range for usual daily loading. Limitation is more severe for longer conduit lengths up to 500 feet, but changes little with increase from 500- to 1,000-foot lengths.

5. Cracking of sheaths in manholes due to cable movement may be reduced by improving manhole conditions.

6. For many cables a balanced design requires a lead-alloy sheath that gives increased resistance to effects of cable movement and of internal pressures.

7. Continuous field temperature surveys are essential to efficient use of large conduit and cable systems.

8. Only a small fraction of the cable ever operates at the higher temperatures.

9. Data on center empty-duct temperatures and on average heat losses over 24-hour periods give satisfactory results in heat calculations.

10. Other practices which increase load ratings are the use of different ratings for various periods of the year, the replacement of poor soil in special cases, and the use of extra-large conductors in warmer conduits.

THE PURPOSE of the investigations covered in this paper has been to obtain the most efficient use of impregnated-paper-insulated lead-covered cable and accompanying conduits and manholes. Using considerable recent data, the author has attempted to cover in one study all important factors affecting load ratings.

With the sharp improvements during the past 20 years in the quality of cable and joints and in their installation and maintenance, there has been an accompanying sharp decrease in the rate of failures; for example, during the past 15 years in Chicago the total rate of failures of 12-kv lines consisting of 500,000-circular-mil three-conductor 13-kv belted cable has decreased from about 40 failures to 5 failures per hundred miles per year. At the same time, the quality of terminal equipment has improved greatly. These trends show a sharp decrease in the frequency with which low- or high-voltage circuits are being subjected to loads approaching their maximum ratings on account of outages of parallel circuits.

The outgrowth of these improvements, together with new knowledge on the effects of high temperatures on cable and joints, has been the definite establishment during 1930–37 of emergency load ratings for cable in Chicago operating from 120 volts to 132,000 volts. Generally emergency ratings determine, with the exception of the 120-volt a-c network cables, when additional underground circuits must be installed.

If, for example, the maximum rating of 500,000-circular-mil three-conductor 13-kv cable is increased by 15 per cent by allowing temperatures to exceed occasionally the maximum given in AIEE standards,[1] then on a growing system large reductions in the immediate and future investments needed for additional circuits and conduit will be obtained. With the increases in copper losses, in dielectric losses, and in maintenance costs at the higher loadings taken into account, the net reduction in total annual investment and operating charges per kilovolt-ampere mile for installed cable and conduit would be 9 per cent, assuming that the higher temperatures did not cause the cable life to be less than would be required from the standpoint of obsolescence and system changes. The increased ratings might be obtained on future lines without exceeding the maximum temperature given in the standards by using 650,000- instead of 500,000-circular-mil cable, but the total annual charges per kilovolt-ampere mile would then be reduced only one per cent.

On account of the use of items having standard ratings, terminal equipment frequently can safely carry more load than the cable connected to it. Increased load ratings of cable, therefore, produce substantial reductions in the cost per kilovolt-ampere carried through such equipment. An important over-all advantage is obtained by higher loading in reducing the space required in the streets for conduits and at stations and substations for terminal facilities.

Various aspects of the problem of load ratings are discussed in four divisions of the paper:

Part I—Limitations due to the insulation

Part II—Limitations due to the sheath

Part III—Heating characteristics of conduits

Part IV—Principles and methods of calculation

Part I. Limitations Due to the Insulation

Since 1925 the maximum allowable operating temperature for solid-type impregnated-paper insulation has been 90 degrees centigrade minus the rated voltage in kilovolts, with a maximum of 85 and a minimum of 60 degrees centigrade. For multiple-conductor belted cable the rated voltage between conductors, and for shielded and single-conductor cable the voltage between conductor and sheath is used. This rule was apparently based on the following: above 85 degrees the rate of deterioration in mechanical strength of the paper increases rapidly with increasing temperature; as the operating voltage increases, the stress, and hence the probability of serious ionization, increases.

When this AIEE temperature rule was first established (then it was 85 degrees minus the rated voltage), ionization had not been recognized, and means to measure it were not available. At that time the reason for the decrease in temperature with increase in voltage was that the dielectric losses for high-voltage cables increased rapidly to very high values with temperature and increased the possibility of cumulative heating. For over 12 years, impregnating compounds which give small dielectric losses have been in use.

Paper number 39-29, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 22, 1938; made available for preprinting December 15, 1938.

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The author is very grateful for the assistance received in the preparation of this paper and wishes to make the following specific acknowledgments: part I, H. A. Adler, C. B. Betzer, and O. B. Turbyfill; part II, M. C. Hedges, H. J. Plumley, and F. O. Wollaston; parts III and IV, C. A. Bauer and D. D. Nagley.

[1] For all numbered references, see list at end of paper.

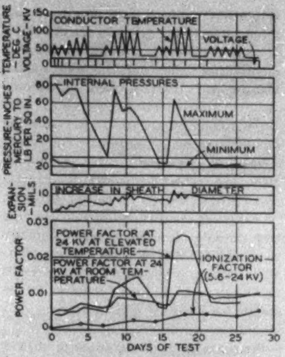


Figure 1. Aging test of sample F (three-conductor 13-kv cable)

The rate of deterioration of paper at various temperatures has been investigated by Massachusetts Institute of Technology,[2,3,4] H. W. Fisher and R. W. Atkinson,[5] F. M. Clark,[6] and others in individual tests varying from ten days to 17 months. The results are in agreement in showing that heat above 85 degrees centigrade causes an appreciable decrease in tensile and tearing strength of paper, but that the dielectric strength is not influenced much until the mechanical strengths have been almost destroyed. The main consideration, therefore, is the influence of heating on the ability of the cable to withstand removal and reinstallation without material lowering of the dielectric strength of the cable.

Experience, supplemented by careful examinations, has shown that the mechanical properties of the insulation of cable that has been in service in Chicago up to 40 years are sufficient to permit withdrawal of the cable and its successful operation after reinstallation, except in the few cases where the maximum temperatures have substantially exceeded the limits given herein for emergency operation. The cable, however, is gradually scrapped before the useful life of the insulation has expired due to (1) refitting to other locations on the system, (2) elimination of sheath damages, and finally (3) obsolescence. Serious deterioration due to ionization has been found mainly in poor high-voltage solid-type cable made 12 or 15 years ago; this has been due mainly to factors other than operating temperatures. Evidently the maximum use of the cable was usually not being obtained, because the loading was insufficient.

The temperature rule makes no provision for increased loads in emergencies for periods of one or two days. Therefore, sufficient carrying capacity needs, under the rule, to be installed so that in the worst expected emergencies the hottest part of the cable in the circuits remaining in service will not exceed the prescribed temperature. Obviously, then, almost all of the cable will be operating far below the temperature limits for the great majority of the time. For example, for the 1,030 miles of three-conductor cable operating at 12 kv in Chicago in 1937, 95 per cent operated up to a maximum of 60 degrees or less, 4 per cent up to 70, and the very maximum was 82 degrees centigrade or only 5 degrees above the temperature given by the rule. For each length of cable, the average temperature for the year was usually 15 or 20 degrees less than the annual maximum. Due to the new emergency ratings, it is expected that in the future the temperatures will range to higher values.

It is, therefore, economical to take some chances of exceeding the limits in AIEE standards for a few hours or so during an occasional emergency in order to utilize efficiently the entire system, with the understanding that it might result in shortening seriously the life of the insulation of one or two per cent of the cable.

Tests[4] have shown that the wood-pulp paper available for cable insulation in 1926 deteriorated in mechanical strength less than about five and ten per cent in 50 hours at, respectively, 120 and 140 degrees centigrade and showed no change in electrical strength. Tests[5] reported in 1921 showed that Manila papers in use at that time deteriorated with heating only slightly more than those tested in 1926 and much less than those tested in 1905.

Wood-pulp paper, which began to replace Manila paper about 1924, is used almost entirely now. Marked improvements have been made in it for electrical insulation since 1926. The tearing and dielectric strengths increased by about 50 per cent. In addition, improvements have been made in the application of the paper. Thus, paper in recently-made cables may be permitted much greater percentages of depreciation due to heating than paper in earlier cable before the cable becomes unfit for reinstallation. The available information indicates that recently-made paper may be safely operated about 15 degrees centigrade higher than 1920 paper and about 30 degrees higher than 1905 paper.

In general, the subsequent discussion on maximum allowable temperature refers to the temperature for the cable in the

Table I. Tests on 500,000-Circular-Mil Three-Conductor Belted 13-Kv Cable With Pressure-Tight Potheads

Sample	*A*	*B*	*C*	*D*	*E*	*F*	*G*
Year made	1924	1925	1928	1937	1937	1937	1938
Service before test (years)	13	12	9	None	None	None	None
Compound	Petrolatum	Petrolatum	Rosin-mineral oil	Rosin-mineral oil	Mineral oil	Mineral oil	Rosin-mineral oil
Power factor at 13 kv and room temperature							
Initial	0.0036	0.0077	0.0042	0.0029	0.0026	0.0043	0.0040
After 60- and 80-deg cycles	0.0052	0.0069 (0.0095)	0.0043	0.0077 (0.0086)	0.0031 (0.0033)	0.0060 (0.0071)	0.0049
After 100-deg cycles	0.0057	0.0085	0.0049	0.0099	0.0037 (0.0048)	0.0074 (0.0089)	0.0067 (0.0076
After 115-deg cycles			0.0048 (0.0050)	0.0218	0.0074 (0.0087)	0.0093 (0.0109)	0.0073
After final 60-deg cycles			0.0047	0.0089	(0.0085)	0.0085	0.0062
Power factor at 24 kv and room temperature							
Initial	0.0098	0.0086	0.0043	0.0032	0.0027	0.0046	0.0055
After 60- and 80-deg cycles	0.0122 (0.0199)	0.0137 (0.0272)	0.0088	0.0106 (0.0135)	0.0031 (0.0035)	0.0061 (0.0072)	0.0049
After 100-deg cycles	0.0120 (0.0183)	0.0181 (0.0182)	0.0094	0.0129 (0.0155)	0.0044 (0.0074)	0.0078 (0.0089)	0.0074 (0.0077)
After 115-deg cycles			0.0099 (0.0117)	0.0302	0.0138 (0.0153)	0.0100 (0.0109)	0.0110 (0.0207)
After final 60-deg cycles			0.0095	0.0113	0.0137 (0.0168)	0.0105	0.0081
Power factor at 24 kv and 60 degrees centigrade							
Initial	0.0137	0.0563	0.0084	0.0038	0.0026	0.0041	0.0032
After 60- and 80-deg cycles	0.0153	0.0400	0.0100	0.0061	0.0031	0.0050	0.0043
After 100-deg cycles			0.0107	0.0069	0.0035	0.0065	0.0045
After 115-deg cycles			0.0125	0.0090	0.0062	0.0079	0.0065
After final 60-deg cycles			0.0123	0.0083	0.0101	0.0078	0.0063

NOTES: Figures in parentheses are maximum values reached in the test period. Samples *A* and *B* failed at low temperatures after the 100-degree cycles.

Table II. Tests With Pressure-Tight Potheads—Maximum Pressures

Sample	A	B	C	D	E	F	G
Maximum pressure (pounds per square inch)	6	92	85	58	110	83	107
Cycle number	10	3	1	2	1	1	7
Temperature at which maximum pressure occurred (deg C)	100	75	58	60	60	60	99

hottest conduit section along a circuit during an emergency of a day or two every year or few years, it being known that the temperatures of the remaining conduits will usually be 5 or 10 or even as much as 20 degrees centigrade lower, and that the usual cable temperatures will be materially less than the maximum permitted by existing rules in the United States.

Limitations for Low-Voltage Cable

For cable operating at 7,500 volts and less, the electrical stress is so low that the deterioration of the insulation from the standpoint of electrical strength at temperatures up to limits of 82$^1/_2$ to 85 degrees centigrade set by the temperature rule is practically impossible. Since the stresses are low, there is no problem of dielectric losses.

Large amounts of cable have been in operation for 25 years or more at about 120 volts in Chicago and elsewhere, and much of it has operated at temperatures up to 105 degrees centigrade or more without serious effects on the insulation. Due to effects of the World War, carrying capacity was inadequate in Chicago for five years. During this five-year period, about 15 per cent of the two-conductor concentric cables were operated at least once a year at conductor temperatures above 125 degrees centigrade, and about seven per cent exceeded 125 degrees regularly with temperatures of 9 to 25 degrees centigrade less for the outer insulation. Some of the cable operated with copper temperatures over 200 degrees. Although the inner tapes in the insulation of some of this cable were greatly weakened, the outer tapes were almost invariably in fair condition and did not preclude successful use on reinstallation. During the past 35 years, several per cent of the 1,500,000-circular-mil single-conductor cable has operated at times at temperatures of 110 to 135 degrees centigrade without interfering with its continued operation or with its reuse after removal.

Proposed standards for this country set maximum hot-spot copper temperatures of 95 and 105–115 degrees centigrade, respectively, for continuous and emergency operation of transformers of any voltage. Even higher temperatures for emergencies have been proposed by one manufacturer[7] for transformers having an inert gas over the oil and by some operators for all transformers.

All factors considered, it seems that a reasonable limit for the insulation of low-voltage cable for emergency operation is 105–120 degrees centigrade. For so-called continuous loading, the limit should be 85–95 degrees, depending on how often and long the cable operates at the maximum temperatures.

A somewhat similar conclusion was discussed[8] before the AIEE in 1921 and was generally acceptable for practical use. The necessity for emergency operation was admitted, but some of the discussers were reluctant to provide for it in the rules. It appears that the insulation of cable manufactured during at least the past 12 years can successfully withstand operation up to the suggested limits.

Limitations for Three-Conductor 7.5- to 15-Kv Cable

Half of the high-voltage cable used in this country is three-conductor belted cable operating at 7.5 to 15 kv. For three years the Commonwealth Edison Company has been conducting accelerated aging tests on such cable to determine the effects of rare overloads on the cable on its 9- and 12-kv systems. Fifteen samples representing new cable and cable removed from the system and made by various manufacturers have been tested to the time of writing. The effective test length was usually a little over 100 feet and ended near the crotch in each pothead.

The first eight samples had test terminals filled with heavy compound exposed to open air. These terminals were not effective in preventing migration of the filling compound into the cable and did not allow the development of high pressures and vacuums in the cable.

Conditions in the middle of long lengths of cable between manholes were simulated in tests with pressure-tight terminals on the next seven samples. The amount of filling compound was reduced as much as feasible, that is, to 0.4 gallon per terminal. The terminals were filled completely with a heavy mineral oil as used in solid-type cable.

The test consisted in the application of a continuous overvoltage with superimposed heat cycles. The test voltage has been 24 kv, three-phase, 60 cycles for all samples except two. The heating was done by induced current through the three conductors, each conductor forming a one-turn short-circuited secondary on iron-core transformers placed around the samples. Heating current was applied daily for eight hours, except on Saturdays and Sundays, with cooling in open air. The nominal maximum copper temperatures began at 60 and increased as the testing proceeded to 115 degrees centigrade. After each set of high temperature cycles, one or more cycles of heating to 60 degrees were introduced to determine the effects of the higher temperatures on the normal operating characteristics, 60 degrees representing a high value of normal maximum temperatures. The duration of the aging tests was usually four weeks.

During the aging tests, measurements were made daily of power factors at test

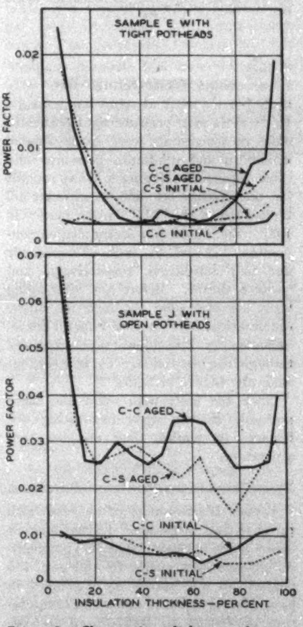


Figure 2. Changes in radial power factor at 60 degrees centigrade for 13-kv cable in accelerated aging test

C-S—Conductor to sheath
C-C—Conductor to conductor

Table III. Tests With Pressure-Tight Potheads—Pressures and Sheath Expansion

Sample	Start	After 80-Deg-C Cycles	After 100-Deg-C Cycles	After 115-Deg-C Cycles
Maximum pressures during 60-deg-C cycles*				
A	0	− 4		
B	42	− 5		
C	85	8	8	6
D	58	18	3	−10
E	110	11	5	− 4
F	83	1	−12	−16
G	57	4	− 5	− 5
Minimum pressures during 60-deg-C cycles (inches of mercury)				
A	−27	−17		
B	−10	−17		
C	−18	− 4	− 2	− 2
D	−30	−25	−28	−28
E	−20	−23	−27	−22
F	−13	−18	−19	−19
G	−18	−22	−24	−24
Increase in cable diameter at room temperature (mils)				
A		2	2	
B		6	12	
D		5	12	15
E		12	18	24
F		4	7	8
G		2	6	8

* Pressures (positive values) and vacuums (negative values) are, respectively, in pounds per square inch and inches of mercury.

voltage at room and elevated temperatures. Power factor-voltage tests were made twice a week at room temperature. In the tests with pressure-tight potheads, daily measurements were made also of maximum and minimum pressures and cable diameters. Figure 1 shows records for one sample of 500,000-circular-mil three-conductor 13-kv cable made in 1937. Such cable has sector-shaped conductors, $^8/_{64}$ and $^5/_{64}$ inch of conductor and belt insulation, respectively, and $^8/_{64}$-inch sheath. Before and after aging tests, samples were obtained for visual examinations and for power factor measurements of individual tapes radially through the insulation. Typical test results are shown in figure 2.

Since the tests with open and tight potheads showed some interesting differences in results, they are discussed separately.

TESTS WITH PRESSURE-TIGHT POTHEADS

Table I is a summary of the tests with pressure-tight potheads. In the four new cables made recently, ionization increased during the load cycles to 100 and 115 degrees as was shown by increases in power factor at 24 kv and room temperature of 0.0063 to 0.0270 and by development of some carbon in the compound at the center of the cable. However, in subsequent cycles to 60 degrees considerable recovery in power factor occurred. Similar cables made 12 or 15 years ago have sometimes shown similar changes in normal operation. Three of the tested samples have been placed in service at 12 kv to verify over a period of years the conclusion that they will still operate satisfactorily.

One of the test samples was from 1928 cable which had been in service for about nine years, during which considerable deterioration had occurred. Although some further deterioration developed in the aging tests, the changes were not so great as those in the four new cables.

Two cables containing petrolatum, made in 1924 and 1925, had been in service for 12 to 13 years. Ionization increased considerably in both of these cables in load cycles to 60 and 80 degrees, and both failed at low temperatures after the end of the series of 100-degree cycles.

The outstanding point in these data is that, although ionization increased in all samples, the changes in power factor at rated voltage were usually small, indicating that, even if void spaces are created, little ionization occurs at normal voltage. In general, the first cycle of each temperature step seemed to have the greatest effect. An exception is the maximum power factor at 13 kv of 0.0218 for sample *D*, which occurred after cooling from 115 to 21 degrees. Such severe temperature changes will never occur in service. Furthermore, this sample recovered in subsequent 60-degree cycles so that the power factor decreased to 0.0089.

The changes in solid losses (losses in impregnated tapes only) were of minor importance compared with the increases in losses due to gaseous ionization. The power factors at room temperature and 5.6 kv increased during aging by 0.0006 to 0.0077. As table I shows, the changes in power factor at 60 degrees were moderate. Also, the radial power-factor curves obtained before and after aging showed small changes except near copper and lead. The changes in the average of the radial power-factor curves before and after aging ranged from a decrease in power factor of 0.0127 to an increase of 0.0039.

Maximum pressures as high as 110 pounds per square inch were observed. As indicated in table II, the maximum pressures did not occur at the highest temperatures. Four of the samples developed maximum pressures in the initial 60-degree cycles; none in the 115-degree cycles. The maximum pressures were apparently determined by the increase in maximum temperature over the highest previous temperature level rather than by the absolute values of the maximum temperature.

The maximum pressures decreased sharply in succeeding heat cycles at the same temperature due to expansion of the sheath. Table III shows how the maximum pressures decreased in the 60-degree cycles at various stages of the test. For the four new cables, *D*, *E*, *F*, and *G*, the pressures did not become positive at any time during the series of final 60-degree cycles, although for cable *E* this final series lasted four weeks. The minimum pressures showed little change during the aging tests in spite of the sheath expansion.

These pressure data suggest that emergency loading causing temperatures up to 100–115 degrees are not likely to produce much higher pressures than occur in the early stages of usual operation, especially since the cable will have probably carried a good load prior to the emergency. The data further indicate

Table IV. Tests on 500,000-Circular-Mil Three-Conductor 13-Kv Cable With Open Potheads

Sample	*H*	*I*	*J*	*K*	*L*
Year made	1923	1924	1927	1935	1936
Service before tests (years)	13	12	5	None	None
Compound	Petrolatum	Petrolatum	Rosin-mineral oil	Rosin-mineral oil	Mineral oil
Maximum temperature reached (deg C)	117	107	118	120	111
Power factor at 13 kv and room temperature					
Initial	0.0062	0.0047	0.0050	0.0052	0.0037
Maximum	0.0098 (103)	0.0138 (107)	0.0080 (118)	0.0099 (83)	0.0052 (111)
Final	0.0082	0.0122	0.0080	0.0084	0.0048
Ionization factor (5.6-24 kv) at room temperature					
Initial	0.0040	0.0086	0.0042	0.0006	0.0041
Maximum	0.0088 (103)	0.0202 (62)	0.0127 (118)	0.0195 (104)	
Final	0.0028	0.0024	0.0060	0.0030	0.0003
Power factor at 24 kv and 60 degrees centigrade					
Initial	0.0353	0.0159	0.0154	0.0078	0.0039
Maximum	0.0390 (103)	0.0588 (107)	0.0275 (118)	0.0277 (104)	0.0242 (111)
Final	0.0370		0.0221	0.0137	0.0214

NOTE: Figures in parentheses show maximum temperature in degrees centigrade after which power factor shown occurred.

the overloads will not make the vacuum after cooling more severe.

Tests With Open Potheads of Cables Made After 1920

In contrast to the tests with pressure-tight potheads, none of the cables tested with open potheads failed or showed signs of approaching instability. No high pressures or low vacuums occurred in these samples. Table IV summarizes the test results. Although the heat cycles produced some increases in ionization factor, the final values were low. The maximum ionization factors did not occur in most cases after the highest temperature steps. It appears that the decrease in viscosity of the pothead compound at the higher temperatures favored migration of compound and reimpregnation of cable insulation near the ends of the samples. For sample *L* the ionization factor was highest at the start.

The solid losses increased in all cases. In sample *L* the increase in power factor was mainly caused by the unusual migration of asphaltic compound from joints in the test leads for at least 20 feet into the test length. For other samples, the increases in power factor were probably caused mainly by deterioration at the high temperatures. The increases during aging in the average of the 60-degree power factors of the individual tapes varied from 0.011 to 0.115 for four samples. For the fifth sample the average of the radial power-factor curves after aging was 0.022 less than before aging, but this difference was due probably to longitudinal nonuniformity of the quality of this sample of used cable.

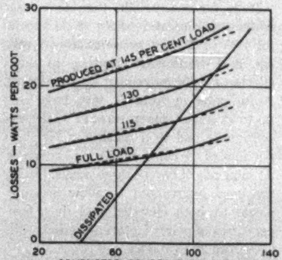


Figure 3. Thermal stability of used 500,000-circular-mil three-conductor 13-kv cable

Solid curves—Sample *J*, rosin-mineral compound cable made in 1927

Dashed curves—Sample *H*, petrolatum-compound cable made in 1923

Table V. Aging Tests of an Old Rosin-Impregnated Three-Conductor Cable

Approximate Maximum Temperature (Deg C)	Number of Cycles	Power Factor at Room Temperature and 15 Kv		Ionization Factor (5.6–20 Kv)		Power Factor at Elevated Temperature	
		Start	Maximum	Start	Maximum	Start	Maximum
60	4	0.0095	0.0167	0.0107	0.0121	0.224	0.224
80	5	0.0112	0.0309	0.0075	0.0173	0.353	0.388
90	5	0.0131	0.0148	0.0163	0.0216	0.429	0.479
60	5	0.0142	0.0146	0.0208	0.0184	0.209	0.238
100–110	5	0.0145	0.0198	0.0184	0.0330	0.550	0.636
60	2	0.0187	0.0185	0.0326	0.0319	0.334	0.334

These data indicate that the changes in solid losses, even where considerable migration of asphaltic compound into the cable occurred, are not serious enough to cause failures in service, except possibly in some rare instances.

Tests With Open Potheads of Old Rosin Cables

Aging tests were made of three samples of cable made prior to 1920. They had round conductors and $^{12}/_{64}$- and $^{9}/_{64}$-inch rosin-impregnated conductor and belt insulation, respectively. One sample of 250,000-circular-mil cable failed in the first cycle after having been subjected to 24 kv for $2^{1}/_{4}$ hours and having reached a copper temperature of about 60 degrees. Another sample of similar cable withstood 36 days at 15 kv but showed considerable increases in power factor and especially in ionization factor after the first 80-degree cycle, as shown in table V.

The third sample, having number 4/0 conductors, showed signs of serious instability in the first 60-degree cycle at 15 kv, the power factor at elevated temperature rising from 0.20 after six hours of heating to nearly 0.70 after about seven hours of heating. The voltage was therefore reduced to 12 kv. The cable failed at elevated temperature in the fourth 60-degree cycle.

The two failures in this group of tests were apparently caused by thermal instability due to high dielectric losses. The impregnation of these cables was poor. The older cable, as represented by these two samples, is not considered suitable for reinstallation on the 9- and 12-kv systems, while somewhat similar cable made later (1912–18) has been so satisfactory that it is being reinstalled.

Thermal Stability

The thermal instability just discussed raises the question of instability due to overloads in service. Figure 3 shows thermal stability diagrams for two cables removed from service. The curves represent the total watts generated in the cable at various loads at operating voltage. The straight line shows the total watts which can be dissipated assuming at the start a summer duct temperature of about 40 degrees, no load for the cable in question, and usual loading for the other cables in the conduit. The intersections of the curves with the line give the copper temperatures reached for one-day emergency loads. In deriving the curves, the author has in each case used unfavorably high values of power factor in order to take into account the effect of deterioration due to aging during the overloads.

The diagrams show that such used cables are thermally stable even at 30 per cent overload. The approximate copper temperatures reached at full load, 15 per cent overload, and 30 per cent overload, respectively, are 77, 90, and 110 degrees. Lower duct temperatures would move the straight line to the left, and correspondingly higher loads would be permissible. It is of interest that, even if a power factor of about 0.20 is reached at a temperature of 115 degrees, the dielectric losses on these cables would be only seven per cent of the copper losses.

Assuming a base duct temperature of about 40 degrees, even old rosin cables would probably be thermally stable at 30 per cent overload. Since the dielectric losses were calculated from the average power factor of about 100 feet of cable, it is possible that in localized spots much higher dielectric losses may develop, especially for some of the older cables.

Joints

As the result of high loading, asphaltum-base or petrolatum joint-filling compound may become so fluid as to migrate in large amounts into the cable insulation, thereby causing increased insulation losses; but this does not seem serious, especially since migration is usually limited to cable in the manhole which is subjected to lower temperatures. A further result is pressures or vacuums in the joints which may cause serious bulging or collapsing of the joint sleeves. Most lead joint sleeves, especially for the larger cables, have been too weak mechanically. For Chicago 13-kv 500,000-circular-mil joints, the new practice is to use only low-loss varnished cambric insulation as applied insulation instead of insulating tubes, and to use a circular sleeve of calcium-type lead alloy, $4^3/_4$ inches inside diameter and $^9/_{64}$ inch thick, instead of a $5^1/_2$-inch plain lead sleeve which was ridged to provide an air space at the top. So far, the practice is to continue using an asphaltum-base compound for filling.

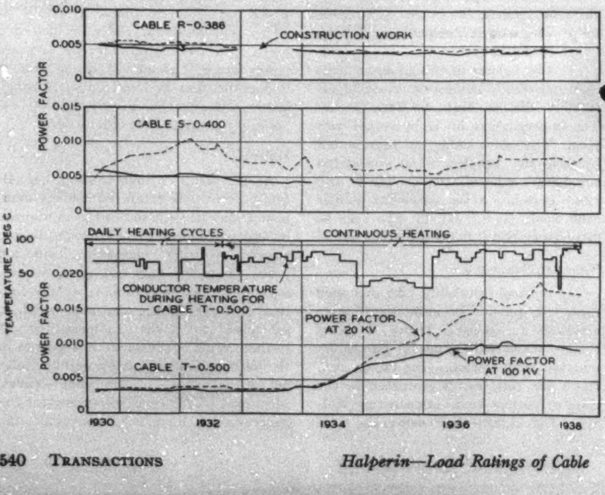
SUMMARY

The outstanding effect of overloads up to 115 degrees centigrade upon multiple-conductor belted cables made since 1923 was an increase in ionization; but for only two old petrolatum cables was this effect serious enough to cause failure at 24 kv. At rated voltage the changes in ionization factor were small, and considerable recovery effect was noticeable during periods at moderate temperatures following overloads. It may, therefore, be expected that the effects of emergency loading to temperatures of 100–115 degrees will not be serious and that certainly limits of 90–100 degrees are conservative.

Power factors at normal operating temperatures should not be expected to increase much due to overloads, except for the cable adjacent to joints. In general, this effect should not be expected to shorten the life of the cable.

For copper temperatures up to at least 115 degrees centigrade, thermal instability is not a factor of danger, except for badly deteriorated or old rosin insulation.

Figure 4. Power factor of experimental oil-filled cable at 60 degrees centigrade



Temperature Limits for Solid-Type 69-Kv Cable

The maximum permissible temperature for the insulation of single-conductor 69-kv cable has been based in Chicago largely on accelerated aging tests and a large accumulation of operating data. The joints in service are filled with a thin oil. Halperin and Betzer[9] showed that cables of good quality withstood for as long as seven weeks, without appreciable change, tests at $2^1/_2$ times normal operating voltage and daily temperature ranges of about 35 degrees to maximum copper temperatures of 65 degrees centigrade. Insulation thicknesses were $^{40}/_{64}$ to $^{48}/_{64}$ inch. Cables of the poorest qualities (made in 1926), which are now in service, showed considerable increase in power factor in a few days of such testing. However, these changes were not sufficient to cause failure at normal voltage, and such poor-quality cables constitute only a very small percentage of the 66-kv system.

Emergency ratings for the 66-kv lines in Chicago have for a few years been based on temperatures of 60–65 degrees centigrade, depending on the quality of the cable insulation, and on a maximum allowable daily range in temperature of 18 degrees. This limiting range is necessary for the insulation of cable now in service because (*a*) deterioration of the insulation occurs almost entirely by ionization in voids produced during cooling, and (*b*) the life of the cable may be shortened by radial expansion of the sheath as discussed later. For the year 1937, the maximum temperatures were 45, 50, and 55 degrees centigrade or less for, respectively, 75, 90, and 96 per cent of the lengths; and the very maximum was about 62 degrees.

Temperature Limits for the Insulation of Oil-Filled Cables

An experimental installation of oil-filled cable in underground conduit in Chicago, to which brief reference[10] has been made, consists of 1,000 feet of each of three kinds of cable made by two manufacturer's and designated as *R*-0.386, *S*-0.400, and *T*-0.500. The number is the nominal insulation thickness in inches. The cables are subjected to overvoltages, that is, to 76 kv to sheath, and to surges by means of a tap from an adjacent 132-kv three-phase overhead line. Heating current is supplied by current transformers insulated for 132 kv. Provision is made for measurements of power factor and average conductor temperature. The standard insulation thicknesses for 132-kv operation were 0.719 and 0.506 inch, respectively, in 1930 and 1938.

Testing was started in June 1930. Except for interruptions for various reasons, the cable was subjected to only voltage for one month, then to voltage with superimposed daily load cycles for two years, and subsequently to voltage and continuous heating to limit effects of daily movement on the sheath. As indicated in table VI, tests have been up to average temperatures of 91 degrees centigrade and to a maximum for short portions of the cable of 120 or 130 degrees. At intervals of two to three weeks, testing has been interrupted to allow measurements at both elevated and duct temperatures of power factors at various voltages from 20 to 100 kv.

Figure 4 shows the variations in power factor throughout the tests, for each of the three cables, all power factors being adjusted to a conductor temperature of 60 degrees, through data from frequent measurements over a range in temperature. Figure 5 shows the maximum and minimum changes found in power factor-temperature characteristics among these cables.

Cable *R*-0.386 has remained entirely stable throughout the test. Cable *S*-0.400, also, has been relatively stable except for a moderate increase in the power factor at 20 kv early in the test. In general, this cable has undergone somewhat less severe heating than cable *R*-0.386, but the 33 days of heating to conductor temperatures of 90–91 degrees had no apparent effect on the power factor.

Cable *T*-0.500 remained stable during the first three years of testing and then developed instability, as evidenced by increasing power factors. A comparison of the temperature data and power factors for this cable indicates, however, that there is no consistent relation between the increases in power factor and the testing temperature. The 365 days of heating to 85–91 degrees had no tendency to accelerate the deterioration in electrical properties since the power factors at both 20 and 100 kv showed indications of approaching a stable condition during this period.

Increases in power factor of the same peculiar type, but of much greater magnitude than those which occurred on cable *T*-0.500, were found also in one case on a portion of a commercial 132-kv line which had been subjected to only moderate temperatures. This showed that high operating temperatures are not essential for the occurrence of deterioration of this type and gave additional support to the conclusion reached above that the high temperatures to which cable *T*-0.500 was subjected were not in the main responsible for the instability found. In general, however, serious deterioration in oil-filled cable has been rare.

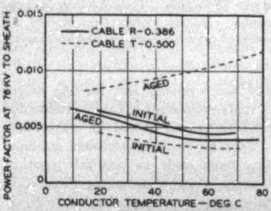


Figure 5. Change in power factor of oil-filled cable in eight years of experimental operation

Figure 5 shows that such deterioration not only raises the power factor but changes the power factor-temperature characteristic from a relatively flat or falling curve to one which rises with increasing temperature. For cable *T*-0.500 the deterioration which has occurred is only a small fraction of that which would be necessary to cause failure due to cumulative heating. The increase in power factor which has occurred is, however, sufficient to reduce the carrying capacity by several per cent.

The results of these data and consideration of the earlier data in the paper and the references lead to the following conclusions:

(*a*). Changes in dielectric losses in oil-filled cable with time are not materially affected by increase in operating temperature, at least up to 90 degrees. This conclusion is not, as might be inferred, at variance with published data which indicate that the rate of deterioration in transformer insulation about doubles for each eight-degree-centigrade rise in temperature nor with the finding that rate of oxidation of oil exposed to air increases with temperature. The important deterioration in transformer insulation occurs in mechanical strength rather than in electrical properties and is caused by oxidation of the solid insulating material. The deterioration in oil-filled cable, which is usually negligible, is caused mainly by chemical changes in the oil which is in contact with only the materials sealed with it in the cable. Chemical changes of the oil do not necessarily result directly in increases in dielectric losses of the insulation.

(*b*). Oil-filled cable insulation may safely be operated continuously at copper temperatures up to 85 degrees and during emergencies at 90 degrees centigrade or more. These values are 5 to 15 degrees or more above the corresponding limits in the present Association of Edison Illuminating Companies cable specifications, where the limits already have been increased twice in the past five years.

Table VI. Tests of Experimental 132-Kv Oil-Filled Cable in Chicago From Start to July 8, 1938

Cable	*R*-0.386	*S*-0.400	*T*-0.500
Total elapsed time (days)	2,934	2,934	2,934
Days of voltage application (76 kv to ground)	2,105.1	2,222.5	2,457.8
Number of daily load cycles to:			
Less than 65 deg C	197	99	216
65 to 74.9 deg C	364	462	345
75 to 79.9 deg C	0	1*	0
80 to 84.9 deg C	0	0	0
85 to 89.9 deg C	1*	0	1*
Total	562	562	562
Days of continuous heating to:			
Less than 65 deg C	49.8	265.0	216.9
65 to 74.9 deg C	592.8	371.2	464.7
75 to 79.9 deg C	590.5	222.6	308.2
80 to 84.9 deg C	142.4	19.6	235.3
85 to 89.9 deg C	48.7	0	336.8
90 to 91 deg C	27.8	32.9	27.8
Total	1,452.0	911.3	1,589.7

* During this cycle the heating current was applied continuously for nine days instead of the normal time of 12 hours.

Note: Copper temperatures are average values determined by resistance measurements. A recent longitudinal survey of duct temperatures revealed extremely large variations in temperatures—a condition never before even approached in Chicago except where steam mains were present. This conduit was installed for test purposes in a prairie and the condition and thickness of the soil covering varies greatly. It is estimated that for an average copper temperature of 90 degrees centigrade the maximum at one localized region was 120–130 degrees.

Part II. Limitations Due to the Sheath

Radial Expansion of Sheath

As the temperature increases with load, the thermal expansion of the compound in insulation of solid-type cable causes an increase in the internal pressure and may result in serious sheath expansion. The rate of sheath expansion increases with increasing internal pressure and increasing temperature. Such matters and the properties of lead and lead alloys are covered in a recent research bulletin[11] on lead sheaths by H. F. Moore and others.

As far as load ratings are concerned, there seems to be no problem involving radial expansion of the sheath for low-voltage cable. The percentage of the volume inside the sheath that is occupied by compound is relatively small; and, because of the low electrical stresses in the insulation, void or gaseous spaces are not particularly objectionable. For cable that operates at about 12 kv and is connected with joints filled with a hard or plastic compound, the internal pressures at loads corresponding to emergency ratings may be 50 or 100 pounds per square inch, as shown in the accelerated aging tests. Several cycles of emergency loading during the life of such cable may produce sheath expansion of 10 to 30 mils, but such expansion is small compared to the expansion that has been found on 69-kv single-conductor cable in Chicago which has operated successfully to date. It, therefore, seems that no trouble due to sheath expansion will occur on moderate-voltage cable.

If 69-kv solid-type cable with oil-filled joints is loaded rather heavily soon after installation, the internal pressure rises until the sheath is stretched enough to accommodate the heat-expanded insulation; and the pressures may be 50 or 75 pounds per square inch or more. Under usual loading conditions common daily maximum pressures are one-third or one-half of these values. As the sheath expands, oil enters the cable from the joints. For the 280 miles of such cable in Chicago that has been in service up to 12 years, the rate of expansion of the cable with $^{9}/_{64}$-inch sheath is about six mils in diameter per year, and this rate does not appear to be decreasing. For a relatively small amount of the cable in which $^{8}/_{64}$-inch sheath was used, the rate of sheath expansion has been roughly twice as much.

In the first few years of service, open-

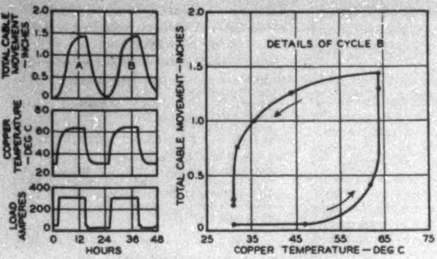


Figure 6. Cable movement for line with block-type loading

For 391-foot length of 350,000-circular-mil three-conductor 13-kv cable

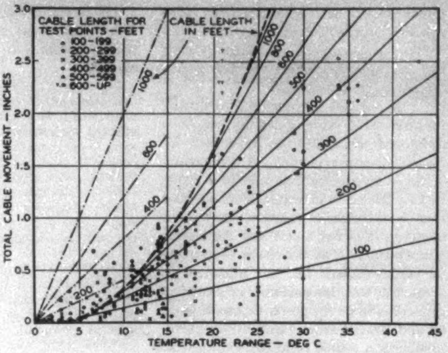


Figure 7. Theoretical and actual cable movement

Data are for 500,000-circular-mil three-conductor 13-kv cable. Lines at left are for copper-bar expansion, while lines at right are values of cable movement derived from formula

ings developed mainly at defects in the sheath structure. With further service, openings developed at the thinnest portion of sheaths that varied considerably in thickness around the circumference. In most of these cases the minimum thickness of the sheath initially was less than 85 per cent of the average thickness. Sheaths produced prior to 1930 developed high rates of trouble when the average expansion around the circumference exceeded about three per cent. More-recently-made sheaths are better in quality and in concentricity and should withstand internal pressures much better.

Accelerated aging tests[9] have shown that as far as the insulation is concerned the daily temperature range for the 69-kv cable could be increased from the present limit of 18 degrees centigrade to 30 or 35 degrees. Such an increase is not feasible for solid-type cable with thick insulation and connected with oil-filled joints unless a sheath with increased creep resistance is provided.

Induced Sheath Potentials on Single-Conductor Cable

For single-conductor cable installed with insulating sleeves and bonded to eliminate sheath losses, the induced sheath potentials vary directly with the magnitude of the alternating current and directly with the distance between insulating sleeves. In 1929 it was suggested in an AIEE paper[11] that, on the basis of laboratory and field experience, the limiting a-c potential between sheath and ground should be 12 volts in order to avoid a-c electrolysis. The general idea then was that this limit would apply during the time of the maximum expected load.

Since then, in order to provide further field data on the effects of the a-c potentials in causing electrolytic sheath corrosion, fireproofing has been removed periodically in many manholes from cable that has been submerged. No corrosion was found except in one case where the sheath also had a slight positive d-c potential on it inadvertently. These cables in general had operated with a-c potentials up to about ten volts. In view of these and other data, it was decided in 1935 that the maximum safe induced sheath potentials to ground for the usual daily loading should be 11 volts and that during emergencies the potential could go to 15 or 16 volts. When the loadings cause higher potentials than these values with cross or auxiliary cable bonding,[12] it is necessary to use sheath-bonding transformers to make the sheath potential to ground one-half of the induced sheath potential in the length of cable between insulating sleeves. Even with bonding transformers and ordinary spacing of the ducts, the usual and maximum loadings may be limited to approximately 875 and 1,250 amperes, respectively, in case the sections of cable are about 600 feet long.

Cable Movement

It has been accepted usually that in the determination of the load rating of a cable the allowable copper temperature should be based solely on characteristics of the insulation, although there has been some discussion of the possibility that the life of the sheath should also be considered. The noticeable number of sheath cracks found especially in cable which has been installed for ten years or more has served to put added emphasis on consideration of the life of the sheath and has suggested that limiting ranges of copper temperature based on allowable cable movement should be established.

Since no previous published attempts have been made to establish practical working relationships among sheath life, cable movement, and load ratings, it has been necessary to start at the beginning and see what could be learned (*a*) about the relation between cable movement and loading and (*b*) about the relation between cable movement and sheath life. Operating and laboratory data bearing on these points have been collected and studied for over 12 years in Chicago. The results of these studies have provided a basis for quantitative discussion of both phases of the problem.

CABLE MOVEMENT AS A FUNCTION OF LOAD RANGE

It has generally been assumed that cable movement should be directly proportional to temperature change and to cable length, allowing for a constant percentage of reduction of movement due to flexing or snaking of the cable in the ducts. Field data, however, have failed to corroborate this assumption. This was thought to be due to the masking influence of the many indeterminate disturbing factors which are present in actual underground systems. It now appears that by taking into account the *restraining forces* due to friction in the duct and to the training of the cable in the manholes, the field data on cable movement become intelligible. A review of the theory will be given.

Consider a length of cable in a duct in which neither tension nor compression is present. Evidently a reduction in the temperature of the cable, which tends to cause it to contract, must develop enough tension to "pull" the length of cable between the manhole and any given point before there can be any movement at that point. The maximum possible tension which could be developed is that required to move the cable along the entire length from the manhole to the center of a duct run. The effect of a temperature reduction cannot be manifested entirely as contraction of the cable, since some of it appears as tension.

Assume now that the maximum tension which can be developed by load cycles and friction in a duct run is present when heating starts. Part of the thermal expansion relieves tension. Part of it tends to push the cable out of the duct mouth, but this takes force, which can be developed only by compresssion of the cable and must be great enough to "push" the portion of cable which actually moves. As heating progresses, the remaining tension becomes localized nearer and nearer the center of the length, while the cable near the ends becomes compressed and produces movement. Finally, maximum compression is developed, it being that force which is just sufficient to push all the cable from the center toward either end. The actual movement appearing at the duct mouth is that portion of the expansion remaining after the tension and compression requirements are fulfilled.

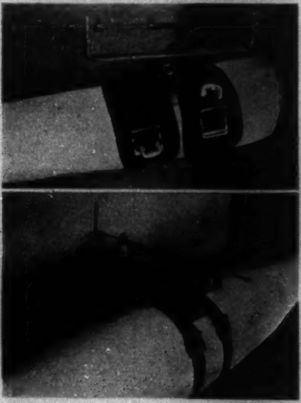


Figure 8. Cable-movement indicator

Top—Indicator installed to measure cable movement at a duct mouth. The range in movement over a period of time is obtained by measuring the separation of the two riders shown on the rod which butts against the duct wall

Bottom—Indicator installed on a cable joint to determine the limits of the lateral movement of the joint to and from the manhole wall

These points are illustrated in figure 6, which gives movement data obtained on a certain well-loaded 12-kv three-conductor line, which had unusually abrupt changes in load. During the first portion of the load cycle, the increase in copper temperature does not result in any appreciable movement at the duct mouths. Instead, the cable becomes compressed. As the temperature increases, the forces developed finally exceed the force required to push the cable through the duct, and movement takes place at the duct mouths. On the cooling cycle, considerable drop in copper temperature occurs before the cable commences to move back into the duct. The copper temperature must decrease enough to relieve the existing compression in the cable, and then must build up sufficient tension to overcome the frictional forces tending to prevent the retraction of the cable.

In the equations for cable movement, the following symbols are used:

M = total movement in inches for a length of cable
L = length of duct in inches
C = coefficient of thermal expansion of cable (16.7×10^{-6} approximately)
A = cross-sectional area of copper in circular inches
W = weight of cable in pounds per inch
D = coefficient of friction for cable in the duct
E = Young's modulus of elasticity for cable in pounds per circular inch ($15 \times 10^6 \times \pi/4$ approximately)
k = maximum longitudinal stress which may exist in the cable at the duct mouth, due to the restraining force of the cable in the manhole
T = copper temperature change in degrees centigrade
T_c = $(WDL + 2k)/AEC$, the temperature change necessary to produce the maximum possible change in stress throughout the cable

If a condition of maximum *compression* were present at the start of a heating cycle, the total movement would be the same as for the thermal expansion of a copper bar, that is,

$$M = LTC \text{ inches} \qquad (1)$$

Under actual conditions of normal cyclic movement, the state of maximum compression or of no initial strain would never be present at the start of a heating cycle. Either part or all of the cable would be under maximum tension. For this condition, which is characteristic of uniform cyclic loading, the cable movement is given by the following formula:

$$M = AEC^2(T - 2k/AEC)^2/2WD \text{ inches} \qquad (2)$$

This formula is to be used up to the point where $T = T_c$. At higher temperatures the formula to be used is

$$M = LC(T - T_c/2 - k/AEC) \qquad (3)$$

Equation 3 takes account of the temperature rise being more than sufficient to overcome the effects of tension, compression, and the restraining force k in the manhole. Neither equation 2 nor 3 takes account of flexing. The cable is considered to act as an elastic column. Numerical values for use in these equations are readily available except for the variables C, k, and D.

The coefficient of thermal expansion of cable might differ somewhat from the coefficient of expansion of a copper bar. The coefficient C for cable would therefore include, for example, the effect of buckling strands or restraint due to the sheath. No definite evidence that buckling occurs has ever been found in Chicago. The sheath does not appear to offer much restraint. The temperature change of the sheath is less than for the conductor, but this is almost exactly offset under stable conditions by the greater coefficient of thermal expansion for the lead sheath. The lag of the sheath temperature behind the copper temperature in normal load cycles may have some slight effect, but calculations confirmed by one laboratory test show that C for cable in ducts that are not submerged is about the same as for copper, that is, $C = 16.7 \times 10^{-6}$ approximately.

The restraining influence k of the cable in the manholes is variable and data on it are scanty. For typical horizontal and vertical offsets of the joint with respect to the duct mouth, the force is estimated from one full-scale laboratory test to be roughly 300 pounds for a three-conductor 500,000-circular-mil 13-kv cable. It is assumed that the force of 300 pounds is built up at some part of the heating cycle to oppose the expansion of the cable into the manhole; then, as the cable cools down, the force drops to zero and builds up to 300 pounds in the opposite direction. An installation of cable having small offsets in the manhole would require a greater force to cause

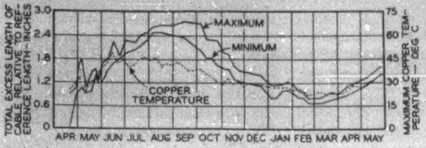


Figure 9. Weekly data for cable movement over one year

For 506-foot length of 69-kv 2,100,000-circular-mil single-conductor solid-type cable

bending than one with large offsets, and the movement would be correspondingly less. The flexibility of the fireproofing used on the cable in the manhole has a marked influence.

The coefficient of friction of cable during installation may vary greatly with different cable and duct materials, but a fair average value, based on many measurements of pulling strains during the past 12 years, is 0.5. This value has been used for *D*, and is probably accurate enough for any length of cable being pulled or pushed in a duct by cable movement, except where a long length is being pushed. In the latter case, it would be necessary also to include the effect of flexing in a duct and the resultant component of pressure against the sides of the duct. This has not been included, as no values for such conditions were available. The friction coefficient under service conditions may be somewhat reduced where there is vibration of the conduit.

Figure 7 shows curves from equations 1 and 3 for one type of cable. Field data are shown for comparison. The straight lines emanating from the origin are for the theoretical expansion of copper bar, which is given by equation 1. The parabolic fan of lines to the right is obtained from equation 3. The dashed line is the calculated movement that would occur on an *infinite* length of cable; for example, regardless of length the total movement at the two ends could never exceed 1.7 inches for a temperature range of 20 degrees centigrade. This is not due to flexing, since equation 3 is not set up to take account of it. It is due solely to the fact that a large percentage of the thermal expansion is taken up by longitudinal compression or tension in the cable.

If flexing were taken into account, the calculated movement would be even smaller. It might be still further reduced by the occurrence of enough flexing to make the cable anchor itself firmly in the duct at widely-separated points, thereby reducing the effective cable length. Although these phenomena are known to occur, the theory is conservative in not taking them into account.

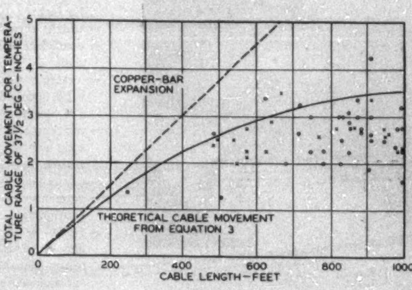
The movement follows the dashed line as long as compression is building up in the cable. When the maximum possible compression for the length under consideration has been reached, the movement thereafter becomes directly proportional to temperature rise and diverges along a straight line. The point of divergence occurs at large temperature rises for the longer lengths, for example, 14 degrees for a 600-foot length. This means that no matter how long a length of cable may be installed, its movement will be no greater than that of a 600-foot length, as long as the temperature range does not exceed 14 degrees. After the point of divergence is passed, longer lengths move more than short ones; but the 1,000-foot length in figure 7, for example, moves only 46 per cent more than the 500-foot length for a temperature range of 25 degrees.

The difference between one type of cable and another is negligible as long as the ratio of conductor cross section to total cable weight is similar. The calculated movement for 69-kv 2,100,000-circular-mil single-conductor cable and 5-kv 375,000-circular-mil three-conductor cable is about the same as for the 13-kv cable in figure 7. It is much less for heavy cables with small conductors, such as 132-kv 600,000-circular-mil single-conductor cable with 719 mils of insulation.

Figure 10. Cable movement on a 69-kv line of the Cincinnati Gas and Electric Company

⊙—Slope of conduit three per cent or less

X—Slope of three to six per cent



The conclusions derived from the theory are strongly supported by the field data obtained on various cables at more than 250 locations during the past 15 years. Usually the movement has been measured in adjacent manholes at the two ends of the length. In some cases the lateral movement of the joint has been obtained also. Data have been gathered at each location for periods ranging from one day to one year. The instruments used for obtaining complete records such as shown in figure 6 are graphic recorders. Where only the limits of the movement are desired, the indicator shown in figure 8 is used. Since this is more convenient to install and maintain than the recording devices a large number have been put into use.

A continuous record of cable movement for a year is shown in figure 9 for a length of 69-kv single-conductor cable. The record illustrates that movement has two components, the daily movement due to load cycles and the annual movement due to changes in ground temperature. The two solid lines form the envelope of the daily oscillations which are superimposed on the seasonal cycle. Both the seasonal and daily movements are fairly well distributed between the two ends of the cable. Field data obtained in many cases indicate that the probable distribution of movement between the ends of a length is 60 and 40 per cent, although a 50-per-cent distribution is not uncommon, and, in a few cases for small movements, the entire movement of a length will appear at one end. The assumption used in Chicago is a 60- and 40-per-cent division of the total movement.

The annual movement is usually larger than the daily, but obviously only the daily movement is of importance in causing sheath cracks. The annual movement may have some indirect effect such as changing the training conditions in the manhole or forcing the joint against the wall. In Chicago the seasonal range in ground temperature is about 18 degrees centigrade. The usual daily range in copper temperature for 500,000-circular-mil three-conductor 13-kv cables has been about 8 degrees centigrade although it has on rare occasions reached 35 or 45 degrees.

Figure 7 shows that actual daily cable movement is usually less than indicated by equation 3, particularly for the higher temperature ranges. Some test points in the lower temperature ranges are higher than the calculated values, but this is partly due to errors in estimating the temperatures as indicated elsewhere in the paper; the cable movements involved, moreover, are small and relatively unimportant. The few recorded cable movements for the higher ranges that lie above the lines given by equation 3 are probably for cables installed under different conditions than were assumed—that is, less friction in the duct, smaller restraining forces in the manholes, a different initial state of tension or compression, or a combination of these factors. To illustrate, data designated by T for 69-kv 2,100,000-circular-mil single-conductor cable have been inserted in figure 7. These points are all for 600-foot lengths of cable which were installed in winter and left without load until the middle of summer. Thus they were subjected to sufficient seasonal rise in ground temperature to approach maximum compression. Upon application of the first load cycle they would tend to obey equation 1; that is, the movements should be about the same as the theoretical expansion of bar copper, and so they were. Subsequent daily movements have been normal, that is, much lower.

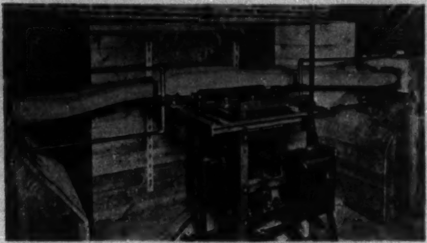


Figure 11. Dummy manhole apparatus

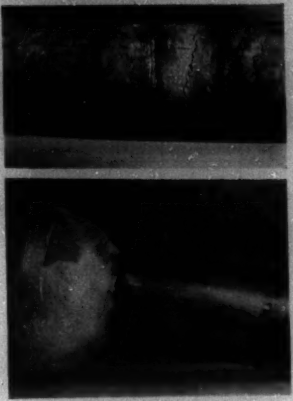


Figure 12. Sheath cracks developed in the dummy manhole tests

Top—Cracks on straight cable near the duct mouth

Bottom—Cracks adjacent to the joint wipe

The daily movement is less, in general, than calculated for all the many types of cables installed in Chicago, as illustrated for the 13-kv cable in figure 7. This means it is safe to accept the calculated values as an upper limit of daily movement. It strongly indicates also that flexing or anchoring of the cable in the duct often occurs. Undoubtedly other factors, such as abnormally high values of duct friction and restraint in manholes, help to decrease the movement in some cases, but certainly not in all cases. For some of the very low values plotted for the 400- and 500-foot lengths, the field records show that practically no movement occurred at one end of the length, which definitely indicates flexing and anchoring. Another such indication is the wide spread in recorded values of movement for any one length and temperature range; for example, for 300–400 feet and 18 degrees the movements range from 0.2 inch to 1.06 inch. Any assumptions that now appear reasonable concerning variations in duct friction and manhole restraint can account for only part of this spread.

It is concluded that there is no objection, from the standpoint of cable movement, to the installation of cable lengths that are much longer than have usually been considered practicable in the past.

Some installations of long single lengths of cable in normal underground ducts have been made in Cincinnati. The cable is 500,000-circular-mil, single-conductor, 69-kv, oil-filled with 315 mils insulation and $^5/_{64}$-inch lead sheath. Cable movement data, for which the author is indebted to the Cincinnati Gas and Electric Company, are shown in figure 10, along with the theoretical movement calculated from equation 3. The cable movement was entirely in accordance with what would be expected from the previous discussions herein; for example, the movement for 900–1,000-foot lengths is only slightly greater than for 500-foot lengths, and the movement is generally much less than for 13-kv 500,000-circular-mil three-conductor cable in which the ratio of copper cross-section to cable weight is relatively much greater. These data are of interest also because some of the conduit sloped considerably, while Chicago conduits are almost always horizontal. There was a tendency for the cable movement at the downhill end of a sloping length to exceed the movement at the uphill end, but no permanent downhill migration was found. This is in accordance with the theory which indicates that downhill creep would occur only for shorter cable lengths or larger temperature ranges than prevail in Cincinnati.

SHEATH LIFE

During 1920–24, the importance of sheath cracks on the Chicago underground system was coming to be fully appreciated, partly because the number of sheath repairs and line failures due to cracks was too high. In 1925, inspectors examined as many of the 24,000 manholes as was feasible, with the result that 500 or 600 sheath cracks were found. Many of the manholes were found to be too small from the standpoint of cable movement, and the protection at duct mouths and supports was found to be inadequate to prevent wearing of the sheath. A program for remedying these deficiencies was instituted and has since been vigorously followed, with the result that two-thirds of the present manholes conform to present standards. The present standards for straight-type manhole sizes are 8 feet by $4^1/_2$ feet for manholes

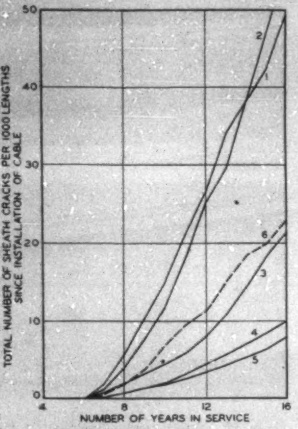


Figure 13. Sheath cracks on 500,000-circular-mil three-conductor 13-kv cable

1—Lengths 400–500 feet
2—Lengths 300–400 feet
3—Lengths 200–300 feet
4—Lengths 100–200 feet
5—Lengths 0–100 feet
6—Total rate for all lengths

to contain three-conductor 5-kv cable with conductor sizes of 375,000 circular mils or less, 10 feet by 6 feet for three-conductor 500,000- or 650,000-circular-mil 13-kv cable, 11$^1/_2$ feet by 6 feet for 750,000- or 1,000,000-circular-mil solid-type 69-kv cable, and 13 feet by 6$^1/_2$ feet for 2,100,000-circular-mill solid-type and oil-filled-type 69-kv cable. The minimum headroom is 6 feet in all cases.

As a result of the rehabilitation program, the number of sheath cracks found per year has been cut approximately in half. About 86 per cent of the sheath cracks on the transmission system are found and repaired before they develop into electrical failures. The methods of repair have evolved and improved somewhat with time. At best, however, operating experience has demonstrated that many of the repairs are satisfactory for only a limited time.

Laboratory studies carried on along with the rehabilitation program have consisted mainly of tests made in the full-sized "dummy" manhole illustrated in figure 11. The manhole is of the standard octagonal shape used in Chicago, with the ducts at the middle of the end walls. The length is adjustable. In a test, a 20- to 25-foot length of cable is used. The training, jointing, and fireproofing are varied to suit the test, but are done in accordance with field procedure. A motor-driven device imparts the desired amount of reciprocating motion to the cable at both duct mouths in order to simulate cable movement. The duration of a test cycle is either 70 or 110 seconds. Three-hundred-twelve cycles are taken as the equivalent of one year of life. In order to indicate sheath failure, oil pressure is supplied to the joint. The appearance of oil or cable compound shows a sheath crack. Figure 12 illustrates the appearance of the cracks. They are similar to cracks developed in sheaths in service and their division among joint wipes, duct mouths, and bends is about the same.

Sixty-seven tests have been made and more are planned. Many of the test results are not directly applicable to problems in the present study. However, they confirm, in general, the proposition that sheath life is greater for longer manholes and for greater offsets between axes of joints and cable at the duct, but the data are not sufficient to determine numerical relationships. Some calculations, however, indicate that an increase in offset is two or three times as effective, within limits, in prolonging sheath life as an equal increase in length of bends. This benefit of increased offsets is based on no accompanying increase in concentration of bending at the duct mouth and joint wipe. Tests indicate that sheath life is shortened by concentration of bending as occurs in service. Studies are being made of schemes to prolong the sheath life in small manholes that cannot be enlarged.

Some definite figures on the life of various sheaths under certain conditions have been obtained. The findings for two types of sheaths on 13-kv and 69-kv cables installed in "standard" sized manholes are summarized in table VII. The average life of commercially pure sheath of 13-kv three-conductor 500,000-circular-mil cable is about 20 years, when installed in a manhole ten feet long from duct to duct, with an offset of 19 inches (18 inches horizontal, 6 inches vertical), with a cable movement of 0.75 inch at each duct mouth and with the joint free to move on the supporting bracket.

The duty on the sheaths of cable in actual service has been considerably less severe than in the tests as far as cable movement is concerned, but more severe with respect to manhole conditions, especially for lines which were installed in manholes built 12 or more years ago. Different lines have, of course, been subjected to different conditions, especially as to loading.

Table VII. Life of Sheaths in Dummy Manhole Tests

Kind of sheath	Commercially pure lead	Calcium-type lead alloys
Kind of cable	500,000-circular-mil, three-conductor, 13-kv	
Number of tests	9	2
Sheath life in years:		
Minimum	9.6	31.6
Average	20.8	38.3
Maximum	34.5	45.0
Kind of cable	750,000-circular-mil, single-conductor, 69-kv	
Number of tests	4	5
Sheath life in years:		
Minimum	9.6	38.2
Average	18.5	67.8+
Maximum	23.8	80.0+

Note: Movement at each duct mouth was about 0.75 inch. The sheathing of cable with calcium-type alloys was done on an experimental basis.

The effect of differences in loading is seen in the fact that 45 per cent of the cracks that occurred in 500,000-circular-mil three-conductor cable operating at 12 kv during the five-year period 1933–37 were confined to the 13 per cent of the lines which carried the heaviest loads. Obviously, some lines must carry relatively heavy loads. These lines had normal cable movement for daily temperature ranges of about 15 degrees centigrade during the 11-year period 1927–37, whereas the average range for the remaining 87 per cent of the 12-kv lines in the same period was only 7 degrees, with correspondingly less movement. The effect of small manholes and of inadequate protection at duct mouths and supports

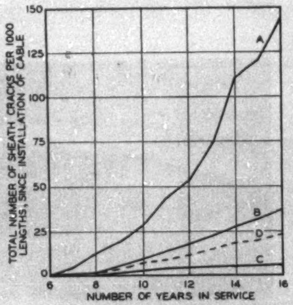


Figure 14. Effect of loading on sheath cracks in 500,000-circular-mil three-conductor 13-kv cable

Average Loading of Lines During 1927–1937 (Amperes)

Curve	Range	Mean
A	250–315	277
B	200–249	225
C	Up to 200	175
D	All lines	205

was to shorten the life of the cable. In spite of the rehabilitation program, the effects of previous conditions are still being felt. For example, in 21 out of 29 typical cases of sheath cracks occurring in 1937, there were prominent contributing factors such as small manholes, wearing at old duct mouth before manhole was enlarged and adequate protection provided, and abnormalities in sheaths such as solder patches applied for bond-wire connections or repairs of former cracks. All of the 29 cables involved were 10 to 15 years old.

Similar findings in varying degrees apply also to the cables operating in Chicago at 120 to 33,000 volts, appreciable percentages of which were installed before protection and training had been improved. They do not apply to the 66-kv and 132-kv cables, which were all satisfactorily protected against wearing and were installed largely in manholes that still appear to be of sufficient size for the usual daily temperature ranges of 5 to 15 degrees centigrade that have prevailed. The measured daily cable movement has been about 0.1 to 0.5 inch at each duct mouth, with an average of about 0.25 inch. Assuming that the sheath life is inversely proportional to the amount of daily movement, then, according to table VII, an average sheath life of about 50 years would be expected, with a possible minimum of about 25 years. It is still too soon to tell what the actual sheath life will be, although a few cracks have occurred, most of them at bond wipes or where the sheath was damaged. The oldest lines have been in service 12 years.

The life of 13-kv cable sheaths has been studied to determine the influence of the age, loading, and length of the cable, with the results shown in figures 13 and 14. About 7 or 8 years after installation a few lengths developed sheath cracks. After 15 years of service the number of cracks has been about two per cent of the number of lengths, some lengths having more than one crack. The longer lengths developed about twice as many cracks as the short lengths, but lengths over 400 feet did not have many more cracks than those between 300 and 400 feet. This is to be expected from the relative amounts of cable movement. Also, the lengths over 400 feet are often part of installations in exceptionally favorable locations. For example, such lengths are common on 12-kv lines installed along with 66-kv lines, for which especially long manholes are provided.

The rate of cracking was much higher on the lines that had higher average loads. Figure 14 shows that the group of lines having the highest average weekday loads (277 amperes) developed about 130 cracks per 1,000 lengths after 15 years of service compared to 30 cracks per 1,000 lengths for moderately-loaded lines (225 amperes average) and 5.6 cracks per 1,000 lengths for lightly-loaded lines (175 amperes average). In this study the load for each line was treated as follows:

(*a*). The maximum three-hour average load for each week in the year was taken from the records; and the average of these 52 values was called the yearly average.

(*b*). The yearly average was determined for each of the 11 years in the period 1927–37, inclusive.

(*c*). The over-all average of the 11 yearly averages was used for the average load on each line.

The individual lines were grouped in accordance with their average loads, as indicated in figure 14, which shows the mean for each group and also the grand average for all lines.

One very important application of the findings on cable movement and sheath life is the determination of the effect of increased loading. For example, the loading on the 13-kv cables in Chicago is being increased to provide greater transmission efficiency. An increase over the past 11-year loading of about 18 per cent in the average load is expected for the 87 per cent of the lines that carry the lighter loads. The increase for the remaining lines, which carry the heavier loads, will probably be held to about 8 per cent to limit sheath cracking. The probable effect on the life of the cable sheaths over a period of years is indicated in table VIII.

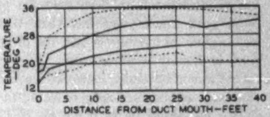


Figure 15. Temperature gradients of empty ducts at typical locations

The increased rate of sheath cracking, excluding the effects of increasing age, would boost the total rate of cable failures from 3.2 to about 3.8 failures per 100 miles per year, assuming that the present efficiency of locating and repairing sheath cracks will be maintained and no substantial changes in manhole conditions take place. The increase does not appear alarming at first, but it is to be expected that most of the additional failures will appear on the more heavily loaded lines and result in a high rate for those lines. There are reasons for supposing that the life of sheath would decrease somewhat faster than the cable movement increases. There would be a considerable increase, also, in the time and expense of repairing sheath cracks. On some of the heavily-loaded lines, cable replacements might be necessitated because of excessive cracking in certain manholes.

The present study is limited to about 15 years because of scanty data on older cables. For this reason, any figure for the total sheath cracks on older cables could be obtained only by extrapolation on figure 14, with possible error. The number of sheath cracks on the oldest cables having the heaviest loads is probably higher than normal now due to lingering effects of former manhole conditions. These effects will gradually disappear as the cables with damaged

Table VIII. Effect of Increased Loads on Sheath Life of 500,000-Circular-Mil Three-Conductor Cable Operating at 12 Kv

	Average of Lighter-Loaded Lines (87 Per Cent of Total)		Average of Heavier-Loaded Lines	
	Former Conditions	Future Conditions	Former Conditions	Future Conditions
Average of weekday maximum loads (amperes)	195	230	277	300
Daily range in copper temperature (deg C)	7	10	15	18
Average cable movement at a duct mouth in inches:				
0–200-foot lengths	0.025	0.045	0.07	0.09
200–300-foot lengths	0.05	0.11	0.204	0.26
300–400-foot lengths	0.06	0.16	0.312	0.40
400–500-foot lengths	0.06	0.19	0.400	0.52
over 500-foot lengths	0.06	0.19	0.456	0.625
Approximate number of sheath cracks in per cent of lengths in 15 years:				
In 200–300-foot lengths	1.0	2.6	9.2	14
In 400-foot and longer lengths	1.4	6.8	45*	65*
In all lengths	1.3	3.4	13.0	19

* Based on relatively meager data.

sheaths are weeded out, but an upward trend in sheath cracks and service failures with increased loading and age will remain.

The magnitude of movement which is producing sheath cracks is 0.6 inch and less at each duct mouth for almost all of the cases involved. This movement is less than might have been expected to cause such cracks and less than that which gave an average sheath life of 20 years in dummy manhole tests.

One way of obtaining increased loading efficiency without seriously reducing the sheath life would be to make the manholes still longer and wider, notwithstanding the small amount of movement involved. This has the obvious disadvantages of requiring great time and expense and of not being always feasible owing to lack of space in the streets. Another method is to try to improve other conditions affecting movement and cracking, but the prospects are not too bright for such a solution. A better method is to obtain more resistant sheath.

The results of the dummy manhole tests (see table VII) show definitely that the life of calcium-type lead alloy sheaths, which were furnished on an experimental basis, is at least twice as great as for commercially pure lead. Such alloy sheaths have outstanding resistance also to radial creep and to abrasion, making for an ideal combination of qualities. Lead containing two per cent tin was no more resistant to bending in a few tests than commercially pure lead, and antimony alloy was less resistant.

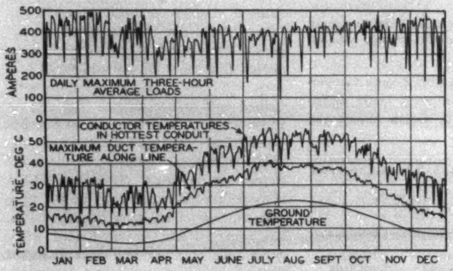


Figure 16. Load and temperature records for a 69-kv 750,000-circular-mil single-conductor solid-type cable

It seems that for many cables the expected time of present-day commercial sheaths to cracking in manholes is only one-third to two-thirds of the life of the insulation, at least for Chicago. Apparently better sheath materials are needed to resist the effects of cable movement. Then a *balanced* cable design will become available, but it would be many years before the full advantage of installing such cable could be realized on an underground system.

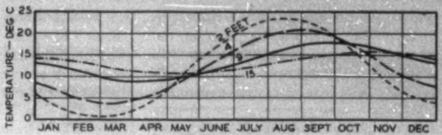


Figure 17. Seasonal variation in ground temperature at various depths

Curve for four feet is used as general ambient

Part III. Heating Characteristics of Conduits

Some work on determining temperatures of conduits has been done in Chicago since 1910. From 1923 on, however, such work has been unusual in that it has been on a *continuous* basis for the entire city and has been supplemented with many special investigations. The resultant data have afforded a continuous basis for determining the allowable loading of cables and the allowable number of cables to put into a given conduit, without the necessity for large factors of safety to take care of unusual or unforeseen variations in the heating characteristics and without danger of the conduit temperatures and resultant cable temperatures materially exceeding the maximum values expected from survey data and calculations. This eliminates such fears as were expressed in a 1921 AIEE meeting[13] that "The operating man does not know where the hottest spot in his system is, and from the operating man's point of view, as I see it, I should prefer to keep the rating down to a reasonably conservative basis."

An inquiry just made by the author concerning the practice of 18 utilities indicates that most of them (having over 65 per cent of the cable in the country) closely follow the temperatures of conduits and cables where the temperatures are the highest, and 15 of them make some routine conduit temperature measurements.

Annual Temperature Surveys of System

In connection with operating the system or with proposed additions to it, the Commonwealth Edison Company makes routine and special surveys of conduit temperatures every year, thereby measuring 2,500 to 6,000 spot temperatures. These surveys yield considerable information also on water conditions. Most of these data are obtained in the summer, which is almost always the time of highest cable temperatures, even though the loads may be heavier in the winter.

The duct temperatures are obtained with mercury thermometers attached to a steel tape and inserted 20–25 feet into the apparently hottest empty duct. As illustrated in figure 15, the results of numerous detailed surveys of temperatures between manholes have indicated that temperatures taken 20 feet from the duct mouth are not influenced by manhole air. This is contrary to the opinion that temperatures taken 5 or 8 feet from the duct mouth are satisfactory and to another statement that 25 feet[14] is not far enough.

To obtain air temperatures in a duct containing cable, it is necessary that the cable fireproofing be broken away at the duct mouth and then later replaced. In addition, it appears difficult to get consistent results on the air temperature with a temperature-indicating device inserted between the cable and the duct, where the clearance is often only one-half inch.

The gas-filled bulbs of about 36 recording thermometers are installed approximately 25 feet into the hottest duct in the hottest location found in the annual surveys for each important line and installed also at other special locations. At any other location along the route of the line, the temperature may be estimated at any time during the year on the basis of records from these thermometers along with temperatures and other data obtained in the annual survey.

A record is kept for each location having a recording thermometer as indicated in figure 16. Another Chicago practice is

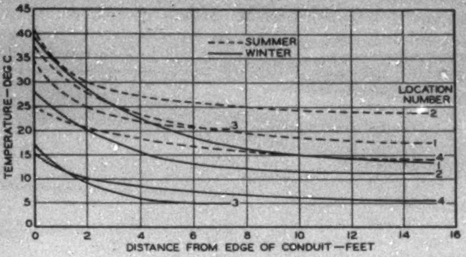


Figure 18. Temperature gradient laterally from conduits

to determine and record for each week the maximum load and the maximum three-hour average load for each line operating at nine kilovolts and over. Periodic load checks are made for lower-voltage cable.

Recording thermometers are installed at various depths in the soil at two locations remote from all other sources of heat for the purpose of establishing ambient earth temperatures. As indicated in figure 17, the annual variation in ground temperature becomes less with depth. The heating characteristics of a conduit section are based on the temperature rise of the air in that conduit above the ambient earth temperature corresponding to its depth.

Special Surveys

When large irregularities in the heating conditions along a section of conduit are indicated or suspected—such as an external source of heat being close to a portion of the conduit—then longitudinal temperature surveys are made. A 410-foot cable with thermocouples attached at 20-foot spacings is used. After this cable is installed in a selected empty duct, it may be moved to permit temperature readings at any desired spacing.

Usually the duct-temperature rise varies less than 10 per cent along the conduit section, except for the end 15 feet or so. Where one conduit crosses another, the temperature rise has been found to be as much as five or ten degrees centigrade in excess of normal. Another example of a special case occurred where a leaking steam main crossing the conduit created an excess temperature rise of about 50 degrees.

In 1926, thermocouples were installed in a plane at right angles through the conduit at ten different kinds of locations. At each location 20 thermocouples were placed in the periphery of the conduit and in the soil for 2 feet on each side of the top and bottom and for 15 feet each way from the sides. The principal findings are illustrated in the typical curves in figure 18. This study, together with other field observations, indicates that from 70 to 85 per cent of the total temperature drop from the hottest empty duct to the base earth temperature occurs from the side of the conduit and through soil, provided the width of the conduit is four ducts or less. Furthermore, separations, even though small, of adjacent conduits are found helpful. To make the heat effect of one conduit on an adjacent conduit negligible, the separation between conduits should be about twice the combined height of the two conduits, although separations over 15 feet are probably unnecessary. A slight advantage in heat conductivity was indicated for conduits using precast concrete ducts as compared to fiber ducts.

In 1936, thermocouples were installed transversely through a variety of conduits at four different locations mainly to aid in determining what value of thermal emissivity should be used for the sheaths in connection with calculating the rise of the sheath above empty-duct temperatures. Typical cross sections are shown in figure 19.

Although it had been appreciated that the temperature rises through conduits varied considerably with circumstances, the results obtained, as illustrated in figure 20, were surprising. If the empty-duct temperature is used as the ambient, then even for the cable in the hottest ducts it appears conservative, especially for the heavier loads, to employ the usual sheath emissivity constant of 1,200 degrees centigrade per watt per square centimeter. With reference to the air surrounding a cable in a duct, the sheath emissivity constant is usually 550 or less. These statements are based on the fact that for losses of four watts or more per foot of cable, most of the sheath temperature rises were below the lines drawn on figure 20 to correspond to the sheath emissivity constants of 1,200 and 550, respectively. Chicago data, together with other data, indicated also that the rise in temperature between the air in the occupied duct and the air in the hottest adjacent empty duct may be roughly, one degree centigrade per watt per foot of cable. Kirke[14] has indicated that the unit temperature rise of the sheath decreases as the cable losses (and sheath temperature) increase, and it appears that this phenomenon might well be taken into account for, particularly, very high loads, although it has not usually been done in Chicago.

The above tests showed that, contrary to the findings of some authors,[15,16] not all the heat comes to the ground surface during each season of the year. Usually in the summer the top row of ducts is hotter than the bottom row. The test results confirm the previous finding that the corner ducts are cooler by a small margin over other outside ducts.

Other Results of Surveys

Heating characteristics of conduits have been found to vary with the following conditions, excluding the effects of whatever neighboring structures may be present: type of soil, moisture content of soil, season of the year, size and material of duct, number and configuration of ducts, depth of conduit, and number of cables installed.[17,18] Considerable information has been published on the first two items. In Chicago, the temperature rise of the conduits in the soil having the poorest heat-dissipating characteristics is five times the rise in the best soil, all other conditions being the same, and

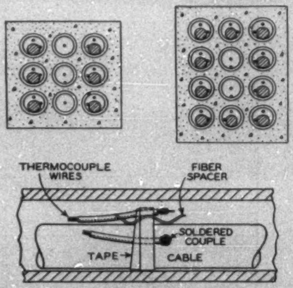


Figure 19. Installation of thermocouples in underground conduits

Thermocouples were placed in air or attached to sheath as indicated, and leads were imbedded in concrete of conduit and soil up to special terminals near street surface excluding conduit under water. For the soils usually encountered, this ratio is 1.4 to 1. The conduit and soil dissipate the heat less readily in the summer than in the winter, the variation being from 1.3 to 1.

An increase in duct size increases the perimeter of the conduit and, thereby, decreases the unit thermal drop to base earth. Increasing the depth of the conduit may bring the conduit in contact with wetter soil,[19] but unless water is reached this is counterbalanced by increased length of the path of the heat flow to the ground surface.

As illustrated in figure 21, the duct temperature during any 24-hour period almost always varies only a few degrees, or less, and this variation is generally less than 15 per cent of the temperature rise of the duct above ambient earth, even though the cables have daily load factors of only 50 or 60 per cent. In view of such data, the conduit heating constants for determining the temperature rise for the hottest empty duct are calculated on the basis of the *average* heat loss during the 24-hour period. The maximum duct temperature usually occurs two hours after the maximum heat load.

Except for week ends or during emergencies, the heat generated does not vary considerably from day to day. In emergencies, correction must be made for the effect of the excess heat generated over the usual amount of heat; and it has been found that the temperature rise due to this excess reaches 40 per cent of its ultimate value in one day and 60 per cent in two days. These attainment factors are much lower and much higher than those given by some other investigators.

Instability of the heating constant has been found where the soil around the conduit was unusually dry and perhaps full of sizable voids, and where the duct temperature was somewhat above 50 degrees centigrade. An example of what may occur in such rare circumstances when the heating load in the conduit increases substantially above its usual value is illustrated in figure 22. In a few days the heating constant increased from 1.8 to 2.9 degrees centigrade per watt per foot of conduit. Even in this case the daily range was not more than 20 per cent of the duct-temperature rise.

In January 1921 a rare record of heating load of conduit and duct temperature was obtained on a short length of heavily-loaded conduit adjacent to a d-c substation. The conduit had 30 ducts containing 25 cables. The load on these cables was heavy for eight hours of each day, and light the remainder of the time. The maximum duct temperatures for the first five weekdays were, respectively, 80, 89, 94, 94, and 96 degrees centigrade with a daily variation of 20–25 degrees. The soil was a little better than average from a thermal standpoint. Since all cables involved were operating at 115 volts direct current, no cumulative cable heating resulted.

From studies of the two experiences cited, as well as considerations of soil characteristics in general, it appears that the maximum allowable duct temperature, if serious drying out of poor soil is to be avoided, is about 50 degrees centigrade. This limit agrees with Church's general recommendation.[20] For fair or average soil, this limit might be increased about 10 degrees; and for compact soil containing over 15 per cent moisture still further increases seem justifiable, although they are unlikely to be useful.

For a given conduit containing a given number of cables, the temperature surveys have indicated that the heating constant for a given time of year may vary considerably from the average in previous years. This is one outstanding

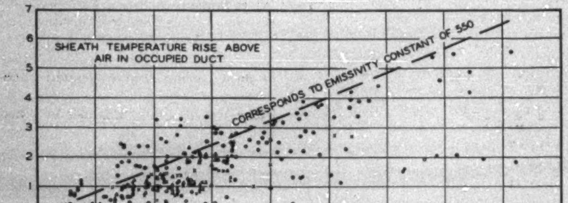
Table IX. Heating Constants for Underground Conduits

Number of Ducts	Heating Constant in Deg C per Average Watt of Loss Over 24 Hours per Foot of Conduit			
	Summer (June 1–October 31)			Winter (November 1–May 31)
	Chicago	NELA[21]	Kirke[14]	Chicago
4	1.42	0.93	1.5	1.26
6	1.17	0.82	1.2	1.04
8	1.02	0.77	1.0	0.91
9	0.98	0.77	1.0	0.87
12	0.89	0.74	0.86	0.79
16	0.79	0.72	0.75	0.70
20	0.72		0.67	0.64
24	0.64		0.60	0.57

NOTE: Above is based on only the outside ducts being occupied with cables carrying load.

Figure 20. Temperature rise of sheaths above air in four conduits

All rises have been corrected to cable with $2^7/_8$-inch diameter when diameter was different. One type of point in graph for each location



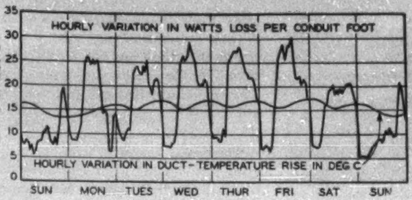


Figure 21. Heat and temperature variations in a conduit

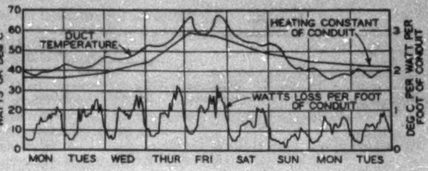


Figure 22. Temperature characteristics for a thermally unstable conduit

reason for making temperature surveys year after year. Detailed studies for 298 locations in Chicago showed variations ranging from 0 to about 50 per cent, the average being 20 per cent. The variations in percentage were less for the poor but fairly stable soils.

Heating constants for fair conditions with only outside ducts occupied are given in table IX. In Chicago, many center ducts are occupied by small cables used for relaying, signals, and voltage indications.

When data from temperature surveys and cable loads indicate that changes should be made in order to avoid excessive temperatures, the procedures successfully used are illustrated by the following:

1. When the problem requires an immediate solution, the conduit is usually flooded.

2. The soil around the conduit, especially when the heating characteristics are poor, may be replaced with bank-run sand and gravel. In such cases the replacement is for the soil from the surface to a depth in line with the bottom of the conduit and extending for about two feet on each side of the conduit. In one case, for instance, the result of this replacement for a 16-duct conduit was to reduce the heating constant from a range of 1.65–2.23 to 1.05–1.32. Since this conduit contained 12 single-conductor 13-kv cables which together served one side of a large transformer, it was not feasible to remove any of the cables. Even where it is feasible to remove cables, it has been found more economical in some cases to replace the soil instead.

Table X. Maximum Allowable Temperatures for Some Chicago Cables

Kind of Cable			Maximum Copper Temperature Used (Deg C)	
Operating Voltage (Kilovolts)	Number of Conductors	Type	For Normal Rating	For Emergency Rating
0.12	Any	Solid	85	105
4	Any	Solid	85	105
9	3	Old rosin	81	95
9	3	Solid	81	95
12	3	Old rosin	77	90
12	1	Solid	80	100
12	3	Solid	77	90
66	1	Solid	58 or 60	60 or 65
132	1	Oil-filled	70	80

NOTES: Three-conductor cable is of belted type; same cable of a given design is used for 9 and 12 kv. Temperatures for normal ratings are in accord with present rules. Above limits are used for the 132-kv cable already in service, because they give ample ratings for system requirements.

3. One or more cables may be removed from the conduit.

4. The existing cable may be replaced with cable having a larger conductor.

5. A ventilated manhole may be installed where one conduit or a steam main crosses another conduit.

6. A steam main crossing a conduit may be specially insulated, and the separation perhaps increased.

In general, 9- or 12-duct conduits are the most economical in Chicago from an over-all standpoint, except where space is limited, in which case a greater number of ducts must be used. On the other hand, if it is known that the total number of cables will not exceed, say, four, then a 4-duct conduit should be built. On the Chicago system about 17 per cent of the total length of all ducts is in conduits having more than 12 ducts. The present trend in construction will reduce this percentage.

Part IV. Principles and Methods of Calculation

The method of calculating the ratings is in line with literature prepared by Simmons[21] and others. The chief principles and assumptions follow:

(*a*). Thermal resistivity of the insulation, the value of which is based mainly on Chicago tests, is taken as 550 degrees centigrade per watt per cubic centimeter for oil-filled cable, 600 for solid-type 69-kv cable, 650 for modern solid-type 5- to 35-kv cable, 800 for lower-voltage cable, and 900 for old rosin-impregnated cable.

(*b*). The thermal emissivity of the sheath is assumed to represent the thermal drop from the sheath to the air in the hottest unoccupied duct, and based partly on some test data is taken as 1,200 degrees centigrade per watt per square centimeter for cable with a diameter of three inches or more. For smaller cable the constant used is smaller, for example, 1,165 and 1,005 for two- and one-inch cables, respectively. For three cables in a duct, the equivalent surface is taken as 2.25 times the surface of one of the cables, while for two cables it is taken as 1.83 times the surface of one cable.

(*c*). In calculations of a-c resistance of cable, corrections are made for skin effect, copper proximity effect, and sheath losses. Skin effect is determined from Ewan's curves.[22] The copper proximity for multiple-conductor cable is usually assumed to be one-half the skin effect except for "compack" conductors, where it is considered as zero. The sheath losses in the three-conductor cable are calculated on the basis of Meyerhoff's[23] formula. For segmental single-conductor cable, the magnitude of the skin effect is assumed to be the same as for a conductor having a d-c resistance equal to 2.6 times the resistance of the conductor under consideration.

(*d*). The dielectric losses are determined from test data. For each type of cable a conservatively high dielectric loss-temperature curve is used. In determining the temperature rise of the conductor above sheath, it is assumed that the entire dielectric loss passes through one-half of the insulation resistance for single-conductor cable and through two-thirds of the insulation resistance for three-conductor cable.

(*e*). The cable dimensions are approximated for the cable as furnished. For instance, the sheath thickness is taken as being five per cent more than the nominal thickness, and the insulation thicknesses for 13-kv cable are taken as being six per cent more than the nominal thicknesses.

(*f*). In the calculation of general ratings, the heating constant is based on fair soil and on normal depth of conduit. In the determination of load ratings for special cases, investigation is made of conduit and ground conditions pertaining to each case to determine the specific heating constant applicable.

(*g*). Ground temperatures are taken as practically the highest that will occur during the two periods of the year, that is, 14 and 23 degrees centigrade for winter and summer, respectively, for conduits at normal depths.

(*h*). For general ratings, the first calculation, which is important, is based on the most prevalent size and on conservatively large conduit sections.

Table XI. Load Ratings for Typical Cables in Chicago

Cable			Rating in Amperes			
			Normal		Emergency	
Normal Size (Circular Mils)	Number of Conductors	Operating Kilovolts	Summer	Winter	Summer	Winter
1,500,000	1	0.12	1,230	1,330	1,460	1,520
375,000	3	4	340	375	400	425
250,000	3	9	230	255	260	285
500,000	3	12	365	410	420	465
350,000	3	33	315	375	340	400
750,000	1	66	460	480	510	550
1,100,000	1	132	450	550	735	825

Notes: Ratings are for the maximum three-hour average load during a day. Single-conductor cable has no sheath losses. The normal ratings for the example of 132-kv cable are limited by thermal conditions created by other cable in the same conduit.

(*i*). The characteristics of the loading of the cable as to daily load factor, heating load factor (ratio of average loss during 24 hours to loss corresponding to average load over the three-hour maximum period), and ratio of usual daily maximum three-hour average load to normal rating are based on present and expected future trend. This three-hour average load is usually five or eight per cent below the very maximum load during a day and gives a good practical value to use in determining temperature rise of a cable. The heating load factor is usually taken, for instance, as 60 per cent for 13-kv three-conductor cable and as 70 per cent for 69-kv single-conductor cable; and the ratio of daily maximum three-hour average load to normal rating is taken as 68 and 90 per cent for the two cables, respectively. An example of an exception is for nine single-conductor cables feeding the 12-kv side of a large transformer; then the calculations are based on all nine cables having maximum loads equal to the normal or emergency ratings.

(*j*). The maximum allowable copper temperatures used at present in Chicago, as indicated in table X, are, in general, less than recognized as safe in part I of this paper. It is considered wise to be conservative on this matter until additional test data and, more particularly, operating experience are available, especially because of the probability of an excessive number of sheath cracks incidental to extra-high copper temperatures. In some instances, the ratings actually used produce lower maximum copper temperatures than given in table X, because of any of the following limitations: (1) allowable maximum duct temperature, depending on the type of soil; (2) allowable maximum duct temperature, depending on other installed cable that may be predominating in importance over the cable under calculation; (3) allowable daily range in temperature for heavy solid-type insulation.

(*k*). The cable movement at the duct mouth is determined mainly from the standpoint of the usual expected daily maximum load. The accepted maximum allowable movement for the usual daily loading is 0.5–0.75 inch at a duct mouth, the lower maximum applying for the more important cable. The cable movement is given a little consideration in connection with the emergency rating.

Ratings are usually determined for two periods of the year, summer and winter. For the important 66-kv tie-lines where the desired loading is frequently great and the load may be controlled, it has been found advantageous to give a special set of ratings applying for the periods of June 1–July 15 and October 1–31 in order that during these periods the ratings may be above the midsummer ratings. For each period of the year, calculations are made for all kinds of cable for the normal ratings and for the emergency ratings. The emergencies are considered to last one and two days, respectively, for solid-type and oil-filled cable because experience shows that repairs may be made to a circuit within those periods. In addition, special ratings are occasionally calculated where requests are made for larger than the general ratings and it is found that the lines are installed under abnormally favorable conditions, or where the surveys of the group following temperatures show the ratings should be less than the general ratings on account of subnormal local thermal conditions. Another special set of ratings is given for some tie-lines where the emergency will exist for only one or two hours, with the result that the temperature rise of the cable and conduit during the emergency is materially less than would be the case for protracted operation. The transient heating characteristics of the cable in such cases are determined on the basis of data by Miller and Wollaston.[24]

The common size outside the downtown area in Chicago is the 500,000-circular-mil three-conductor 13-kv cable operating at 12 kv. It is assumed in the first calculation that there are eight such cables in a 12-duct conduit, and the normal ratings are determined on the basis that seven of the cables have a usual daily maximum load of 68 per cent of the normal rating and that the eighth cable is carrying the rated load. The emergency ratings are then calculated on the basis that seven of the cables are carrying the calculated usual load and the eighth cable is carrying the emergency load. In determining the ratings of cable operating at, say, 4 kv, it is assumed that there are five of the 12-kv circuits in the conduit and three of the 4-kv cables, and that the 12-kv circuits and two of the 4-kv cables are carrying the usual loads.

Where the conduits are abnormally warm or congested, as at generating stations, where 12- or 16-duct conduits may be less than ten feet apart or may cross in some cases, it is the practice to specify an extra-large size of conductor in order that the portion of circuits in such conduits shall not limit the carrying capacity of the circuits as a whole. For example 650,000-circular-mil is used in place of the usual 500,000-circular-mil conductor on the three-conductor lines operating at 12 kv; and 1,000,000-circular-mil is used in place of the 750,000-circular-mil conductor on the single-conductor lines operating at 66 kv. The use of the extra copper for a short part of a line has been found to be an extremely economical procedure.

The general ratings are given for some typical cable sizes in table XI.

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Discussion

G. B. Shanklin: See discussion, page 560.

Wm. A. Del Mar: See discussion, page 561.

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): Mr. Halperin's findings were very interesting in regard to the beneficial effect of increased offsets in manholes versus manhole lengths. What other means have been found in Chicago to prolong the life of the lead sheath by changing conditions in the manhole outside of lengthening it or broadening it?

Under the heading "Summary," in part I of the paper, a remark is made as follows: In general, this effect (power factor increased at normal operating temperatures) should not be expected to shorten the life of the cable adjacent to joints. It seems that in the experience of The Detroit Edison Company the presence of joint-filling compounds of petroleum base, because they mix readily with the cable impregnating compounds, results in a considerably higher power factor.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): Careful field records and research data are of infinite value to the cable manufacturer. No product made is first presented in its perfect state even for services in which conditions are static; and for utility distribution systems which continually change in practice, economics, and operating conditions, it is even more evident that continual change and improvement is essential in its component parts. Such work as here presented which sums up manufacturers' results and technique, field experience, and economic distribution practice definitely points the way to further work by manufacturers to develop more satisfactory cable for present day practices.

The conclusions drawn by the author are general. With a problem containing so many variables probably no general conclusion is safely applicable to all cases and bears out the contention of cable engineers which is of many years standing that current ratings by manufacturers must be approximate and then considerably on the safe side.

Undoubtedly some increase in operating temperature could be allowed based on the probable performance of modern cable insulation only. However, such increase is counteracted by detrimental features concerned mainly with the performance of the lead sheaths such as expansion from pressures, cracking, electrolysis, and also probable changes in heating constants of the ducts and surroundings due to drying out. It is interesting to note that in the main all detrimental features are due to varying conditions imposed by load cycles.

More definite practices and rules governing permissible overloads have been greatly needed for many years. Here again it is probably not practical to use a general rule but allowable overload temperatures and their durations could well be developed for various insulations. The present AIEE rule for maximum continuous operating temperature is not complete enough for modern uses. The author's tests and summation approaches this subject.

The question of thermal stability of old rosin cables is interesting. We know of very old rosin-rosin oil cables of high loss still operating at 12 kv after many years. Thermal stability at this voltage is not appreciably affected by losses in spite of the great agitation on this subject a few years ago.

The opinion given that admixing of joint compound with cable compound is not detrimental is interesting and also directly opposite to the ideas of some other utility engineers. We are inclined to agree with the present writers in this respect for moderate voltage cables.

Part II of the paper dealing with sheaths is quite unique and well done, particularly that part dealing with equations for expansion of cable. We would not expect the coefficient of a cable to be as near to that for a solid copper bar as here taken. The lay of cabling and stranding must have some effect. Considering a stranded single-conductor cable in which the strands are laid at an angle of 30 degrees, an increase in the length of the strand is not directly applied to the length of the cable as shown below:

Let

l = lay or pitch of strand along cable
d = pitch diameter of layer
L = length of strand
$= \sqrt{l^2 + (\pi d)^2}$

Let L increase by ΔL and assume no change in pitch diameter

$$L + \Delta L = \sqrt{(l + \Delta l)^2 + (\pi d)^2}$$

or $l = L \cos 30°$

It would be expected that

$$\Delta l = \Delta L \times \cos 30° = 0.866 \Delta L$$

and that

$$C \text{ effective} = 16.7 \times 10^{-6} \times 0.866$$
$$= 14.5 \times 10^{-6} \text{ for the assumed case}$$

This effective coefficient would be different for multiconductor cable and different again where tension or pressure existed since there is no reason why either could not change the angle at which the longitudinal force of expansion and contraction is acting.

With reference to sheath life we find the probable life as determined by test very interesting. Many improvements in lead-sheath quality and workmanship have been made quite recently and undoubtedly more will follow. Thicker lead sheaths promise some help but alloyed sheaths may give equal or better results more economically.

Robert J. Wiseman (The Okonite-Callender Cable Company, Passaic, N. J.): Mr. Halperin has written a very remarkable paper, describing the researches his company has been conducting for many years and now have reached a point where the story may be told. I think the title might be changed to something like "Why Cables Operate as They Do" for he gives a very fine exposition of the problems of operation and how they might be solved. He is helping all of us better to appreciate the utility's operating problem and we cable manufacturers will have to give considerable study to his paper and in fact all of the papers dealing with cable operation in order to find how we can help to improve their operation or increase their life. Therefore, our attention is brought to the statement in the third paragraph under Part I in which he states that the main consideration is the influence of heating on the ability of the cable to withstand removal and reinstallation without material lowering of the dielectric strength of the cable. I think that all of us will agree that if we could keep a cable at a constant temperature continuously we would be willing to agree to higher temperatures than are standard at the present time, but it is the upsetting effects of the high and low temperatures that the cable is subject to that causes a cable to lose its physical as well as electrical properties. Its life is shortened and, therefore, we must try to hold down the maximum temperature in order to get as far as we can guess a life expectancy that is in fair agreement with other kinds of equipment. As Mr. Halperin states, there is a scrapping of a cable before its useful life is reached because of causes other than insulation deterioration and one of these is that the cables have not been operated in most cases to their present permissible temperature. Until we do get this experience it is difficult for us to accept higher permissible temperatures than now set up. As this experience can only come from the utilities we must await their collecting the information for us.

This is equally true of the emergency rating of cables. We don't know how much a cable will lose in years of life by short periods of overloading. It has not been possible to determine it because the last 12 to 15 years have seen a remarkable improvement in the quality of oil and paper as well as manufacturing methods. If the MIT research were conducted today it is possible that we would find less deterioration than in 1926 when it was made. If it were possible to follow closely the daily load cycle on a cable and its history for several days previously, a method for determining an emergency rating could be set up that would still keep the maximum conductor temperature close to the permissible values. It would be a tedious job and require almost continuous calculating of the conductor temperature as the load varied. It would not be practical. A comparison of transformers and cables in their emergency ratings is not entirely possible. The former has plenty of oil which can circulate and assist in cooling and also a large volume of material for heat absorption and if moved, it is done as a unit without upsetting the insulation. For cables we have small volume, no oil circulation to assist cooling, and the cables during their expected life will be moved. The aging of transformers due to heating has been well studied from the life expectancy viewpoint and, therefore, the limits set up are probably based on this knowledge. Why should cables be set as high as transformers in the light of the difference in their make-up and operation?

Mr. Halperin's description of his aging tests on 12-kv cable with pressure-tight potheads and open potheads checks our experiences on aging tests on 69-kv solid-type cable. When we sealed up the end of the cable and conductor to prevent oil flow from the pothead into the cable we obtained higher pressures and higher vacuum in the cable than when the ends were left free to permit the flow of oil back and forth and obtained a longer life on cables with ends open. We then studied the effect of more freedom of flow of oil between a joint and a cable by eliminating the varnished cambric wrapping and substituting tubes and open-mesh cotton tapes directly over the connector. We got better stability, lower pressures, and longer life. We have suggested to several utility engineers that better operation would be obtained if this were done and I hope some will do so soon. This will eliminate the experience described by Mr. Halperin on joints in part I. I cannot appreciate how using low-loss varnished cambric taping is going to improve the mechanical problems of his joints.

Part II dealing with limitations due to the sheath is very instructive. Mr. Halperin's experience that increasing the lead thickness $^{1}/_{64}$ inch cuts the expansion in half is what we found years ago and I have reported it at other meetings. We found this to be true when comparing $^{8}/_{64}$ inch to $^{9}/_{64}$ inch, and $^{8}/_{64}$ inch to $^{10}/_{64}$ inch. It is one of the reasons that I advocated years ago increasing the wall thickness of our large-diameter cables in order to reduce the radial expansion. I still believe in it.

For single-conductor cables the oval-shaped conductor with a circular sheath is the solution of the internal-pressure problem. This has been proved in tests by our associate company in England, The Callender's Cable and Construction Company, and in our laboratories. On new cable we did not exceed an internal pressure at the sheath of 20 pounds per square inch and a vacuum higher than 5 inches, whereas on circular conductors we have gotten 85 pounds per square inch pressure and 20 inches vacuum. We have obtained about 25 per cent longer life on accelerated aging tests using oval conductors. An oval conductor and free-flowing joint will give a much longer life in single-conductor solid cables than obtainable with round conductors and solid joints.

I wish to congratulate Mr. Halperin on the section dealing with the manner in which a cable moves with changes in temperature. It is understandable and sounds logical and makes us glad to know that cable movement is not as much as we thought and how fortunate we are. If it did move in the amount a solid copper rod expands, the utilities would be experiencing a greater number of lead failures and replacements which is costly, or would have to build much wider ducts which is also costly. I would like to ask one question. How much actual influence will the size of the cable compared to the size of the duct have? The restraining forces in the duct for a cable close to the size of duct will be higher than for a smaller cable in the same duct, but the chance of snaking or flexing in the duct is less in the former. Do they offset each other so that we can say that all cables act alike in the same duct?

We have been told by utility engineers of the cracking of lead sheaths near the duct mouth due to cable movement and have been studying ways of overcoming it. We have considered cutting the sheath and inserting a bellows which relieves the lead and permits cable movement at one location. One of the companies had the same idea.

Part III, dealing with the heating characteristics, brings forth few questions to discuss as it is well written and quite detailed. Under "Annual Temperature Surveys of System," fifth paragraph, reference is made to using the maximum three-hour average load in determining the load factor. Although this is usually done, we have wondered sometimes on what basis a three-hour time period has been taken instead of a one-hour period.

Under "Special Surveys," fifth paragraph, is not the low surface emissivity constant of 550 likely to be due to inaccuracy in measuring the air temperature, the latter probably being too high which would give a low temperature gradient? We consider temperature measurements in an empty duct more accurate. The temperature of the duct wall for a duct containing cable is probably not more than one or two degrees higher than the temperature in the empty duct due to heat conduction of the concrete.

Part IV, item *b*—we understand the thermal emissivity of a sheath is assumed to represent the thermal drop from the sheath to the air in the same duct and not to the air in the hottest unoccupied duct. The thermal duct constant takes care of the duct and the surrounding earth. We like the method proposed by Kirke, and which is used in England, to use a separate term for the duct structure and another for the earth. If this is done, the earth term can vary according to the kind of soil. We also hope that some day a study of these constants for a small number of ducts in a duct bank and very few cables installed will be determined experimentally so that we will be more sure of what they are than we are today.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York): Mr. Halperin is to be complimented on the scope and treatment of the various factors influencing the load ratings of cable. In part II the treatment of cable movement presents a fresh theoretical explanation of the magnitude of movement which will probably be encountered and the wealth of field test data seems to confirm the theory advanced.

In connection with induced sheath potentials on single-conductor cables treated in this section of the paper I would like to point out that Searing and Kirke in an article "Reduction of Sheath Losses in Single-Conductor Cable," *Electrical World*, October 6, 1928, suggested the desirability of limiting the voltage between cable sheaths to a value of 200 volts during fault conditions. This limit was set as one dictated by safety to personnel who might be working in manholes containing these cables during the period when a fault occurred. It was assumed that high-voltage circuits in general had impedances in the form of transformers or protective reactors which would limit the fault currents to approximately ten times the full-load currents and thus it was expected that the voltage between sheaths under normal load conditions would be about 20 volts and the voltage to ground about 12 volts. Tests were carried out on lead electrodes and short lengths of cable sheath and the data were reported showing the loss in weight which might be expected for various current densities of alternating current. From this data it was the conclusion that this arbitrary value of 20 volts between sheaths set up from safety considerations was a value of potential so low that the expected sheath life of cable due to a-c electrolysis would far exceed the expected life of the cable due to other causes even when that cable was installed and operated in the presence of high-conductivity tidewater.

We have not experienced any failures due to a-c electrolysis during the period of years which we have operated single-conductor cable installed with cross-bonds as described in the article by Searing and Kirke. Some of these cables normally operate at voltages to ground in excess of 15 volts and are submerged in tidewater. I do not feel that voltage values of even 20 volts to ground would be a factor detrimental to the life of the cable due to any a-c electrolysis which might result even if the cables were operated submerged in high-conductivity water. If one considers the possible shock hazard involved it would seem quite likely that considerably higher differences of potential may be experienced on a system using sheath-bonding transformers.

L. F. Roehmann (Hastings-on-Hudson, N. Y.): I want to comment on Mr. Halperin's paper, on the section dealing with the cable movement. The cable engineers should be thankful to Mr. Halperin and his collaborators that they considered, for the first time, the restraining and frictional forces in studying cable movement due to temperature changes. The conditions are similar to those occurring in the rails of a railroad track which are also subjected to movement and to internal stress. In the cable technique these problems are, of course, of lesser importance than in railway engineering, but I am glad that they have been broached.

If one tries to check the formulas presented in Mr. Halperin's paper, one has some difficulties. Therefore I would appreciate it if Mr. Halperin would indicate, perhaps in an appendix, how and under which assumptions these formulas are derived, inasmuch as they form the base for the subsequent discussion of the experimental results.

Herman Halperin: The submitted discussion on this paper, as well as other discussions that have been given me, is greatly appreciated.

Some of the discussers apparently have missed one of the main points of the paper, which is that emergency ratings have become feasible, not because of mere conjecture, but because of a thorough engineering study which brought out the big increase in the quality of installed lines during the past 15 years, the moderate temperatures at which cables are usually operated, the indications of little or no deterioration in tests of various kinds of cable and of paper insulation by itself at temperatures much above those now allowed by the existing rules for cables, etc. Although it may have been wise 18 years ago (time of presentation of similar papers) to keep temperatures always below the limits given by the AIEE rules, it does not seem wise to do so now in view of the various large improvements since made by manufacturers and by utilities.

As is recognized in the paper, the use of the emergency ratings may result in moderate increases in maintenance and in failures, but even with these increases these items will be small as compared with what prevailed 15 years ago. The tendency for these items to increase will, of course, be counterbalanced by further improvements.

Another point that pertains particularly to the solid-type cables is the fact that emergency ratings based on temperatures greatly in excess of the temperatures permitted by existing rules are feasible provided the usual daily loading is moderate. If the daily loading is heavy, with the result that the cable operates at temperatures very close to the limits now allowed by the AIEE rules, and furthermore if the loading varies considerably during each 24 hours so as to result in large daily temperature ranges, then the margin of safety in the installed underground circuit is decreased. This means that the safe temperature for emergency operation becomes less than would be the case for moderate loading.

Wiseman points out that the operating companies have not collected much information on the operation of cable up to the present permissible temperatures. Of course, this is due to the fact that the permissible temperatures were applied to the rare emergency conditions, which resulted in building cable systems to operate normally at very low temperatures. In order to operate the cables nearer to the present permissible temperatures, it is necessary to permit the temperatures to exceed these limits during rare emergencies.

Apparently some discussers have not taken full cognizance of the indications, as they seem to my associates and me, that somewhat *higher* temperatures than given in the recommendations in the paper are safe for the insulation for emergency use according to the results of the *tests* and *field investigations* made in Chicago and elsewhere. Of course, as indicated in the paper, the critical limiting factor may be the sheath during the usual daily loading which corresponds to the increased maximum allowable loading.

Shanklin refers to the fact that the maximum allowable copper temperatures in Europe are lower than set by the American rules. Our studies of English and continental literature, as well as private information, do not disclose adequate technical explanation for these differences in allowable temperatures. In the successful tests on single-conductor cable at 150 kv to ground at Arnhem, Holland, the temperatures have been usually above the limits allowed by rules either in the United States or in Europe. Furthermore, it is of interest to note that when it comes to other equipment the maximum allowable temperatures in Europe are comparable to those allowed in the United States; and, in that connection, one frequently sees figures such as 100 or 110 degrees centigrade, which is much above the limiting temperatures set for cables in Europe.

It is of interest to note that Shanklin sees no objection to "the use of emergency overload ratings under favorable conditions and where performance can be watched closely." In the paper it was intended to adjust the emergency overload ratings to the field conditions as well as to limitations imposed by the insulation or sheath or both. If the manholes, for example, are small, then the effect of this condition must be taken into account.

Roehmann has requested the derivation of the formulas relating to cable movement. His interest, as well as the interest of others, in this general subject is gratifying, but nevertheless it seems desirable to omit taking the space to present this derivation because of the unusual amount of space already taken by the paper. I shall, however, be glad to send a copy of the derivation to anyone upon request.

Davis' objection to using the same coefficient of thermal expansion for cable as for bar copper (16.7×10^{-6}) seems to be based on erroneous assumptions. He derives a value of 14.5×10^{-6} on the assumptions (1) that the angle of lay of the strands in a conductor is 30 degrees and (2) that the expansion of the conductor is to be found by multiplying the *total length of the strand* by the expansion coefficient. The usual angle of lay of strands is nearer 12 degrees than 30 degrees, which would give a value of 16.34×10^{-6} for the coefficient when calculated by his method. This is only two per cent less than the value used in the paper. The second assumption is not in accordance with the usual practice of multiplying the axial length of the cable by the coefficient to get the expansion. The value given in the paper is correct when used in connection with the axial length of the cable, instead of the total length of the strand.

Regarding the discussion by Thomas as to the limiting induced sheath potential for single-conductor cables, the maximum allowable values set in Chicago were based on extensive laboratory and field tests, as indicated in reference 12 of the paper. For instance, two trial installations in the field were operated at 18 volts to ground in one case and 20 volts in the other case for $1^1/_2$–2 years, and corrosion was found in both cases. It therefore seemed to us from these and other data that normally the sheath potentials to ground should be considerably below 18 volts in order to avoid a-c electrolytic corrosion over a period of many years. It is recognized that in some instances such potentials may be safe, but once corrosion starts, mitigating measures may not stop it entirely. We have, therefore, set a limit of 11 volts to ground for usual daily loading, and periodic examinations of cable under water in manholes have indicated this limit to be reasonable.

When it comes to the personal safety angle in connection with sheath potentials, it should be recognized that the transient sheath voltages may be a matter of several kilovolts instead of just the 200 volts at generated frequency mentioned by Thomas. This fact, plus the fact that there is no considerable difference in the order of magnitude of the transient sheath potentials between bonding systems using the cross-bonding scheme or open-end bonding or sheath-bonding transformers, is given in the paper "Transient Voltages on Bonded Cable Sheaths" in the January 1935 issue of ELECTRICAL ENGINEERING. These transient potentials cause no harm to individuals nor to sheaths, and experience has demonstrated that they will cause no harm to insulators or other accessories provided the presence of the transients is recognized and adequate insulation is provided to withstand them.

Replying to Komives, careful training of cable in manholes has been quite effective in prolonging sheath life. We have standards set up for all types of manholes (straight, X, T, etc.) and all numbers of ducts. These are closely followed. The desired joint position is accurately indicated on the cable installation print for each job. In the case of manholes not conforming to standard, the cable training is worked out with the aid of a scale model of the manhole.

Regarding the effect of nonfluid joint compounds on the power factor of cable, we get high power factors in solid-type cable adjacent to joints filled with asphaltum-type compounds, but we have never had a failure ascribed to this cause. Nevertheless, as a matter of improvement, we are continuing to give consideration to insoluble compounds.

It has been found also in Chicago that thin joint oil entering solid-type cable has caused increased power factor of the cable insulation, due mainly to contamination of the oil by the varnish of the old-type varnished cambric used in existing joints. This action occurs essentially in manholes where the ambient temperatures are lower than in the ducts and, except for a few special cases, this cable will remain thermally stable to the highest temperatures mentioned in this paper.

The use of the taped joint decreased the possibility of bulging and collapsing of three-conductor joints by permitting the

Maximum Safe Operating Temperature for 15-Kv Paper-Insulated Cables

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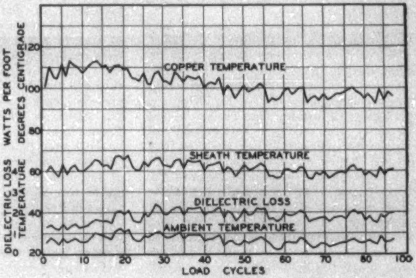
THE capital investment in the underground cable system of any large metropolitan utility is so great that any means of increasing its usefulness is of great and immediate interest. The operation of paper-insulated cables at higher copper temperatures than are accepted as normal, offers one obvious means of increasing the usefulness of the cable system where voltage regulation does not limit load.

Previous reports on thermal limits of paper insulation have been presented by Del Mar,[2] Torchio,[3] Roper,[4] Clark,[5] Fisher and Atkinson,[6] and Bush.[7] Electrical and physical characteristics of impregnated paper as affected by temperature were investigated at the Massachusetts Institute of Technology under the direction of a joint committee on research set up by the National Electric Light Association and the Association of Edison Illuminating Companies. This work was reported from time to time over the period between 1921 and 1929.

Figure 1. Load-test measurements at three phase, 13.6 kv, 60 cycles

Three - conductor 250,000-circular-mil 15-kv cable



About five years ago, the Edison Company in New York started a series of laboratory and field tests to learn the effect of intermittent operation of paper-insulated cables of the 15-kv class at temperatures above the level generally accepted as maximum for their class. This we believe is the only extensive field investigation of the kind that has been made on cable as a unit instead of on constituent parts of the cable and, for that reason, may be of interest.

Laboratory Test

Investigation was begun in January 1934 with laboratory tests on five 50-foot lengths of cable withdrawn from service. The cable was three-conductor round 250,000 - circular - mil paper - insulated belted - construction lead - covered cable which had been in service on the system for about 25 years. For about half of its life, prior to these tests, it was operated at 6.6 kv, 25 cycles, and thereafter at 11 kv, 25 cycles. The cable has rosin-compound-impregnated Manila-paper insulation, 170 mils conductor insulation, 155 mils belt insulation, jute fillers, and 125 mils lead. Examination of samples prior to the testing showed the paper tapes to be wide and badly wrinkled, dry, lifeless, and covered with powdered rosin. A few of the sections examined, however, showed the paper tapes to be fairly well saturated.

The laboratory test connections were arranged so that load current could be circulated in each phase conductor and a three-phase test potential of 13.4 kv, 60 cycles, applied continuously. The load currents were applied for a period of eight hours during each working day and then removed for the remaining period. The values of the current were adjusted to obtain different maximum copper temperatures.

The potheads used on the first sample of cable to be tested were filled with petrolatum. Some of the potheads used were not of an oil-tight type and compound was forced out during the loading cycles. Potheads on the second sample were filled with paraffin in order to see what effect this compound might have

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Paper number 39-70, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 25, 1938; made available for preprinting January 3, 1939.

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The authors gratefully acknowledge the co-operation of the many individuals in the operating, research, test, and engineering divisions of the company who collectively made possible the carrying on of this study.

[1] For all numbered references, see list at end of paper.

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use of a substantially smaller diameter of sleeve than was feasible with the cell-type joints formerly used.

We do not know exactly what effect the ratio of cable diameter to duct size has on cable movement at the manhole. Our data do not show that there is any definite correlation. We would expect less movement in larger ducts. It is difficult to get significant data on this point because we do not have the larger "stiff" cables in very large ducts which could be studied for comparison with similar cable in normal-sized ducts. We do have cables that are small compared with the duct size, such as 4/0 four-conductor four-kv cable in $3^1/_2$-inch duct, but such cable is relatively flexible, and the ratio of copper cross-section to total weight is low.

Our studies on cable movement and related problems are continuing.

Wiseman, in referring to part III of the paper, inquires as to why the maximum three-hour average load is used in determining the load factor. Step-by-step calculations have been made in the past to determine the maximum temperature based on readings of the current taken every half hour, and it has been found that the resultant temperature is very close to what is obtained by using for Chicago conditions the simple arithmetical average load over the peak three-hour period.

He refers to the possibility of inaccuracy in taking the air temperatures in connection with figures 19 and 20. It may be noted that considerable care was exercised in taking these readings. In fact, the care was greater than may be expected from a field crew when taking temperatures of occupied ducts by the usual methods. As to his further remarks on thermal emissivity, we have found it reasonably correct (see figure 20) to use an emissivity constant approaching 1,200 and have the calculated thermal drop represent the thermal drop from the sheath to the air in the adjacent warmest unoccupied duct.

Table I. Classification of Troubles, Long-Time Test on Three-Conductor 250,000-Circular-Mil Cable

Cause of Trouble	During Test		After Test		Total	
	Number	Per Cent	Number	Per Cent	Number	Per Cent
Cable						
Dielectric failure	16	54	2	17	18	43
Lead-sheath failure	7	23	6	50	13	31
Miscellaneous	1	3	2	17	3	7
Unknown	1	3	1	8	2	5
Joints						
All	5	17	1	8	6	14
Total	30	100	12	100	42	100

on the cable under test because some of the joints on this cable in the field were filled with paraffin. The potheads installed on the remaining three samples were filled with an oil-insoluble hard compound. This prevented the migration of compound into or from the cable during loading cycles. At the end of each loading cycle dielectric power loss, voltage, charging current, and sheath and ambient temperatures were measured.

The curve shown in figure 1 is typical of the values measured on the test samples. The sample for which data are shown in figure 2 failed before the end of the 16th load cycle. An attempt was being made on this sample to operate it at a copper temperature of 140 degrees centigrade. This temperature was at the borderline of thermal instability and slight changes in the load current resulted in a runaway condition and failure.

Examination of this cable at the end of the tests showed severe carbonization. The tapes near the conductor were reduced almost to ash for the entire length of the sample. The cables used for the other tests showed signs of carbonization in each case where the temperature had exceeded 130 degrees centigrade to any extent during the test. One sample whose temperature varied between 120 degrees and 130 degrees centigrade showed no signs of carbonization. The only sample whose temperature never exceeded 115 degrees centigrade also showed no signs of carbonization. At the end of the test, the paper tapes near the conductor on this sample seemed somewhat drier and more brittle than at the beginning of the test. This was the only noticeable change.

Table II. Failure Rates by Manufacturers, Long-Time Test on Three-Conductor 250,000-Circular-Mil Cable

Manufacturer	Sections	Section-Years During Test	Failures per 100 Section-Years	
			During Test	After Test
A	40	95	5.3	4.5
B	22	27	3.7	0
C	22	36	30.0	6.3
Other	21	21	0	5.0
Unknown	15	20	25.0	20.0

The conclusions drawn from these tests were that the practical limit for safe maximum operating temperatures in the field should be 100 degrees centigrade. It was also concluded that if any failures were to occur as a result of cumulative heating from high dielectric loss, they would occur within the first few load cycles, since the trend of dielectric loss showed little tendency to change after the first few test load cycles.

Long-Time Field Tests on Old-Type Cable

The conclusions based on four months of laboratory operation could be verified only by similar tests carried out under field conditions over a longer period of time. Four 11-kv 25-cycle feeders were selected for the field test. A temperature survey was made along the routes of these feeders in order to determine the existing empty-duct temperatures. These values ranged from 30 to 40 degrees centigrade. Based on this information, a value of current was calculated which would result in a 100-degree-centigrade copper temperature for this cable having dielectric loss characteristics as determined from laboratory tests and for an assumed average empty-duct temperature of 35 degrees centigrade. The computed value of current was 290 amperes and this was the assigned value of loading to be carried on the cable in these feeders. The field tests on the four feeders started in July 1934. These feeders contain a total of about 6½ miles of old 250,000-circular-mil cable.

The feeders under test supplied d-c substations from a generating station. Daily load cycles of 290 amperes on each feeder over an eight-hour period were obtained by using substation tie feeders to transfer load to and control load on the feeders under test. The test loads were not applied on week ends or holidays, and there were a few short intervals when operating conditions necessitated the omission of the test loads. The week-end periods were utilized to make kenotron tests at 20 kv to ground for five minutes in order to determine whether the insulation resistance changed as indicated by the milliampere leakage current during the test. The values of current measured during these tests show very little change and could probably be accounted for due to variations in temperature. It was further felt that the test might serve a useful purpose in developing incipient failures and thereby avoid some operating failures.

Due to the variations in duct constants and heating from other underground structures, the actual copper temperatures which resulted had maximum values ranging from 78 degrees centigrade to 133 degrees centigrade as determined from calculations based on measured empty duct temperatures obtained at various locations during the testing period.

The last of the original four long-time test feeders containing the three-conductor 250,000-circular-mil cable was taken off test in February 1937, about 2½ years after the test began. The other three feeders had been on test for 1, 1½, and 2 years, respectively.

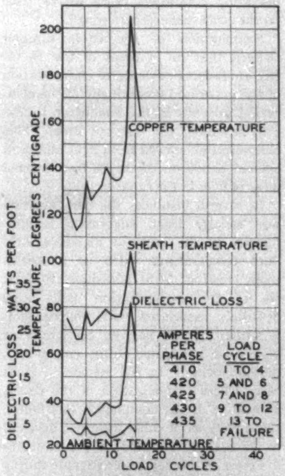

Figure 2. Load-test measurements at three phase, 13.6 kv, 60 cycles

Three-conductor 250,000-circular-mil 15-kv cable

During the testing period a total of 30 cases of failure and removal before failure had developed on the 250,000-circular-mil cable in these feeders. Between the period in which these cables had been removed from test and November 1938, 12 additional failures occurred. A summary of the classification of this trouble is shown in table I.

The cable in the feeders under test was made up principally of three different makes. The cable of one manufacture had an extremely high rate of dielectric failure compared to the other makes of cable. Comparison of cables by manufacturers is shown in table II. The various makes of cable were distributed among the four feeders tested as shown in table III and for comparison the range of duct temperatures in which these feeders operated is shown below the respective feeder numbers. While the one make of cable which showed a very high rate of dielectric failure was confined principally to one feeder, this feeder was installed in duct banks which had lower empty-duct temperatures than the other feeders. It does not seem probable that the high failure rate was due to operating conditions more severe than on the other three feeders tested.

Examination of the cable samples showed that in 14 cases of the 18 dielectric failures evidence of carbonization of the paper and occasionally of the compound appears in the conductor insulation adjoining the burnout area. This carbonization ranged from light and dark colored bands on the tapes to a charred mass of filler in the cable crotch.

In several instances the carbonization was strictly local, suggesting nonuniformity in the cable or excessive local duct temperatures, such as a steam-main crossing might occasion. In fact, six of the carbonization cases were definitely associated with copper temperatures of 110 degrees centigrade or higher as calculated from empty-duct measurements. In the remaining eight cases where carbonization of the paper, as well as of the compound, was found, these sections of cable were not believed to have been operated at temperatures in excess of 100 degrees centigrade during the loading test.

In order that the investigation be not confined solely to those sections of cable which had failed, six sections from these feeders were removed before failure for visual examination of dissected specimens and laboratory tests to determine dielectric loss and thermal resistance of the insulation. The sections selected had been subjected to from 225 to 366 cycles of test loading. There were specimens of each of the three different makes of cable. Physical inspections showed no marked difference in the appearance of the insulation made by any of the manufacturers. Varying degrees of saturation were found but most of the insulation was fairly dry. The compound was frequently found to have become discolored and hardened, particularly next to the conductor. In two instances flecks of carbonized compound were discovered but in one of these cases the carbonized specks occurred at a point in the cable which had been at a higher temperature caused by a steam main. No carbonized paper was found.

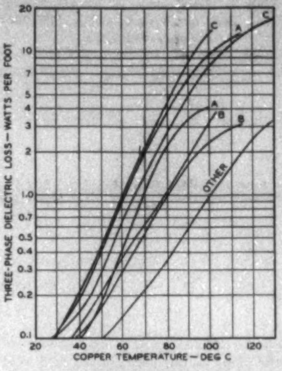

Figure 3. Dielectric loss measurements, 13.6 kv, 60 cycles

Three-conductor 250,000-circular-mil 15-kv cable

The results of laboratory tests on these removed samples of cable showed that their measured thermal resistivity was from 8 to 24 per cent above the value of 700 degrees centigrade per watt per centimeter cube which value had been used in computing the rated current for the load tests. While these test values were higher in each case than those measured on cable samples removed prior to the loading tests no change was made in the value previously used in computing copper temperatures. It is quite possible that copper temperatures may have been a few degrees higher or lower in some instances than those calculated. The dielectric-loss measurements on these samples were different in some cases than the values obtained on samples preceding the starting of load testing at which time a dielectric loss of one watt per foot had been assumed for a copper temperature of 100 degrees centigrade. Typical measured values of dielectric loss for these removed samples are shown in figure 3. These values are shown to indicate the relative variations between makes of cable. The actual dielectric loss when operated at 11.4 kv, 25 cycles, would be approximately one-half of these values. It will be noted that the samples of cable of manufacturer *C* are among the highest values for each temperature. This was the make of cable which had the highest dielectric-failure rate.

Table III. Distribution of Failures in Feeders, Long-Time Test on Three-Conductor 250,000-Circular-Mil Cable

	Number of Sections Tested and Number of Faulty Sections							
	Feeder No. 1		Feeder No. 2		Feeder No. 3		Feeder No. 4	
Manufacturer	Tested	Failed	Tested	Failed	Tested	Failed	Tested	Failed
A	16	1	11	2	4	0	18	2
B	3	0	2	1	16	0	1	0
C	16	13	3	1	3	0	0	0
Other	0	0	1	0	20	0	0	0
Unknown	0	0	2	2	11	2	2	1
Average empty-duct temperature	31		35		35		30	
Maximum empty-duct temperature	37		37		38		42	

Several specimens of 250,000-circular-mil cable from a faulty feeder which had not been subjected to load tests were examined. Striations of carbonized paper and compound were noticed near the fault and evidences of carbonized paper near the conductor were found at several other locations. The presumption is that this portion of the feeder had been subjected to overloads at some previous time. The presence of carbonized paper in these samples serves as a warning that the test load cycles must not be charged too readily with the full responsibility for similar conditions in samples from the tested feeders.

Long-Time Field Tests on Modern-Type Cable

After nine months of load cycle testing on old 250,000-circular-mil cable, it was decided that similar long-time tests should be started on a small amount of three-conductor 600,000-circular-mil shielded cable, 220 mils insulation, 125 mils lead, in order that it might be pos-

Table IV. Classification of Trouble During Test, Long-Time Tests on Three-Conductor 600,000-Circular-Mil Cable

Cause of Trouble	Number	Per Cent
Cable		
Dielectric failure	0	0
Lead-sheath failure	8	62
Miscellaneous	2	15
Joints		
Cracked sleeves	3	23
Total	13	100

Table V. Distribution of Failures as to Time of Occurrence, Three-Conductor 250,000-Circular-Mil Cable on Short-Time Tests

	Number of Failures	Per Cent
During test period		
Preliminary kenotron test	3	3
During loading test	21	24
Final kenotron test	7	8
	31	35
After load testing		
Operating failures	22	25
Test failures	29	34
Removals before failure	5	6
	56	65
Total	87	100

sible to determine whether or not the effects of these heavy loads on modern-type cable is different from the effect on the older type of cable.

Such a test was started in April, 1935, on two miles of this cable in one feeder which had been in service about five years. The cable had been purchased in 1929 and 1930. Joints on this cable were filled with petrolatum. The feeder containing this cable was tested until August 1936 and again from October 1937 to May 1938, a period of about two years. More of this type of cable, 4,000 feet long, was put on test in December 1937 and is still under test. The interruption of the test on the first feeder was made necessary by operating conditions.

During the period of these tests a total of 13 troubles have developed. One trouble developed on this cable during the period when the feeder was temporarily removed from test. The causes of the troubles which occurred on this cable during the tests are shown in table IV.

The trouble rate on this cable during the period that the test was in progress was high compared with similar cable not under test. During the interval the test was discontinued, there was a marked decrease in failures in this cable.

An examination of the sheath and insulation was made on all cable removed from service. The sheath in some cases was slightly swollen but not excessive as might be attributed to a "ratcheting" action as occasioned by successive load cycles. The cable insulation in all cases was in good condition and showed no signs of carbonization. The troubles caused by cracked and swollen joints cannot be attributed definitely to the load test as the joints in manholes were not inspected prior to the test and several similar cases have been found on cable operated at normal copper temperature. It seems quite probable that a great number of sheath failures can be directly attributed to the test loading.

Short-Time Field Tests

After about nine months' experience with cyclic loading to 100 degrees centigrade copper temperature on the four feeders containing 250,000-circular-mil cable, it was found that most of the failures had been due to dielectric heating, and relatively few were due to mechanical trouble. It was tentatively decided to rate cable of this type and vintage for emergency operation at current values corresponding to 100 degrees centigrade copper temperature. The feeders containing such cable were for the most part made up with cable having larger-size conductors than 250,000 circular mils, and it was felt that these higher operating temperatures could be tolerated, even should they cause increased operating failures, in order not to unduly limit the capacity of the feeders.

Beginning April 1935, a series of short-time field tests was begun. This was undertaken because it was felt undesirable to increase the rating of some 74 feeders containing three-conductor 250,000-circular-mil cable similar to that in the long-time test feeders without attempting to precipitate incipient failures which might otherwise occur at a more inconvenient time from the operating standpoint. Cyclic loads calculated to result in maximum copper temperatures of about 100 degrees centigrade were applied for eight hours each working day for two weeks. Sixty-six of a total of about 74 feeders were tested in this manner. The remaining feeders could not be tested because of operating difficulties in obtaining the desired loads. There is a total of 72 miles of three-conductor 250,000-circular-mil cable in the tested feeders. The ratings of about 50 feeders containing three-conductor, 250,000-circular-mil cable have been increased to a value of current corresponding to 100-degree-centigrade copper. It was proposed that the rating of the other feeders be increased later. Notwithstanding the increase in the rating, it was found in December 1937 that the loads actually carried had increased very little above those which these feeders carried prior to the testing. As a result, the operating record of these cables since the conclusion of the testing cannot be considered indicative of their operation at loads corresponding to 100 degrees centigrade. Eighty-seven cases of trouble have occurred on this type of cable in these 66 feeders during a period of $3^1/_2$ years. Sixty-nine of these were cable failures and 18 were joint failures. The distribution of failures as to time of occurrence is shown in table V. The ascribed causes of failure in cable are shown in table VI.

In two of the cases where the cause was ascribed to dielectric failure, the cable had carried loads far greater than the test load and in two other cases the faulty cable was found to have been near steam-main crossings which increased the duct temperatures to values which gave calculated copper temperatures over 100 degrees centigrade.

Carbonization of the insulation adjacent to the failure or in end samples was observed in 28 cases. The degree of this carbonization varied over the same wide range noted in the discussion of the long-time tests on this cable. The cable insulation in many of the remaining cases of failure was found to be generally desaturated and dry and carbonized compound was often found.

The results obtained on these tests

Table VI. Classification of Troubles, Short-Time Test on Three-Conductor 250,000-Circular-Mil Cable

	During Test*		After Test		Total	
Cause of Trouble	Number	Per Cent	Number	Per Cent	Number	Per Cent
Dielectric failure	20	80.0	24	54.5	44	63.7
Lead-sheath failure	2	8.0	12	27.3	14	20.3
Miscellaneous	3	12.0	4	9.1	7	10.2
Unknown			4	9.1	4	5.8
Total	25	100.0	44	100.0	69	100.0

* Includes failures due to kenotron tests which were made before and after the two-week loading periods.

Table VII. Comparison of Failure Rates

Failure Rates per 100 Mile-Years of Operation

Year	All 250,000-Circular-Mil Belted Cable			All Cable Operating at 11.4 Kv Except Three-Conductor 250,000-Circular-Mil Belted		
	Cable	Joint	Total	Cable	Joint	Total
1931	32.0	3.5	35.5	11.7	6.0	17.7
1932	22.9		22.9	9.0	4.5	13.5
1933	18.5	4.8	23.3	8.8	3.1	12.0
1934*				9.7	3.6	13.3
Jan.–May* 1934	15.9	4.4	41.8			
June–Dec.* 1934	38.8	3.0				
1935*	66.8	17.6	84.4	16.1	5.9	22.0
1936*	55.6	4.1	59.7	13.0	5.5	18.5
1937*	12.8**	9.5**	22.3**	12.4	2.7	15.1

* These rates include removals before failure. Long-time load tests were in progress on one or more feeders between July 1934 and February 1937. Short-time load tests were in progress on one or more feeders between April 1935 and July 1937.

** Rate obtained over period from May 1, 1937 to October 1, 1938.

indicate that the failure rate during the heavy-load testing was several times the failure rate usually experienced with that type of cable when operated normally. The failure rates of these same cables after conclusion of the testing was lower than the value during the testing and tended to approach that usually experienced with this type of cable as normally operated. The comparison of failure rates between old 250,000-circular-mil cable and all other cable operated at 11.4 kv is shown in table VII. A period of time was included to show the operating experience prior to and subsequent to the load testing. It will be noted that the vintage of cable tested had had a consistently higher failure rate than other cable before the test. During the test period, the failure rate increased markedly and at the conclusion of the tests it had closely approached the average of the other cables.

Conclusions

1. Operation over a continuing period of load cycles to copper temperatures of 100 degrees centigrade of old types of paper-insulated cables which were dry and contained rosin, results in a marked increase of dielectric failures.

2. Similar operation of modern well-impregnated paper-insulated cables resulted in no failures caused by dielectric heating or aging.

3. Both old types of cable and modern types of cable had an increase in the rate of failure due to wearing and cracking of the lead sheath when operated at these load cycles.

4. It is our opinion that the occasional operation of paper-insulated cables during emergency conditions to copper temperatures as high as 100 degrees centigrade can be justified where a small number of sections of cable in any feeder otherwise set up loading restrictions.

5. It is our opinion that those types of modern cable which provide means for taking care of the expansion and contraction of the cable compound, such as not to unduly stress the lead sheath, could be operated repeatedly to copper temperatures as high as 100 degrees centigrade.

6. It is our hope that others may be interested in carrying on similar operating experience on cable systems of this and other voltage classifications, in order to obtain a more widespread knowledge of practical operating limitations for paper-insulated cable.

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Discussion

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): It would be interesting to know the distribution of cable failures due both to the dielectric and to the lead sheath in regard to its location, that is, what percentage of failures were in the manhole, near the manhole in the duct, and in the duct section.

Conclusions drawn in the paper seem to be less enthusiastic than in the presentation by the author. However, in either case, the author and his company should be congratulated in taking such a bold step to find out the economical life of a highly loaded cable.

It seems to be a moot question whether the fact that an overloaded cable cannot be salvaged to be reused was taken into consideration when economical loading of cables was considered by the author.

G. B. Shanklin (General Electric Company, Schenectady, N. Y.): I desire to discuss briefly maximum safe operating temperature of underground cable as covered by the two outstanding papers presented by Franklin and Thomas, and Halperin.

Each of these two papers has made a valuable contribution to cable engineering. The paper by Franklin and Thomas is outstanding in giving us for the first time a systematic study of controlled overloading of cable under actual field conditions. I have always felt that this problem would never be solved in the laboratory and that practical knowledge could only be gained in the field, because of the large number of variable factors involved. It is not possible to reproduce these in the laboratory.

Mr. Halperin's paper is outstanding in giving us for the first time a systematic study of cable movement under load conditions in ducts. Now that Mr. Halperin has placed this problem on a scientific basis we need an extension of this information covering all possible conditions of field service. The severe duty requirements on lead sheath, especially with modern, well-impregnated cable, represents a limiting factor in determining maximum safe operating temperature for cable.

An analysis of the data records in both papers leads to the same important conclusion, which is, that when underground cables are loaded above what we now consider normal loads, there is a noticeable increase in the rate of service failures. This is true whether the cable is of the old poorly impregnated type or the modern well-impregnated type using thinner compound. Overloading of the older cable leads to dielectric loss failures and fatigue of the lead sheath due to cable movement. Overloading of modern cable causes an even greater burden on the lead sheath, and the majority of service failures are due to lead-sheath troubles. With modern cable, overloading causes greater stretching of lead sheath which not only adds to the sheath troubles mentioned but aggravates migration of compound, leading to ionization damage in the drained sections of cable. Thinner insulation and higher working stresses in modern cable do not allow pronounced drainage of this kind, and this can only be avoided by keeping the operating temperatures within reason. The same lesson has been learned in Europe, and maximum allowable copper temperature is invariably lower than allowed by our present standard rules.

At the AIEE winter convention in 1921 a symposium of six papers was presented on exactly the same subject. In discussing these papers the present writer summarized the various factors involved and then made the following statement:

> When these factors are considered, it is apparent that the temperature limit of (low-voltage) paper cable insulation should be something less than 105 degrees centigrade. If the temperature limit of 85 degrees centigrade is to be raised, this change must be approached with caution and an increase to 90 degrees or 95 degrees centigrade is all that should be attempted until more is known of the subject.

Thanks to the authors of these two papers and the experience we have all gained since 1921, we now know more about this subject. Nevertheless, the above statement still holds. The margin for temperature increase to take care of emergency loads is without question quite narrow, and the data records in the two papers by Franklin and Thomas, and Halperin show that an increased rate of service failures must be accepted.

The writer does not believe that the cable industry is yet in a position to draw up safe emergency overload ratings for cable that approach the temperature limits suggested by these two papers, in the order of 100 degrees centigrade for 15-kv cable. This will definitely mean an increase in service failures. The burden on the lead sheath is greater than it used to be. If this is a limiting factor, as indicated by these two papers, we must know more about the general conditions of cable movement and training of cables in manholes. Many of these manholes around the country are relatively small and extremely crowded, with short radius of bend on the cable. In drawing up national standard rules we must protect both the users and the manufacturers who must meet these kinds of conditions. This in no way precludes the use of emergency overload ratings under favorable conditions and where performance can be watched closely. These special conditions cannot very well be included, however, in standard rules covering all possible field conditions.

Temperature increases for emergency load rating as proposed by Mr. Halperin are outlined in table X of his paper. In my opinion the proposed temperatures should be decreased by at least five degrees centigrade in some cases and by ten degrees centigrade in other cases for safe use around the country. This would mean that there would be no margin at all for 66-kv single-conductor cable. The present normal load limit of 60 degrees centigrade for cable of this voltage rating is already too high.

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): The papers by Franklin and Thomas and Halperin present a mass of data tending to show that the present standard temperature limits for paper-insulated cables are not sufficiently high, as far as the dielectric is concerned but, if materially exceeded, may lead to early sheath and joint trouble. Such developments as may occur in solid-type cable in the near future are more likely to increase the stability of the dielectric than the life of the lead and, therefore, will not raise the temperature limit.

Moreover, some effects not anticipated may limit the tendency toward higher temperatures such as, for instance, the cumulative effect of soil drying around duct lines operated at very high temperatures or the eventual rise in power factor which must result from ionization during cold periods of the exaggerated load cycle. It must be remembered that the tests were at rated voltage, not under accelerated aging conditions as far as voltage is concerned, and that a two-year test only indicates what will happen in two years, not five or ten years. Accelerated aging tests at voltages well above those of normal operation do not show much change in power factor or hot-spot-temperature development in the early stages but, at some unexpected moment, start up rapidly, as a prelude to failure.

J. A. McHugh (Consolidated Edison Company of New York, Inc., New York): Outstanding features of this paper appear to be the importance of the condition and mechanical construction of paper insulated cables in determining their ratings.

1. The cables of the vintage of a quarter century ago show, as the authors point out, a wide difference of present condition, overloads sustained in more rigorous years being a serious factor. Perhaps distention of the sheath and absorption of excess compound by the paper are causes of their high dielectric loss. The use of the load cycle test at high temperature appears, from the data of table V, to be effective in precipitating voltage-test failures and removing the poorer cable from service at convenient intervals.

2. The modern cables, of essentially "hard" core construction, are limited in their ratings by the sheath, as the authors have indicated in table IV.

3. Conclusion 5 regarding modern cable which provides means for taking care of the expansion and contraction of the cable compound is inevitable. Distinction might well be made between constructions which simply control the internal hydrostatic pressure and those which also maintain continuous impregnation, in further investigations.

It is to be hoped that this investigation, so logically developed, will be helpful in increasing the economic availability of the improved cable constructions.

E. W. Davis (Simplex Wire and Cable Company, Cambridge, Mass.): Conclusion number 4 states that 100 degrees centigrade is justifiable for occasional operation. It is interesting to notice how closely this represents the general opinion of 18 years ago in 1921, as indicated by the AIEE symposium on this subject at the winter convention of that year, particularly if allowance is made for the reasonable differences of opinion that existed then.

The authors' test results indicate that the principal limitation to more severe service is sheath trouble rather than actual temperature deterioration of the paper. This is further reason for the present-day tendency to improve the quality of lead sheath and to develop superior types of sheath where possible.

In connection with the interesting superiority of the new cable over the old we are wondering to what extent this is due to the fact that the new cable was type *H* and the old was belted. Other things being equal, type *H* cable is somewhat superior to belted in allowable operating temperature.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The information, given in this paper, on trials in the laboratory and in service of unusually high operating temperatures for high-voltage cables, constitutes an important and comprehensive contribution on the subject.

The laboratory tests show that temperatures over 130 degrees centigrade are too high for these old rosin-impregnated transmission cables. However, this value is considerably above the present limit of about 75 degrees centigrade for the cable involved, and much can be gained by using some limit between these two values.

In the field tests the authors assumed that the dielectric losses amounted to one watt per foot although their figure 3 shows much higher measured values. Also, the authors found that the thermal resistivities of the insulation were from 8 to 24 per cent higher than the values that they had assumed.

The authors state that in eight cases carbonized paper was found in cable that was believed to have operated at temperatures less than 100 degrees centigrade. Could not these temperatures have been much higher, also, due to higher values of dielectric loss and thermal resistivity than those assumed?

The paper indicates that high rates of trouble occurred during the overload tests but that some weeding out of weak spots occurred with a decrease in troubles for a short time after the tests. The authors seem to suggest that the weak spots be eliminated by an overload test before the emergency ratings are increased. The emergencies during which the temperatures might approach the limits occur at very infrequent intervals. Probably by the time the occasion arrived, other weak spots would have developed. To be effective, then, the elimination testing would be necessary at regular periods.

Trials of higher temperatures of the kind discussed constitute an important step toward more economic use of underground cables. For full effective use of the results, more detailed analysis of the troubles experienced seem necessary, especially in regard to the increases in troubles due to ionization, dielectric losses, sheath cracks, etc., incidental to operation at the increased temperatures.

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York): In support of the data presented in substantiation of the suggested higher operating temperature for paper-insulated cable, I should like to present some data secured about five years ago in connection with a rather extensive investigation of 27-kv paper-insulated cables. The particular study was initiated for the purpose of determining the practicability of reducing insulation thicknesses but as it progressed it became apparent that the most valuable information was concerned with operating temperature limits. The cables were three-conductor 350,000-circular-mil shielded type with 280 mils insulation. Samples were made by two manufacturers each of whom supplied approximately 4,000 feet. Cables for test were installed in the street and in addition 100-foot lengths were tested by load cycle procedure in the laboratory. The number of daily load cycles ranged from 109 to 114. Maximum load temperatures varied from 70–75 degrees centigrade to 100–105 degrees centigrade with a few load cycles falling within other temperature ranges. The principal criteria of cable performance were the increase in dielectric loss and the reduction in life of residual samples when tested at room temperature and four times rated voltage. An examination of the detailed test results shows that the room temperature dielectric loss was practically unchanged as a result of this load cycle testing. There was a progressive increase in the dielectric loss at elevated temperatures but an analysis of the data has failed to show any clear cut difference in the rate of increase during the 60 load cycles in temperature range 70–75 degrees centigrade as compared with the 35 or 40 load cycles when temperature ranged 100–105 degrees centigrade. Such differences as may exist are obscured by the inevitable variations in observed values.

After completing the load cycle tests, five-foot specimens were removed and tested to destruction at four times rated voltage to obtain a measure of the comparative residual life. For one make of cable, the duration to failure was 4.5 hours as compared with more than 570 hours for samples that had not been subjected to load-cycle testing. For this make of cable, the dielectric loss at 80 degrees centigrade had increased to a value 90 per cent above that obtained on tests made at the factory. For the other make, the life of samples after load cycle aging was 11 hours as compared with 40 hours for cable which had not been subjected to the test and the dielectric loss at 80 degrees centigrade had increased sixfold.

C. W. Franklin and E. R. Thomas: In reference to the question of L. I. Komives regarding the distribution of cable failures as to location of failure, 85 per cent of the dielectric failures occurred in the duct, 10 per cent in the manhole, and the remaining at the duct edge or unknown. Similarly 51 per cent of the sheath failures occurred in the manhole, 40 per cent at the duct edge, and the remaining in the duct.

We are in agreement with E. W. Davis that sheath troubles constitute one of the major limitations in the operation of solid-type paper-insulated cables and that everything possible should be done to improve the quality of cable sheaths. While the dielectric failures found during the heavy load testing were confined entirely to old belted cable, and while age and type of cable both probably were contributing factors, we feel that the age of the cable more than the type of cable was the major contributor to the dielectric failures. The impregnating compound used in cables of the vintages tested had dielectric loss characteristics which rose very sharply with temperature and thus tended to make for greater instability of cable operation at the higher temperature levels.

In reference to Herman Halperin's comments regarding the dielectric loss values used, the test values shown in figure 3 were measured at 13.6 kv, 60 cycles, while the cable under test in the field was operating at 11 kv, 25 cycles. The average dielectric losses assumed to be about one watt per foot at the operating conditions are believed to be in agreement with data obtained on test lengths. The eight cases where carbonized paper was found in cable which was believed to have operated at temperatures less than 100 degrees centigrade are based on assumed values of dielectric loss and thermal resistivities. It is recognized that deviations in these latter quantities may easily have been of the order of ten per cent. Considering these deviations and measured duct temperatures we believe that during the test period these particular cables had not exceeded the 100 degrees centigrade copper temperature. It is quite probable that these cables some time during previous periods of operation may have been subjected to loads which might have caused carbonization of paper to develop. The use of the high-load tests for a short time on the various feeders was believed to be a satisfactory way of eliminating those sections of cable having unstable dielectric-loss characteristics when operated at copper temperatures of 100 degrees centigrade. The test, however, was not intended to determine the thermal limitations along the particular feeder as these had previously been checked by temperature survey. We did feel that this method of test would show up existing unstable cable and that it would not necessarily have to be repeated at any particular period unless it was felt that there was progressive continued deterioration going on in the cable on the feeder.

The data on aging tests conducted on 27-kv cable which W. F. Davidson reported is interesting in showing that the elevated temperature operation did not tend to show a decrease in dielectric stability.

In answer to W. A. Del Mar, we have not experienced the effect of soil drying around our duct lines in such a manner as to create an unstable thermal condition for the duct bank. The fact that these load-cycle aging tests were conducted at normal voltage seems to us to be more convincing evidence of the cable operation ability than short-time tests at overvoltages.

We are in agreement with G. B. Shanklin that paper-insulated cable can well be operated at temperatures of from 90 to 100 degrees centigrade without probable injury to the dielectric but that cable-sheath design and technique of manufacture constitutes the greatest drawback to operating our existing cables at these temperatures.

In closing, we feel that as the result of the papers presented on cable operation at the higher temperatures and the resulting discussion of these papers, we come to the generalization that oil-impregnated paper insulation as used in modern cables is not in itself affected when operated at temperatures up to 100 degrees centigrade but when insulation is incorporated as an integral part of cable it may be subjected to some increase in deterioration due to resulting void spaces which may form due to heating and cooling cycles. The sheath problem, on the other hand, is one of a material limited in mechanical strength and affected more from stresses set up in it due to changes in loading than to the magnitude of the temperature. A satisfactory solution of our cable-sheath problem is probably of more immediate concern in securing a more economical high-voltage cable than the development of improvements in oil and paper dielectric.

Voltage Control of Mercury-Arc Rectifiers

G. R. McDONALD
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THE mercury-arc rectifier competes with the older well-established types of apparatus, the synchronous converter and motor generator set. Both of these types of apparatus are flexible in application as they may be adjusted through a range of different output characteristics and in the case of the booster-type converter and motor generator set, considerable range in voltage control is possible. In order to take full advantage of the desirable features of the mercury-arc rectifier, its output voltage should duplicate that of its rivals.

The output of a rectifier and its associated transformer without any special means of modifying or regulating the voltage is similar to that of a shunt-connected generator in that an increase in load causes a reduction in the direct voltage. The characteristic is a straight line from light load to the rated capacity of the equipment. The slope of this characteristic is dependent upon the type of transformer connections used, the reactance and resistance of the transformer and supply system, and the arc-drop characteristic of the rectifier.[1]

The importance of the flat-compound characteristic has long been recognized particularly in the railway field. One of the earliest attempts at modifying the voltage output of a rectifier consisted of a scheme to produce the flat compound characteristic.[2] In figure 1 a commonly used rectifier circuit is shown, with the exception that the interphase transformer is provided with a saturating winding. If this transformer and its winding were short-circuited by the dotted connections x and y, a six-phase diametric connection would be obtained having an output characteristic as shown by AB.

If the dotted connection x is now removed the interphase transformer is placed in operation and six-phase operation is still obtained but the resulting connection is known as double three phase, the two wyes being forced to operate in multiple by the interphase transformer. The output of the rectifier is still six phase as the two three-phase systems are displaced from each other by 60 degrees. This characteristic is shown by ACD.

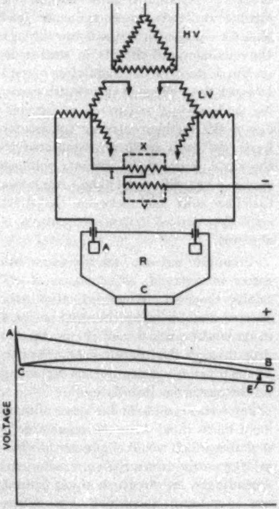

Figure 1

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
R—Rectifier
A—Main anode
C—Cathode

It is apparent that any gradual means of making the interphase transformers ineffective with an increase in load would produce a characteristic similar to ACE. If the short circuit across the interphase saturating winding is now removed and the interphase transformer is made ineffective by saturation in proportion to the d-c output, the desired compound characteristic would be obtained.

An equipment of this kind was described in the TRANSACTIONS of the AIEE. The actual characteristic obtained was shown in the article.[3]

The circuit for compounding described above has the disadvantage of employing saturation of the magnetic circuits, making it difficult to obtain a smooth curve of the desired shape. In addition it causes a poor power factor. To overcome these difficulties a more flexible scheme of compounding was developed as shown in figure 2. In this figure the interphase transformer has connected across it a circuit made up of a saturating reactor and capacitor. The capacitors used in this way advance the firing period of the rectifier anodes, thus compensating for the reactive part of the normal rectifier regulation. The point at which the capacitors have the maximum effectiveness is determined by the constants of the circuit. The advantages of this circuit are: increased flexibility in adjustment, better utilization of the main transformer windings, improvement in power factor, and decreased losses.

A simple way of changing a rectifier direct output voltage is to vary the alternating voltage applied to the rectifier. This can be accomplished by any of the commonly used methods for regulating or changing the voltage of an a-c circuit. In general, however, this type of voltage control is not suitable with rapidly fluctuating loads.

With progress in the art of rectifier design, control grids were introduced which were capable of modifying the starting point of the arc from the rectifier main anodes. By delaying the firing of the anodes, a reduction in the output voltage is possible. Voltage regulation by this means has been fully described in a number of articles.[5-7] This type of regulation is very flexible and any desired output voltage can be produced providing the desired voltage lies below the

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Paper number 39-11, recommended by the AIEE committee on transportation, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 17, 1938; made available for preprinting December 5, 1938.

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1. For all numbered references, see list at end of paper.

natural output characteristic of the rectifier.

This type of voltage regulation imposes some additional duty on the main anodes. It makes no change in the main transformer but does cause an increase in the size of the interphase transformer if used. One of the main objections to excessive use of voltage reduction by grid control is the distortion which it produces in the a-c power supply system and the d-c output of the rectifier.

In order to produce this type of regulation it is necessary to obtain the proper sequence in making the grids positive with respect to the voltage of the rectifier cathode. One of the simplest means of doing this is by a Selsyn phase-shifting transformer as shown by figure 3.

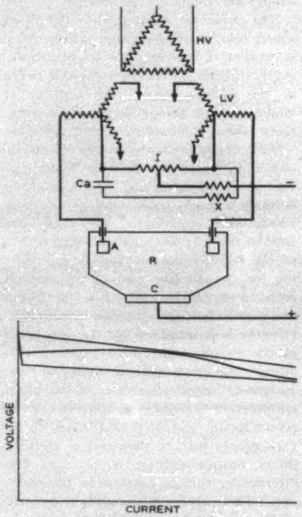

Figure 2

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
Ca—Capacitor
X—D-c saturating reactor
R—Rectifier
A—Anode
C—Cathode

The normal unregulated characteristic of the rectifier is shown by the curve *ABC*. When the Selsyn transformer is adjusted to retard the firing of the anodes and left in position, a characteristic curve parallel to the normal characteristic of the rectifier is obtained but at a lower voltage such as *ADE*. A number of equipments have been installed with control of this type so that the voltage of the rectifiers could be manually adjusted.

A simple modification of the scheme shown in figure 3 is the addition of a voltage regulator equipment and motor drive for the Selsyn transformer. With this equipment the regulator serves to maintain constant direct voltage. This type of regulation has been used for a number of equipments operating under steady load conditions where rapid control of the direct voltage is not necessary.

By means of a winding on the voltage regulator, carrying direct current in proportion to the rectifier output, the voltage can be made to rise or fall with an increase in d-c load as desired.

Load balance coils on the regulators will also balance the load quite accurately between two or more equipments. Figure 4 illustrates one method of obtaining a directional indication of the magnitude of the load being carried by a rectifier. The load indication is taken from a current transformer in the supply to the rectifier. It will be noted that if one equipment tends to carry more load than the other a current will flow through the equalizing coil circuits in such a direction as to cause the regulators to tend to boost the voltage of the rectifier carrying the least load and to reduce the voltage of the rectifier carrying the greater load. As both units are connected to the same bus, their output voltages cannot be different. The regulators therefore shift load between the units until the desired balance in output is obtained.

Although current transformers are shown in figure 4, other means of obtaining the load balance indication may be used such as using the drop across a shunt in the rectifier load circuit, the d-c drop through the interphase transformer, or any other convenient means depending on the particular installation.

For railway and similar types of service a more rapid type of regulation is desirable which would eliminate the slow-moving motor-driven Selsyn transformer. Accordingly, the circuit shown in figure 5 was developed in which the Selsyn transformer is still retained but merely for making the initial phase-angle adjustments. The necessary phase shifting of the grid voltage with respect to the voltage of the main anodes is obtained by applying a d-c bias to the neutral of the Selsyn transformer. The biasing voltage is obtained from a small d-c generator, the output of which is controlled by the regulator. This does not produce any phase-angle shift of the a-c supply to the grids but by biasing the grid voltage there is, however, available a change in the time at which the grids go positive with respect to the cathode of approximately 90 degrees, although a much smaller shift is commonly used.

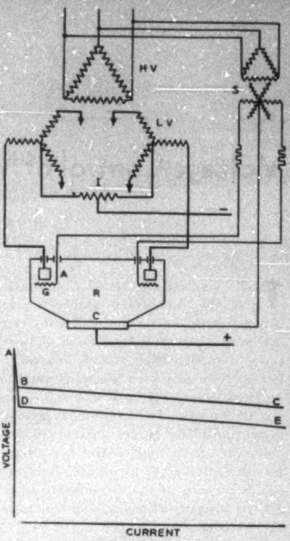

Figure 3

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
S—Selsyn phase-shifting transformer
R—Rectifier
A—Anode
G—Grid
C—Cathode

In figure 5 a biasing generator is connected between the cathode of the rectifier and the neutral of the six-phase grid transformer. This generator is provided with two separately excited shunt fields connected to oppose each other, one double the strength of the other. With this scheme of excitation the full range in voltage of the bias generator may be obtained in either direction. The output voltage of this generator is controlled by the regulator.

From the wave shapes shown in figure 5, keeping in mind that a rectifier anode cannot fire until its associated grid becomes positive with respect to the cathode, it will be noted that by means

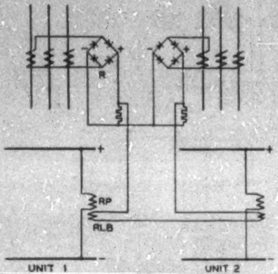

Figure 4

R—Copper-oxide rectifier
RP—Voltage regulator potential coil
RLB—Voltage regulator load balance coil

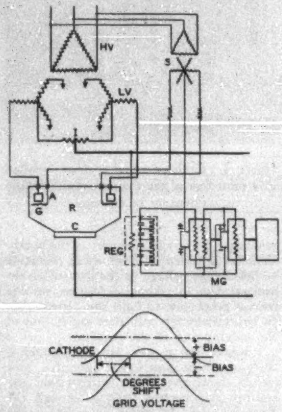

Figure 5

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
S—Selsyn transformer
R—Rectifier
A—Anode
G—Grid
C—Cathode
REG—Voltage regulator
MG—Grid-bias motor generator set

of this d-c bias there is a range of 90 degrees through which the firing of the main anode may be controlled. This gives a range from the normal unregulated voltage of the rectifier to approximately zero voltage if the load is sufficiently inductive to maintain the flow of current. This extreme range is not normally used but illustrates the flexibility of this type of control.

With this method a regulator can hold constant direct voltage or can be under- or overcompounded. The regulators can be equipped with load balance coils for balancing load between units.

The response of the rectifier to a change in grid voltage is practically instantaneous. This type of regulation is therefore very fast, the only delay being in the response of the regulator itself and in the ability of the separately-excited bias generator to change its voltage. There is no difficulty therefore in maintaining any desired voltage regardless of the fluctuations in d-c load demand.

As an illustration, this type of voltage control in railway service is being used with the equipment supplied for the electrification of the San Francisco-Oakland Bay Bridge.[8]

By substituting a current coil in place of the voltage coil of the regulator, the same scheme may be used to obtain a load-limiting effect; that is, as the current approaches limiting values, the voltage of the rectifier will be reduced to prevent any further increase in load. This scheme has been used in a number of installations. While they did not employ voltage control this feature together with load limiting could be applied by simply using a voltage regulator and a load regulator working on the same biasing generator.

This same type of control using a counter-electromotive-force generator has been used for voltage regulation of d-c generators. It is, therefore, comparatively simple to operate a d-c generator in multiple with a mercury arc rectifier with any desired load division between the units.

As noted above, grid control of rectifier voltage causes distortion in the a-c power supply and in the d-c output. The amount of this distortion increases with the amount of regulation. When long continued operation at reduced voltage is required, transformer tap changing under load and grid control forms a good combination. The a-c supply voltage is adjusted so that a minimum amount of voltage control with grids is required.

With the advent of the ignitor[9] a competing type of mercury-pool rectifier of the single anode type has become available and is being used quite extensively for various applications. An equipment of this type is made up of a number of separate tubes combined to give the equivalent of a multiple-anode rectifier. Each tube is fired in rotation by an ignitor which serves to start the arc. At the end of each conducting period the tube arc is extinguished and no ionization exists until the main anode again becomes positive with respect to the cathode and the ignitor is re-energized to provide ionization for starting the main anode.

Figure 6 illustrates the essentials of one method for controlling the operation of these tubes. The ignitor, a crystalline compound inserted into the cathode-pool mercury is energized momentarily each cycle to start the main anode. The operation during each cycle is as follows: When a main anode becomes positive with respect to its cathode the auxiliary vacuum tube is made conducting and current is passed through the ignitor. As soon as sufficient ionization is provided an arc is started between the cathode and grid of the power tube short-circuiting the ignitor circuit. This arc in turn starts the main anode. Voltage control is accomplished in the same way as with grid control by delaying the operation of the auxiliary vacuum tube.

The essential feature of this type of control is the provision of a momentary current supply to the ignitor for starting the main arc. In place of the scheme shown by figure 6, peaking transformers and other similar methods may be used, depending on the requirements of the application.

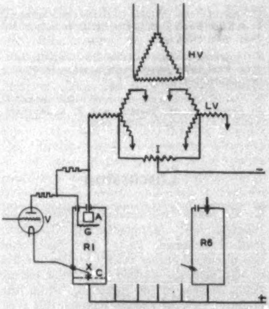

Figure 6

HV—Transformer high-voltage winding
LV—Transformer low-voltage winding
I—Interphase transformer
*R*1 to *R*6—Individual vacuum tubes
A—Anode
G—Grid
C—Cathode
X—Ignitor
V—Control tube

The foregoing outlines in general terms some of the progress made and some of the practical methods which may be used to control the output voltage of mercury-arc rectifiers. In general, equipment is now available to regulate the output of a rectifier which will provide the functions of: flat voltage control, under- or overcompound voltage control, voltage control and current limiting, current control, and control suitable for other special applications, such as the equivalent of Ward-Leonard control for starting purposes. The rectifier equipment is thus in most respects as flexible in application as other types of conversion apparatus. The type of equipment to be used, and its control, will depend upon the requirements of the particular installation.

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Discussion

G. F. Jones (nonmember; Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Mr. McDonald has discussed rather briefly voltage control of the ignitron rectifier, a typical circuit being shown in his figure 6. From the point of view of voltage control, this type of rectifier has distinct advantages which I believe should be emphasized.

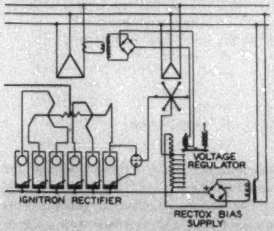

Figure 1. Ignitron rectifier circuit using voltage regulator for current limitation

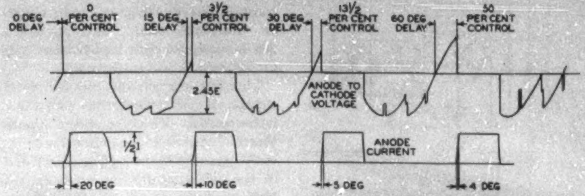

Figure 2. Showing anode-to-cathode voltage and anode currents in function of voltage control and angle of delay

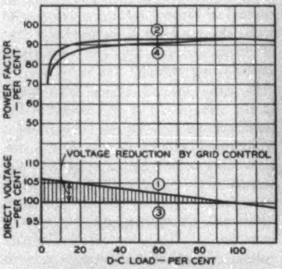

Figure 3. Voltage regulation and power factor of rectifiers with and without grid control

Curve 1—Voltage characteristic (drooping) without grid control

Curve 2—Power factor corresponding to curve 1

Curve 3—Voltage characteristic (level) obtained by grid control

Curve 4—Power factor corresponding to curve 3

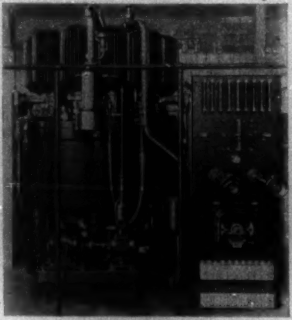

Figure 4. Grid-controlled rectifier installed at a substation of the Commonwealth Edison Company in Chicago

As mentioned in the contemporary paper by Mr. Cox and myself, the arc voltage drop of the ignitron is the same for a voltage-controlled and non-voltage-controlled unit. For the conventional rectifier, the presence of the control grids in the arc path increases the arc voltage drop and therefore lowers the efficiency.

The power required by the grids of the excitation tubes of the ignitron is only a fraction of that required by the control grids of the conventional rectifier. This low power requirement extends the flexibility of the apparatus considerably and simplifies the control apparatus. For example, Mr. McDonald's figure 5 shows a motor generator set as a source of bias voltage to effect voltage control of a conventional rectifier. For the ignitron, the corresponding source can be a small Rectox, thus eliminating rotating apparatus and its inherent response time lag. Figure 1 of this discussion shows the essential circuit for high-speed voltage control of an ignitron. The voltage regulator operates to limit the a-c input to the unit to a constant value when the power demand is excessive. A potentiometer is connected across the fixed bias voltage of the Rectox source. The resistance of the effective part of the potentiometer is varied by a Silverstat regulator to insert bias voltage in the neutral of the excitation-tube grid transformer in the correct proportion to limit the current rise. In this circuit the only time lag is that of the moving arm of the regulator.

I would like to ask Mr. McDonald if there is not an error in his figure 6. This circuit shows the ignitor current and the ignitron grid current passing through the same resistor. In order to conduct the ignitor current, this resistor must have a low value. Connected as shown this would permit excessive grid currents both during normal conduction and during arc back.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): It was very gratifying to see from this paper that another manufacturer of rectifiers has adopted the d-c bias grid voltage control and found that it offers many advantages. This method of control was used in connection with our grid-equipped rectifiers as far back as 1930 and has since then been incorporated in rectifiers for many different kinds of applications, as will be seen later on.

I would like to say a few words first in regard to some statements which I feel should be further amplified. For instance, it is said that the grid voltage control imposes some additional duty on the main anodes. It is true that the rectification phenomenon is made somewhat more difficult when the anodes are delayed in their firing due to the fact that the reverse voltage increases more rapidly immediately

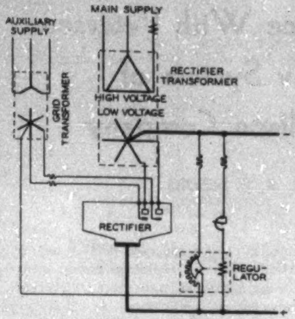

Figure 5. Diagram of connection

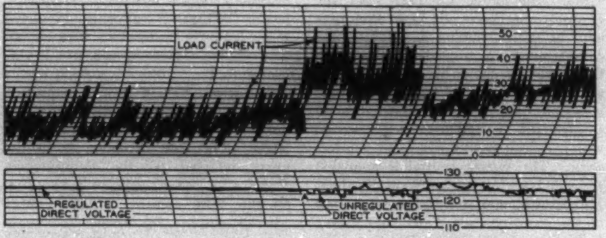

Figure 6. Load charts showing actual voltage regulation obtained with grid-controlled rectifier furnishing power for railway load

Current multiplier—80 Voltage multiplier—5

after commutation takes place, however, as can be seen from figure 2 of this discussion, the anode-to-cathode voltage remains the same in magnitude. Furthermore, the anode currents do not increase appreciably, and therefore a delay in firing does not affect the loading of the anode, see figure 2, which shows the anode current for different angles of delay and percentage of voltage control, respectively.

In most installations where only compounding of voltage is necessary, the reduction of voltage, and therefore the angle of delay is practically zero at full load, and becomes larger toward no load, as is shown in figure 3. Curve 1 shows the natural voltage characteristic of an uncontrolled rectifier, while curve 3 shows the flat voltage characteristic which can be obtained by means of grid control. In other words, the reverse voltage increases gradually as the rectifier load is decreased, and consequently there is no additional duty imposed on the anodes in such an application. In order to avoid delay in firing the anodes at full load the transformer taps are chosen accordingly. We therefore wonder to what kind of excess loading of anodes the author had reference.

The only other characteristics affected by grid voltage control and not mentioned by the author are power factors (see figure 3) and the wave-shape distortion of voltage and current of the primary as well as of the d-c system. These factors become quite important as soon as the amount of regulation of voltage is greater than the usual amount as required in railway service. In most applications, however, operation at greatly reduced voltages is not required, except for starting or for temporary periods of operation, as for instance when transferring load from one unit to another, or during very light loads, etc.

In figure 4 of this discussion is shown a 625-volt 5,000-ampere nominally rated 12-anode rectifier with automatic grid control equipment. This equipment was installed in a substation of the Commonwealth Edison Company in Chicago, and has now been in operation for over seven years. The control board is shown on the right-hand side and contains the necessary apparatus, including a quick-acting rocking-type regulator for automatically controlling the output voltage, as well as some rheostats and changeover switches for manual control of voltage.

From figure 5, which shows the simplified diagram of connections of the above installation, it can be seen that no auxiliary motor generator set is used, as in the application shown in figure 5 of Mr. McDonald's paper. However, instead of the generator voltage the rectifier output voltage is used as a source of the d-c bias. Such an arrangement simplifies the control considerably as only a standard potentiometer is necessary.

Figure 6 shows portions of voltage and current graphs taken in the above grid-controlled rectifier installation, which, as mentioned previously, supplies a railway load. These graphs clearly indicate the effectiveness of the grids in keeping the voltage constant in spite of continued fluctuations in the load current (shown in the upper graph). The automatic grid control was turned off at *A*, so that the portion of the (lower) voltage graph to the right of point *A* represents, by contrast, the unregulated fluctuating voltage of the rectifier. It is therefore evident that the output voltage of a rectifier may easily be maintained within as narrow limits as required in spite of fluctuating loads.

Another very interesting installation using this type of grid control was put in operation over two years ago by our Company in New York, furnishing power to the Third Avenue Railway. However, with this rectifier the grid control acts as a current regulator in that its function is to keep the current constant down to the full-load value as long as the base load of the station is above the rated rectifier load. The rectifier is operating in parallel with rotary converters in the same station. It is, however, connected to a different supply bus than the rotary converters. In other words, this installation is also interesting due to the fact that a 1,000-kw rectifier ties together two very powerful networks of 25- and 60-cycle frequencies, which could only be accomplished safely by using, instead of a rotary converter, a rectifier which is not susceptible to frequency or voltage variations.

Furthermore, a d-c bias control with a rocking-type regulator was used in an installation for the Republic Steel Company in Chicago. The arrangement for balancing the load is the same in principle as shown in figure 5, except that the drop across the shunt in the rectifier load circuit was to be used in order to affect the compensating coil of the rocking-type regulator. In

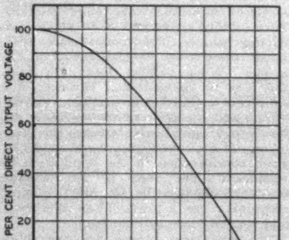

Figure 7 (left). Regulation of direct-voltage output of a six-phase rectifier as a function of the angle of delay

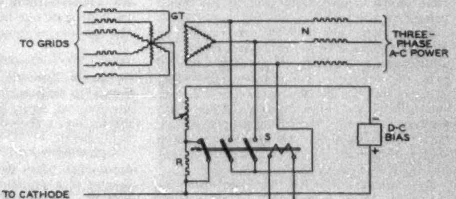

Figure 8. Grid-controlled protection circuit for interrupting short circuits and backfires

connection with this installation it was extremely important to obtain a voltage control over a wide range with quick response due to the requirements of the wire-drawing machines and of the excitation of the machinery in the automatic galvanizing plant. It was found that the d-c bias control on the rectifier met the requirements very well and takes care entirely automatically of a-c and d-c feeder voltage drop and of the inherent machine regulation.

In two radio rectifier plants we probably made the most extensive use of electronic grid control, in that the voltage is regulated from practically zero to the full value. It is general practice in radio installations to apply voltage first at a low value and then to increase it automatically in two or three steps to the normal operating value; see figure 7 of this discussion.

In a rectifier without grids, power is interrupted for protection purposes by means of an a-c circuit breaker in the primary power supply line. In a grid-controlled rectifier, however, additional protection can be provided by means of electronic interruption of power obtained by the blocking action of the grids. This blocking action is dependent upon automatically placing a negative potential in relation to the cathode on all grids when an overload or short circuit occurs in the radio transmitter or rectifier equipment. By using a small high-speed relay to apply the negative grid blocking bias, it is possible to interrupt power in the rectifier unit within a fraction of a cycle. This is considerably faster than the interrupting time required by an a-c circuit breaker, and thus grid control provides much better protection than mechanically operated circuit breakers. Moreover, since an a-c circuit breaker is usually furnished in any event, double protection is provided in that the high-speed grid-control protection apparatus is backed up by the slower-speed circuit-breaker equipment.

Figure 8 shows a diagram of a grid-control protection circuit. Upon the occurrence of a fault in the transmitting equipment, a surge current is induced through insulating current transformer *IT* on the coil of relay *S*. Opening of the back contact of this high-speed relay instantly inserts resistance *R* into the bias potentiometer circuit and thereby causes the negative biasing voltage to be increased to a value of greater relative magnitude than the positive a-c potentials placed on the grids through resistors *N* and grid transformer *GT*. Closing of the other contacts of relay *S* an instant later short-circuits the grid excitation transformer, thus doubly assuring that ony a negative blocking bias is maintained on the grids.

It could be seen from the above that by adding very little extra equipment, an additional possibility of the grid control can be realized. Interruption of short circuits and backfires is accomplished very successfully in connection with our mercury-arc rectifiers furnishing power for the New York subway. Measurements have shown that the backfires are interrupted in less than two cycles.

In connection with the arrangement shown in figure 5, it would be interesting to know what alternating voltage is applied to the grids, and what is the maximum d-c bias voltage used. Furthermore, I would like to know the reason for using energized grids in connection with the arrangement shown in figure 6. It was my understanding that the ignition of the cathode spot can be fully taken care of by the ignitors as developed at present.

G. R. McDonald: I wish to thank Mr. Jones for pointing out an error in the connections of figure 6, and for the further discussion on the voltage control of ignitron equipments.

Referring to Mr. Marti's comments, we agree that when delaying the firing of the anodes of a rectifier to modify the output voltage, the anode currents and voltages remain essentially the same with the same rectifier output. As pointed out by Mr. Marti, rectification is made more difficult when delaying the firing of the rectifier anodes because the inverse voltage increases more rapidly at the end of an anode conducting period. This causes some additional positive-ion bombardment of the anodes. When rectifier loads are light, this is of no consequence. However, if a rectifier is operated at its maximum current rating, and the firing of the anodes is delayed to a considerable extent, this may become of some consequence and should be given consideration in the application of equipment. The type of flat voltage regulation described by Mr. Marti is naturally easy for a rectifier as maximum grid control comes at light load.

The equipment chosen for controlling the rectifier grids to modify the output voltage is entirely a function of the type and range of control desired in each case.

Operating Experience With Petersen Coils on 66-Kv System of Metropolitan Edison Company

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MEMBER AIEE

R. E. NEIDIG
ASSOCIATE AIEE

THE Metropolitan Edison Company, operating in the eastern part of Pennsylvania, has a total of 264 miles of 66,000-volt overhead transmission lines, to which interconnections at this voltage with the systems of the Pennsylvania Water and Power Company and the Pennsylvania Power and Light Company add 59 miles, making a total of 323 circuit miles. A single-line diagram of this system is shown in figure 1, which also shows the various types of line construction used, ranging from all-wood construction to all-steel construction, with overhead ground wires and counterpoise. The system neutral is grounded through transformers at West Reading, Middletown, and the Holtwood station of the Pennsylvania Water and Power Company. At West Reading, the grounding bank consists of three single-phase 500-kva 66,000-13,200-volt transformers connected wye-delta, and at Middletown a three-phase zigzag transformer having an equivalent transformer capacity of 1,000 kva. At Holtwood each 66,000-volt line, as shown in figure 1, terminates in a 20,000-kva three-phase 69,000-13,200-volt wye-delta transformer, having the high-voltage neutral grounded through a 300-ohm reactor. The 13,200-volt winding of these transformers is connected to the 13,200-volt station bus.

The system is, in general, equipped with conventional induction types of directional-phase and directional-ground relays. The only exception is in the phase relaying on the Pennsylvania Water and Power Company's lines and on the York end of circuits number 77 and number 78, where directional-distance type relays are used. Balanced relay schemes are used where advantages can be gained thereby.

All lightning arresters and transformers are insulated for full line-to-line voltage.

Application and Design

Interruptions were being experienced on this system due to insulator flashovers, the majority of which were ascribable to lightning, and various means of lightning-proofing these lines were

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Paper number 39-37, recommended by the AIEE committees on protective devices and power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted October 27, 1938; made available for preprinting December 30, 1938.

H. M. RANKIN is superintendent of power supply and R. E. NEIDIG is relay engineer, Metropolitan Edison Company, Reading, Pa.

The authors wish to express their appreciation of the advice and assistance rendered by E. M. Hunter, of the General Electric Company, in the conducting of tests and in preparation of data.

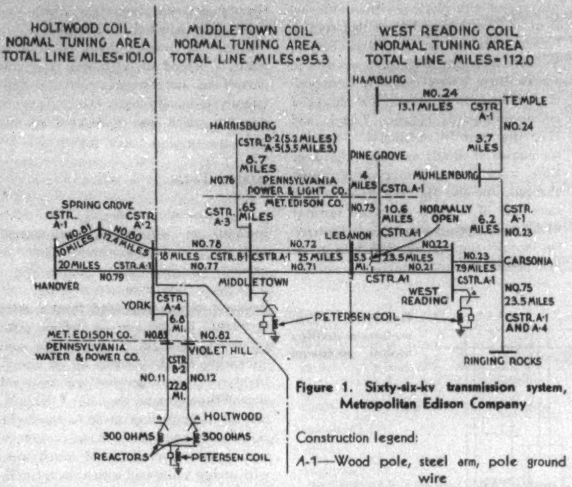
HOLTWOOD COIL
NORMAL TUNING AREA
TOTAL LINE MILES=101.0
MIDDLETOWN COIL
NORMAL TUNING AREA
TOTAL LINE MILES=95.3
WEST READING COIL
NORMAL TUNING AREA
TOTAL LINE MILES=112.0
HAMBURG
NO. 24
13.1 MILES
CSTR. A-1
TEMPLE
NO. 24
3.7 MILES
HARRISBURG
CSTR. D-2 (5.2 MILES) A-5 (3.5 MILES)
8.7 MILES
NO.76
PENNSYLVANIA POWER & LIGHT CO.
MET. EDISON CO.
CSTR. A-3
.65 MILES
PINE GROVE
4 MILES
CSTR. A-1
MUHLENBURG
NO.73
10.6 MILES
NORMALLY OPEN
CSTR. A-1
6.2 MILES
CSTR. A-1
NO.23
SPRING GROVE
CSTR. A-1
CSTR. A-2
NO.81
10 MILES
NO.80
12.4 MILES
20 MILES
CSTR. A-1
NO.79
HANOVER
NO.78
18 MILES
CSTR. B-1
NO.77
CSTR. A-1
NO.72
25 MILES
NO.71
LEBANON
5.3 MI.
23.5 MILES
NO.22
NO.21
CSTR. A-1
NO.23
7.9 MILES
CSTR. A-1
CARSONIA
MIDDLETOWN
WEST READING
PETERSEN COIL
NO.75
23.5 MILES
CSTR. A-1 AND A-4
RINGING ROCKS
YORK
CSTR. A-4
6.8 MI.
MET. EDISON CO.
PENNSYLVANIA WATER & POWER CO.
NO.83
NO.82
VIOLET HILL
CSTR. B-2
NO.11
NO.12
22.8 MI.
HOLTWOOD
300 OHMS
300 OHMS
REACTORS
PETERSEN COIL

Figure 1. Sixty-six-kv transmission system, Metropolitan Edison Company

Construction legend:

A-1—Wood pole, steel arm, pole ground wire

A-2—Wood pole, steel arm, pole ground wire, overhead ground wire

A-3—Wood pole, steel arm, pole ground wire, overhead ground wire, expulsion gaps

A-4—Wood pole, wood arm, no pole ground wire

A-5—Wood pole, steel arm, pole ground wire, expulsion gaps, counterpoise

B-1—Steel tower, no overhead ground wire

B-2—Steel tower, two overhead ground wires, counterpoise

considered. It was found, however, that approximately 50 miles of double-circuit line, which was originally built for operation at 33,000 volts, could not be changed to accommodate either overhead ground wires or expulsion gaps without complete rebuilding. An analysis of the operation for the year 1936 showed that approximately 69 per cent of the faults were cleared by ground or phase and ground relays. Numerous conductor failures and insulator failures during lightning storms were experienced. It was therefore finally decided to install Petersen coils, or ground fault neutralizers, on this system, with the expectation that over 70 per cent of the outages would be eliminated. Due to operating conditions, and in order to maintain selective relaying under all conditions, it was necessary to install a coil at each location where the system neutral was grounded.

The neutral bushing of the zigzag grounding transformer at Middletown was originally rated at 15 kv. This was replaced with one rated 50 kv, in order to provide insulation capable of handling at least line-to-neutral voltage. No other change of system insulation was necessary on account of the Petersen-coil application.

The three Petersen coils were furnished by the General Electric Company and were designed from measurements of charging current made by disconnecting the grounding transformers at West Reading and Middletown and opening the neutral-ground connections at Holtwood, and grounding one phase of the system through a current transformer. This measurement was made at various locations on the 66,000-volt system. The average charging current per mile of circuit was found to be 0.34 amperes.

In the application of three Petersen coils, it was decided to have each coil normally protect approximately one-third of the total circuit mileage and to have sufficient range to allow for any abnormal operation which could be anticipated.

All of the coils were designed for ten-minute operation, those at West Reading and Middletown having ratings of 15 to 60 amperes, and the Holtwood coil having a rating of 12 to 48 amperes. The minimum current rating was selected to permit operation of the minimum expected circuit mileage and maximum rating selected to allow for system growth. The West Reading and Middletown coils are designed with single bushings, conservator tanks, Thyrite by-pass resistors, current transformers inside the tank in the grounded lead, and external manually-operated ratio adjusters by means of which 25 tap points are obtainable. The Holtwood coil is similar, except that a gas seal is used instead of the conservator.

Each coil was provided with an oil circuit breaker which would automatically short-circuit the coil after current had passed for a predetermined time, thereby solidly grounding the system neutral to allow relaying of the faulty line. Figure 2 shows the Middletown coil and short-circuiting oil circuit breaker as installed, together with the grounding transformer in its original position.

The auxiliary-control apparatus for each coil consists of an instantaneous overcurrent self-reset relay, a timing relay, an operation counter, a signal, and a recording ammeter, connected as shown in figure 3. Operation of the overcurrent relay simultaneously energizes the timing relay and the operation counter. The timing relay is a d-c motor-operated device used to close the short-circuiting oil circuit breaker if current flows through the coil continuously for a period of five seconds. The timing relay returns to the starting position instantly if the fault is cleared within the five-second period. The operation-counter registers the number of operations of the overcurrent relay. This provides a more accurate record of the total number of faults, as several faults occurring within a short space of time would appear as a single fault on the recording ammeter, due to the slow

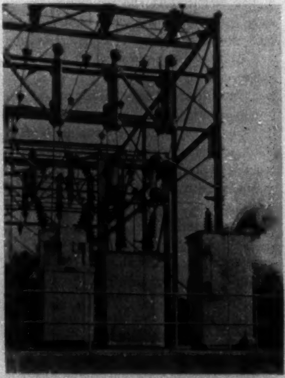

Figure 2. View of Petersen coil and short-circuiting oil circuit breaker installed at Middletown

chart speed. At West Reading and Middletown, the signal is connected as shown in figure 3, and is energized only when the short-circuiting oil circuit breaker closes. At Holtwood, it is connected in parallel with the timing relay, becoming energized directly from the overcurrent relay contact.

Tuning Tests

The coils were installed in September 1937, and immediately after their installation tuning tests were made to determine the proper operating taps for the various operating conditions. Normal tuning areas were established, as shown in figure 1, and each coil was tuned to its respective area without placing a fault on the system, in the manner described in a recent AIEE paper.[1] In these tests it was found that the resonance or "in tune" current values were high, in some cases far exceeding the continuous current rating of the coils. This high resonance current was thought to be due to excessive electrostatic unbalance of the transmission lines, and indicated that transpositions would be necessary. The effect of the various transpositions was calculated and the transpositions made as described recently in an AIEE paper.[2]

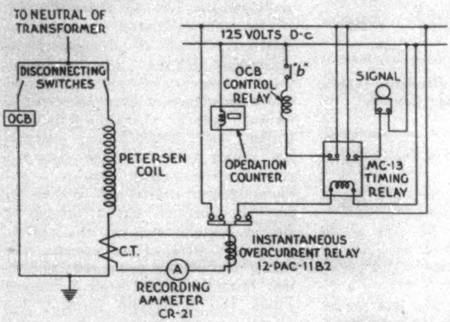

Figure 3. Connections for auxiliary control equipment

Transpositions

Referring to figure 1, circuits numbers 21 and 22 between West Reading and Lebanon, and circuits numbers 71 and 72 between Lebanon and Middletown, were given one complete transposition. The section of circuits numbers 77 and 78 between York Haven and York, which is about two-thirds of their length, was rearranged with phase positions differing from one another and from that section between Middletown and York Haven, so that, in effect, with both circuits in service there is one complete transposition. The bottom and middle phases of circuit number 24 between Temple and Hamburg were interchanged. The two top phases of circuit number 80 between York and Spring Grove were interchanged, and also the two phases on the same side of the pole on circuit number 81 between Spring Grove and Hanover. On circuit number 82, which is a tie line between this company's substation and that of the Pennsylvania Water and Power Company in York, the vertical phasing was changed from *C-B-A* to *B-A-C*. On the Pennsylvania Water and Power Company's system, the top and middle phases of circuit number 11 between York and Holtwood were interchanged. In making these changes, advantage was taken of locations at which the configuration of the circuit changed from vertical to horizontal arrangement, so that the total additional material required for all transpositions consisted of two 14-foot steel crossarms.

The effect of the transpositions was, in some cases, to balance one circuit against another in a given section of the system, which meant that the outage of one circuit in such a section would cause some degree of electrostatic unbalance. If the coils were retuned to the remainder of the system by changing taps, this electrostatic unbalance might be sufficient to cause excessive resonance current to flow through the coils, and a certain amount of detuning would then be necessary. From an operating viewpoint, it was considered preferable to drop the circuit without attempting any retuning, inasmuch as the amount of detuning caused thereby was less than that which was successfully handled in field tests.

After the transpositions, etc., were made, a second set of tuning tests was carried out, and resonance current magnitudes were found to have been reduced to a point which was considered acceptable.

Staged Tests

An automatic oscillograph was then installed at Middletown, and staged tests were conducted. Arcing faults to ground were initiated by connecting a 0.5-ampere fuse wire across a five-unit insulator string suspended from a steel crossarm located on a wood pole, grounded through a down-wire, and energizing this by closing an oil circuit breaker. This down-wire was wrapped several times around the base of the pole, but no attempt was made to lower the pole-footing resistance. A series of seven tests was conducted, six of which were with arcing faults and with system conditions varying from normal tuning to as much as 25 per cent out of tune. The out-of-tune conditions were established by isolating certain lines from that part of the system on which the tests were being conducted, leaving, in some cases, only two coils to clear the fault. No changes of coil taps were made from the normal position for any of the out-of-tune tests. The other test was a solid ground fault, made in order to check the operation of the short-circuiting breakers and the system ground relays. In the arcing-fault tests, the arc was suppressed in each case without any oil-circuit-breaker operation or system disturbance,

Table I. Petersen Operating Record

	Total	Per Cent of Total	Per Cent of Transient
All faults	190	100.0	
Transient faults	173	91.0	
Transient faults cleared by Petersen coil	128	67.4	74.0

Table II. Cause of Transient Faults Cleared by Petersen Coils

Lightning	84
High wind, rain, sleet, etc.	18
Unknown (fair weather)	18
Bushing flashovers	6
Iron oxide on insulator	1
Broken conductor contacting steel tower	1
Total	128
Per cent of total due to lightning	65.6

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[1] For all numbered references, see list at end of paper.

the time of extinguishing the arc varying from one-half cycle to seven cycles.

Operating Results

The coils were placed in service October 16, 1937, and in the various tables, "Petersen-Coil Year" designates the year ending October 15, 1938. Table I shows that out of a total of 190 faults occurring during the Petersen-coil year, 128 faults were cleared by the coils without oil-circuit-breaker operation. However, the operation-counters, and the oscillograph which was in service for several months at Middletown, show that actually there were more than 500 arc suppressions. The oscillograms reveal that every fault was not cleared by the coils at the first attempt, as in some cases several arc suppressions occurred in a fraction of a second on the same phase before the fault was completely removed from the system. In these cases, the coils were given credit for clearing only the initial fault, as a system disturbance resulting in oil-circuit-breaker operation would have begun at that time, had the coils not been in service.

As previously stated, it was expected that the coils would remove over 70 per cent of all faults from the system without disturbance. The results show that 67.4 per cent of all faults (74.0 per cent of all transient faults) were cleared by the coils without oil-circuit-breaker operation.

The fact that results have been somewhat lower than was expected, is due, in part, to an unusual condition existing on circuits numbers 71 and 72, where they pass close by an ore-concentrating plant of a large steel company. At this point, under certain weather conditions, the black oxide of iron dust discharged from the stacks deposits on the insulators and reduces their insulation level. Consequently, a single conductor-to-ground fault at another location, with the accompanying increased voltage-to-ground on the two sound phases, caused the dirty insulators to flash over, resulting in simultaneous faults at different locations involving more than one phase. Faults of this nature occurred on several occasions before the cause was determined. Increasing the insulation level in the affected area has apparently remedied this serious condition, as no simultaneous faults have since been experienced; furthermore, to the best of our knowledge, neither has there been a flashover in the vicinity of the concentrating plant.

Table III. Comparison of Faults and Oil-Circuit-Breaker Operations Before and After Petersen-Coil Installation

	Average of Previous Five Years	Petersen-Coil Year
Total faults	164	190
Total transient faults	138	173
Transient faults causing oil-circuit-breaker operation	138	45
Permanent faults	26	17
Total oil-circuit-breaker operations	448	221
Oil-circuit-breaker operations from transient faults	315	118

Table IV. Interruptions Per Circuit

	Number of Circuit Interruptions								
	Average of Previous Five Years			Petersen-Coil Year					
Circuit Number	Lightning	Other Causes	Total	Lightning	Other Causes	Total	Construction (Figure 1)	Type of Insulators	Equivalent Spacing (Inches)
11							*B*-2	Suspension	
12							*B*-2	Suspension	
21	10.4	5.4	15.8	7 (1)	3	10 (1)	*A*-1	Pin	45
22	12.8	4.4	17.2	5	9	14	*A*-1	Pin	45
23	1.6	1.2	2.8	0	1	1	*A*-1	Pin and suspension	66
24	1.4	2.2	3.6	5	1	6	*A*-1	Pin	61
71	16.2	8.2	24.4	5	6	11	*A*-1	Pin	60
72	13.2	6.8	20.0	6 (1)	9	15 (1)	*A*-1	Pin	60
75	3.6	3.4	7.0	5 (1)	1	6 (1)	*A*-1 and *A*-4	Pin	54
76							*A*-3, *A*-5, *B*-2	Suspension	
77	6.6	1.6	8.2	0 (1)	0	0 (1)	*B*-1	Suspension	153
78	8.6	2.2	10.8	0	1	1	*B*-1	Suspension	153
79	8.2	5.0	13.2	1	1	2	*A*-1	Suspension	94
80	4.6	3.0	7.6	1	0	1	*A*-2	Suspension	100
81	6.0	4.0	10.0	1	1	2	*A*-1	Suspension	100
82	2.6	1.0	3.6	1	0	1	*A*-4	Suspension	91
83	2.4	1.2	3.6	0	0	0	*A*-4	Suspension	91
Totals	98.2	40.6	147.8	37 (4)	33	70 (4)			
Summary									
Pin-type insulators	57.6	30.4	88.0	33 (3)	29	62 (3)	148 circuit miles		
Suspension insulators	39.0	18.0	57.0	3 (1)	3	6 (1)	147 circuit miles		
Combination pin and suspension	1.6	1.2	2.8	0	1	1	14 circuit miles		

Note: Figures in parentheses represent additional interruptions occurring while coils were short circuited and therefore inoperative.

An analysis of the fault-clearing record of the coils is given in table II. It will be noted that 84 of the 128 faults cleared by the coil were caused by lightning. Approximately two-thirds of the 18 operations from unknown causes occurred around daybreak, and were believed to have been caused by birds.

A comparison of the number of faults and oil-circuit-breaker operations before and after the Petersen-coil installation is given in table III. Although the total number of faults during the Petersen-coil year exceeded the five-year average, the total number of oil-circuit-breaker operations was reduced more than 50 per cent. This performance makes it apparent that oil-circuit-breaker maintenance has been materially reduced.

The over-all reduction of system disturbances is clearly shown by the fact that the total number of transient faults causing oil-circuit-breaker operation has been reduced from an average of 138 per year, over five years, to 45 during the Petersen-coil year, or a reduction of 67.4 per cent. Lightning was responsible for 36 of the 45 faults, 29 of which were cleared by phase relays. The majority of these faults, however, occurred in the 50-mile section of double-circuit line with closely spaced conductors. Lightning, therefore, was responsible for a total of 120 transient faults, 70 per cent of which were cleared by the Petersen coils. The reduction in oil-circuit-breaker operations caused by transient faults was about 64 per cent.

It is also interesting to note that during the Petersen-coil year the total number of permanent faults was only about two-thirds of the five-year average (table III). It seems reasonable to credit this reduction to the coil performance, inasmuch as fewer insulator failures and conductor failures were experienced during the coil year than in any one of the previous five years.

The number of interruptions per circuit during the Petersen-coil year, in

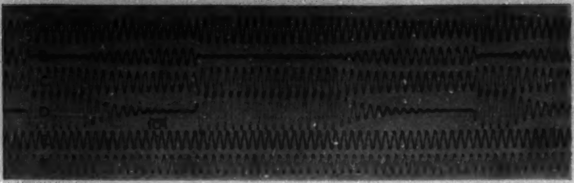


Figure 4. Oscillogram showing fault due to tree contact

A—Line-to-neutral voltage, phase A; 1 CM = 60 volts root-mean-square (×600)

B—Line-to-neutral voltage, phase B; 1 CM = 60 volts root-mean-square (×600)

C—Line-to-neutral voltage, phase C; 1 CM = 60 volts root-mean-square (×600)

D—Petersen-coil current; 1 CM = 12.0 amperes root-mean-square (×1)

E—Line-to-line voltage, phase A-B; 1 CM = 100 volts root-mean-square (×600)

F—Line-to-line voltage, phase B-C; 1 CM = 100 volts root-mean-square (×600) (measured maximum to maximum)

comparison with the average of the previous five years, is shown in table IV. This record shows that the performance of the circuits having closely-spaced conductors and pin-type insulators has been considerably below expectations. On the other hand, the performance of the circuits with five-unit suspension insulators has been excellent. It is probable that part of the reason for the poor performance of the pin-insulator circuits lies in the high pole-footing resistance, which in the event of surge current passing to ground, would cause the potential of the entire pole structure to be raised above ground potential sufficiently to cause multiple flashovers on the same structure. Spot measurements of pole-footing resistance, recently made along circuits numbers 21 and 22, were found to vary between 25 and 150 ohms, with an average from 20 locations of 80 ohms. Further study is being made of the pin-type circuits, in an effort to improve their operation.

Data on circuits numbers 11 and 12, of the Pennsylvania Water and Power Company's system, have been omitted from table IV because of certain changes in line protection made about two years prior to the coil installation. Likewise, circuit number 76 has been omitted due to its having been in use less than one year prior to the coil installation.

Summarizing the data in table IV, comparing the Petersen-coil year with the average of the previous five years, it will be seen that the reduction of interruptions to pin-type insulator circuits was 27 per cent; of suspension insulator circuits, 88 per cent; and of the combination pin- and suspension-insulator circuits, 64 per cent. Of all circuits, a reduction of 51 per cent was obtained.

On four occasions, during severe lightning storms, the system became separated by a permanent fault between Middletown and West Reading, resulting in two systems protected by Petersen coils. On each occasion, one or more subsequent flashovers were cleared in each section with the normal coil tap. In one case this represented an out-of-tune condition of nearly 30 per cent.

Three rather peculiar instances in which the Petersen coils saved the system from major disturbances may be worthy of reporting:

1. During a high wind storm, a tree growing close to circuit number 71 was blown into the line, causing 41 coil operations within a period of 25 minutes, although no system disturbance of any kind was observed. The evidence was found by the patrol crew on the following day. Figure 4 shows a section of an oscillogram obtained during this fault, showing three of the 41 arcs extinguished in various times during a period of approximately one second.

2. A bushing on an oil circuit breaker connected to the 66-kv bus at West Reading broke down and arced to the ground sleeve inside the oil tank. This resulted in five coil operations within a period of a few seconds. Although at the time of the coil operation puffs of smoke were seen in the 66-kv switching structure by the station operator, the cause of the operations was not located until two days later when the oil circuit breaker was opened for overhauling. Again, there was no disturbance on the system. Figure 5 is a section of an oscillogram of this fault, showing that one of the flashovers cleared in less than one-half cycle.

3. A dead-end loop on a steel tower, which at some previous time had been burned by a flashover, broke during a high wind, and the side having the splicing clamp on the end of the wire swung intermittently into the tower. This resulted in a total of 265 coil operations over a period of one hour. During this time the trouble was located and the faulty section isolated, all of which took place with no disturbance to service or line-to-line voltage. The oscillogram of this fault showed phenomena similar to that which took place during the tree fault (figure 4), although in some instances more frequent contacts occurred.

In figure 6 is shown an oscillogram of a typical "unknown" cause of coil operation which was cleared in approximately $2^{1}/_{2}$ cycles. This operation occurred at 5:31 a.m., and was suspected to have been caused by birds.

It is important to note that from numerous oscillograms obtained showing arc suppression by the coils, in no case did the line-to-ground voltage on either of the two sound phases exceed the line-to-line voltage.

Permanent Ground-Fault Relaying and Coil Short-Circuiting Breaker Operation

In order to maintain proper selectivity in the operation of ground relays on

Figure 5. Oscillogram showing fault due to failure of circuit-breaker bushing

A—Line-to-line voltage, phase A-B; 1 CM = 100 volts root-mean-square (×600)

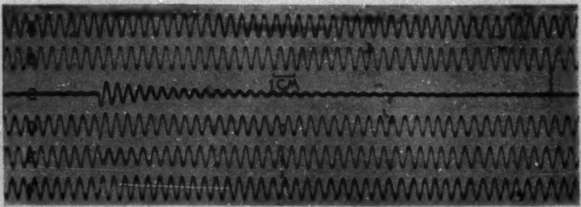
B—Line-to-line voltage, phase B-C; 1 CM = 100 volts root-mean-square (×600)

C—Petersen-coil current; 1 CM = 6.5 amperes root-mean-square (×1)

D—Line-to-neutral voltage, phase A; 1 CM = 60 volts root-mean-square (×600)

E—Line-to-neutral voltage, phase B; 1 CM = 60 volts root-mean-square (×600)

F—Line-to-neutral voltage, phase C; 1 CM = 60 volts root-mean-square (×600) (measured maximum to maximum)



the system, it was, of course, necessary to take care that the coil short-circuiting breakers at all three locations would close at approximately the same time. The d-c motor-operated timing relays used for this purpose were found to be remarkably consistent, and no particular difficulty was experienced in this respect. All coil breakers are reopened manually on order of the system operator.

Only one incorrect oil-circuit-breaker operation was attributed to dissimilar closing times of the coil breakers. In this case a single conductor-to-ground flashover occurred on circuit number 78, which was not cleared by the coils within the five-second period. The coil breakers then closed automatically and circuit number 78 was relayed at both ends, together with the York end of circuit number 77, all by ground relays. It was found that the Middletown coil breaker was about 30 cycles slower than the Holtwood and West Reading coil breakers, the effect of which was to delay tripping at Middletown long enough to allow tripping of circuit number 77 at York. Incidentally, this was the only case observed where a single conductor-to-ground fault was not cleared within the five-second period. It has been suggested that this time be increased in view of experiences with other installations.[2] This cannot be done at present because of eight-second time-delay no-voltage relays connected to coupling capacitor potential devices at a station fed from circuit number 72.

The ground relays on this system are of the directional and nondirectional overcurrent types, most of which have been in service for nearly 15 years. To date, no changes of any nature have been made to the ground-relay schemes or settings on account of the Petersen-coil installation. The 66-kv system relaying, in general, during the Petersen-coil year has been satisfactory, being on a par with the relaying during the previous years.

In two instances, balanced ground relays have operated along with phase relays to clear double-circuit multiple flashovers-to-ground, although none of the coil breakers had closed before the fault was cleared. Further investigation is being made in an effort to determine the reason for these ground-relay operations.

With multiple-coil operation, it is apparent that if any one coil is short-circuited, all other coils are rendered ineffective, as the system is then solidly grounded. Therefore, it is not only important that all coil breakers close at the same time, but it is also important, in the case of manual reopening, that they be reopened in the proper order; otherwise improper relaying might take place for subsequent faults occurring before all coil breakers have been reopened. On our system, this condition is almost entirely avoided by not reopening Middletown coil breaker until after the other two coil breakers have been reopened.

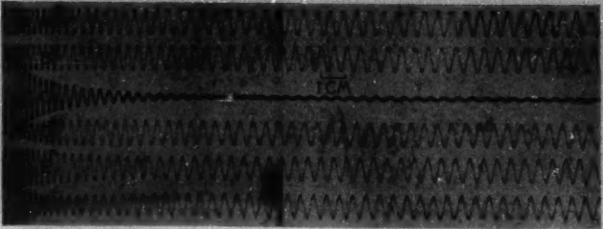


Figure 6. Oscillogram showing flashover from unknown cause

A—Line-to-line voltage, phase A-B; 1 CM = 100 volts root-mean-square (×600)

B—Line-to-line voltage, phase B-C; 1 CM = 100 volts root-mean-square (×600)

C—Petersen-coil current; 1 CM = 6.5 amperes root-mean-square (×1)

D—Line-to-neutral voltage, phase A; 1 CM = 60 volts root-mean-square (×600)

E—Line-to-neutral voltage, phase B; 1 CM = 60 volts root-mean-square (×600)

F—Line-to-neutral voltage, phase C; 1 CM = 60 volts root-mean-square (×600) (measured maximum to maximum)

In some cases, particularly during electrical storms, an appreciable time elapsed before all coil breakers were reopened, during which faults occurred subsequent to the one causing the coil-breaker closures, causing interruptions that might otherwise have been avoided. A total of seven ground faults occurred in such periods during the Petersen-coil year, four of which caused circuit interruptions. Therefore, in order to get all coils back into service as quickly as possible, plans are under consideration to install automatic-reopening equipment on the coil breakers. This equipment will function to reopen the coil breaker after fault current has ceased to flow in the grounding transformer.

Conclusions

From experience obtained thus far, the following appear to be reasonable conclusions:

1. The amount of electrostatic unbalance of a system should first be determined before a coil installation is planned.

2. Experience with multiple-coil installation indicates that the addition of a suitable automatic-reopening device for the short-circuiting oil circuit breakers is desirable.

3. Multiple-coil operation has presented no serious difficulty from the standpoint of system relaying.

4. On lines of fairly modern construction, with five suspension disk insulators, the improvement in service is up to expectations.

5. On lines of older construction, using pin insulators, the improvement is not so marked, indicating, we believe, that more multiple flashovers occur on these lines.

6. It is apparent that successful Petersen-coil operation may be expected with the coils appreciably out of tune. Under abnormal system operation, one section of the system operated with two coils as much as 30 per cent out of tune, during which time successful operations were recorded.

7. Operation of the coils has been responsible for a reduction in the total number of oil-circuit-breaker operations, with a consequent reduction in maintenance cost.

References

1. Some Engineering Features of Petersen Coils and Their Applications, **E. M. Hunter**. AIEE Transactions, volume 57, 1938.

2. The Electrostatic Unbalance of Transmission Lines and Its Effect on the Application of Petersen Coils, **J. A. M. Lyon**. AIEE Transactions, volume 58, 1939, pages 107–11 (March section).

3. Test and Operation of Petersen Coil on 100-Kv System of Public Service Company of Colorado, **W. D. Hardway** and **W. W. Lewis**. AIEE Transactions, volume 56, 1937.

Discussion

E. M. Hunter (General Electric Company, Schenectady, N. Y.): It is worthy of note that the grounded-neutral method of operation has been supplemented by Petersen coils on the Metropolitan Edison Company 66-kv system. This is one of several applications of its kind in the United States and there are indications of a very definite trend in the industry away from the solid neutral ground with its very obvious limitation that every flashover to ground is a short circuit requiring immediate attention. It is believed that the presentation of the operating experience given in this paper is very timely and should be of considerable value to others who may be considering similar applications.

It is expected that from time to time other companies which are now operating solidly grounded will supplement their ground fault protection with Petersen coils. This requires some system planning because certain insulation levels must be maintained in the connected electrical apparatus. Transformer neutrals should be fully insulated, lightning arresters should be of the ungrounded-neutral type, and interconnections of systems of different voltage levels should be through two-winding transformers and not autotransformers. These facts should be kept in mind when new purchases of electrical apparatus are contemplated because otherwise the cost of rebuilding equipment to make it suitable for the Petersen coils may make the application uneconomical.

E. H. Bancker (General Electric Company, Schenectady, N. Y.): One of the factors brought to light in this paper is the fact that the use of Petersen coils does not upset normal ground relaying. It should be pointed out that this does however require that when the coils are short-circuited, there should be about as many grounded neutrals as existed at the time the relay settings were made, or else the relays will have to be reset for the new ground-fault current condition. This factor must be considered when an installation is proposed in order to determine whether certain neutrals previously grounded and not to be equipped with coils also should be provided with grounding circuit breakers.

The control for automatically reopening the breaker may be arranged to perform in any one of several possible ways. In my opinion the circuit breakers should reopen immediately when the coil current ceases in order that the system shall return to Petersen-coil operation as quickly as possible. It seems to me that the breaker should also reopen after it has been closed a definite time that is long enough to assure relay operation even though the ground current is still flowing, so that in the event of relay failures the system will return to Petersen-coil operation rather than maintain ground-fault current. Of course, this means continuous-rated coils or action by the operators to isolate faulty sections before the coil thermal limits are exceeded. It would seem preferable to operate with the Petersen coils in service and one conductor grounded in the event of relay failure, rather than allow the system to continue to supply ground-fault current until breakers are opened manually. The opinion of others on this point is solicited. It would be helpful to the manufacturers in planning future applications and in the interest of standardization.

H. K. Sels (Public Service Electric and Gas Company, Newark, N. J.): The application of Petersen coils to a system must be studied very carefully to determine if the benefits which may be derived are great enough to justify their installation. Messrs. Rankin and Neidig have reported in table IV the interruptions which have been experienced per circuit over a number of years. This tabulation shows that circuits numbers 21 and 22, 71 and 72 have between 60 and 80 interruptions per 100 miles per year so that the pole ground wire apparently has no beneficial influence on the number of interruptions and in fact may have the opposite effect. However it is evident that the presence of this ground wire does give a preponderance of single-phase faults which presents an ideal situation for the successful operation of the Petersen coil.

In studying a section of our system for the application of the Petersen coil, it was found from an analysis of several hundred oscillograms that approximately 80 per cent of the faults started on more than one phase so that only 20 per cent of the faults remained as single phase for a Petersen coil to clear. It is therefore felt that in order to obtain sufficient improvement in line performance that the successful application of a Petersen coil also required a large item of expense in a general reconstruction of most of the lines to increase the proportion of the single-phase faults. Since in connection with the reconstruction protector tubes could be applied more cheaply than the Petersen coil, it was decided that the Petersen coil should not be installed.

I believe that it would contribute considerably to the paper if Messrs. Rankin and Neidig would submit additional information on the proportion of single-phase faults which they believe occur on their line construction. This should show that the installation of the Petersen coil was justified in their case whereas our analysis casts a reasonable doubt on the over-all gain to be obtained by a Petersen-coil installation.

J. R. North (Commonwealth and Southern Corporation, Jackson, Mich.): This paper is very interesting and shows clearly the results obtained by the use of Petersen coils on this system. It further substantiates our opinion, based upon tests and operating experience, that there are many factors which need to be considered carefully in determining the probable effectiveness of Petersen coils in a given application. These include evaluation of

(*a*). Relative number of single line-to-ground faults versus faults involving two line conductors.

(*b*). Relative number of permanent ground faults versus transitory ground faults.

(*c*). Magnitude of the in-phase component of fault current (due to line resistance, insulator leakage, corona, etc.) and influence of voltage recovery rate.

(*d*). System arrangement—radial, loop, multiple lines, relative location of lines.

(*e*). Dynamic and transient overvoltages as may occur with faults at different locations, and the ability of the system insulation to withstand them.

(*f*). Protective relay scheme and necessity for automatically clearing permanent ground faults.

In this connection it may be of interest to mention two rather detailed studies of extensive transmission systems, one operating at 138 kv and the other at 44 kv. On the 138-kv system, calculations indicate what we consider to be an excessive amount of uncompensated fault current if Petersen coils were to be used and there is as yet no definite evidence available to establish the upper limit of permissible magnitude of such fault current under the expected conditions of recovery voltage. Furthermore, rapid and accurate isolation of permanent ground faults would be difficult since the system is quite extensively interconnected. The relative advantages and limitations of various types of operation were carefully evaluated and it was decided to operate this system with the neutral effectively grounded.

The 44-kv system on the other hand consists essentially of a number of radial star-type units, connected together by single lines which may be operated as independent sections. This system appears to lend itself admirably to the use of Petersen coils.

John A. M. Lyon (The Johns Hopkins University, Baltimore, Md.): The experience with iron oxide depositing on pin-type insulators and the consequent reduction in insulation strength suggests the importance of further study on the effects of soot and other deposits on insulation.

It is important to note that the Petersen-coil protection for that part of the transmission system which had closely spaced conductors with pin-type insulators (low insulation level) has been definitely inferior to the protection afforded to the rest of the system consisting of lines of modern construction. Undoubtedly the high pole-footing resistance was also a factor in the relatively lower degree of protection which was afforded to these closely spaced lines. This condition immediately emphasizes the necessity for the consideration of the likelihood of single line-to-ground faults developing into double line-to-ground faults or line-to-line faults. Unfortunately, sufficient information on this subject is not always available. The usual relay records will of course give the past history of a transmission line by classifying line-to-line faults, and line-to-ground faults, but there is no way of knowing (except through the use of an automatic oscillograph) how many of the first type of faults have developed from the second type. It indicates that increased attention should be placed on the possibility of multiple faults developing from single line-to-ground faults. A consideration of the actual construction of the lines in subsequent installations seems warranted, and judgment should be based on such practical information as the present authors have given.

F. Von Voigtlander (Commonwealth and Southern Corporation, Jackson, Mich.): Messrs. Rankin and Neidig report a rather successful application of Petersen coils under somewhat adverse conditions. The system to which the coils were applied had previously been operated grounded through rather high reactances at several stations. Line conditions no doubt were largely responsible for the number of multiple faults experienced, though this may have been increased somewhat by the high neutral reactances. Since nothing would be gained by totally isolating the neutral, and solidly grounding would not be considered because of the condition of the lines and because of some radial services, the application of Petersen coils became a logical consideration for this system.

The important criterion upon which to base performance expectations of Petersen coils becomes the proportion of transitory single line-to-ground faults to all faults experienced on the system. On lines of small phase spacing, multiple flashovers would tend to be a rather large proportion of the total, and for such faults the Petersen coils would be of value only in that they would probably tend to limit overvoltages from single line-to-ground faults and thereby to some extent mitigate the probability of second faults occurring, as is brought out by the operating experience cited by the authors.

A reasonable balance to ground is desirable on any transmission system, and it is essential where Petersen coils are involved. This is forcefully demonstrated by this application, in which the unbalance to ground was found to be so great as to overload the Petersen coils during normal system operating conditions.

When more than one Petersen coil must be short-circuited for permanent ground-fault isolation, it is necessary to delay all ground-relay action until short-circuiting of the coils involved has been completed. This requirement is believed to restrict the application of multiple Petersen coils on extensive and complicated networks, particularly where large ground-fault currents may be experienced.

Two instances are mentioned by the authors of balanced ground and phase relays operating to clear multiple ground faults before the Petersen coils had been by-passed. Was this not due to the faults being on different phases at different locations on the system so that residual currents could flow between them large enough to trip balanced ground relays, and even phase relays, thereby clearing the faults before sufficient time had elapsed for the by-passing switches to operate?

H. M. Rankin and **R. E. Neidig:** In closing the discussion, the authors wish to express their appreciation of the interest shown in the subject and the comments brought out in the various discussions.

Referring to the discussion by Mr. Sels, the results of a study of interruptions caused by lightning during the year 1936 showed that 47 per cent were cleared by ground relays only, 13 per cent by both phase and ground relays, 31 per cent by phase relays only, and 9 per cent with no relay indication. This analysis was based purely on relay-target indication, and shows that at least 60 per cent of all interruptions involved ground, with a possibility of a good share of another 9 per cent.

Undoubtedly pole ground wire does assist in confining flashovers to line-to-ground, and therefore allows successful coil operation. However, our experience indicates that even with pole ground wires on pin-insulator circuits, it is essential to have low pole-footing resistance in order to avoid multiphase flashovers. On circuits with higher insulation levels (suspension insulators, wood pole), the pole-footing resistance does not appear to be as important a factor in obtaining successful coil operations. An investigation is now being made to determine the necessary requirements for lowering the pole-footing resistance on the pole lines having pin-type insulators.

Mr. Von Voigtlander has expressed an impression that the 66-kv system had previously been grounded through rather high reactances, which may have been partially responsible for the number of multiple flashovers. Although the interconnection power transformers at Holtwood were grounded through high reactances, it is to be pointed out that the grounding transformers at both Middletown and West Reading were operated with solidly grounded neutrals.

From operating experience prior to the coil installation, it was suspected that the improvement in operation of the circuits having closely spaced conductors would not be so marked, which suspicion has been substantiated by the first year's operating record. However, it is expected that this condition will improve from time to time due to the fact that bad poles, having closely spaced conductors, are replaced with new poles, affording greater spacing.

In Mr. Von Voigtlander's opinion, with multiple-coil installations it is necessary to delay all ground-relay action until all coils involved are short-circuited. The authors wish to emphasize that on the Metropolitan Edison Company system no additional delay was introduced to any ground relays above that which existed prior to the installation of the Petersen coils. In the authors' opinion, the determination of ground-relay settings will, in general, not be affected by the use of Petersen coils. It is agreed, however, that greater precautions are necessary in this respect when the system involved is in the form of a multiple-grounded ring, rather than a straight-line or radial system as was the case of the Metropolitan Edison Company. For a single-coil installation, no difficulty should be experienced in this respect with either type of systems.

In connection with the two instances of balanced ground-relay operations reported, further investigation showed that quite possibly they were due to faults being on different phases of the two circuits on the pole line involved, as was suggested by Mr. Von Voigtlander. However, it is definite that these faults occurred within the confines of the pole line involved, inasmuch as circuit-breaker operation occurred only on these lines, which suggests the probability that the multiple-circuit fault occurred on a common-pole structure.

The authors agree with Mr. Bancker's opinion concerning automatic reopening of coil short-circuiting breakers when neutral current ceases to flow. Whether or not the coil breaker should also reopen automatically after a definite time, even though neutral current is flowing, is open to question. We believe the adoption of this function would be governed by the operating policies existing prior to the coil installation, and the desirability of continuing such policies. In our case, a circuit-ground-relay failure throws the responsibility for removing the short circuit from the system back to a relay connected in the neutral of the grounding transformer, which, after a definite time delay, operates a warning signal to the station operator. If the operator is then unable to clear the fault within a definite time, the grounding transformer is then disconnected from the system automatically. It is our impression that the increased cost for a continuously rated Petersen coil would far exceed the cost of the equipment required for the above scheme, which we have found to be desirable protection for all grounding transformers.

The following are unusual instances which have occurred since the original paper was written:

1. Piece of fence wire on conductor near insulator was blown into crossarm by wind. Counters recorded over 600 successful operations (each coil) over period of one hour and 28 minutes. West Reading coil breaker was finally closed manually and faulty line (number 75) tripped successfully upon next contact. Film in oscillograph ran out after 162 arc suppressions.

2. During high wind, foreign object (unknown) on pole structure of numbers 21 and 22 circuits, apparently contacting crossarm, caused 220 arc extinctions within 12 minutes, as taken from the oscillograph. Counter readings indicated an average of 51 operations. After 12 minutes the coil breakers operated, clearing the fault, although evidence from the oscillogram showed that an arc had been extinguished a few cycles after the coil breaker received the impulse to close; otherwise an indefinite number of subsequent coil operations would have occurred. This was only the second instance in about $1^{1}/_{2}$ years' operation which indicated the possible desirability of an increased time delay in closing the circuit breakers short-circuiting the Petersen coils.

Factors Affecting Arc Extinction on a Petersen-Coil System

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MEMBER AIEE

I. Introduction

THE effectiveness of the Petersen coil in extinguishing line-to-ground arcs on a transmission system is ordinarily attributed to the fact that the current in the arc is kept at a low value. Another factor of perhaps greater importance is that characteristic of the Petersen-coil system which results in a very low rate of rise of recovery voltage across the arc terminals. Although this characteristic has long been recognized,[1,2] the engineering literature would seem to indicate that its importance has not been thoroughly considered. This paper calls attention to the importance of the rate of rise of recovery voltage, presents the circuit theory pertaining to the voltage recovery rate, points out some practical aspects of the Petersen-coil system design, and compares the theory with published records of operation. It is hoped that this analysis will lead to a better understanding of Petersen-coil systems which will permit their more effective use. With continued study of system operating records, it may become possible to predict the effectiveness of a system even before it is built.

II. A-C Arc Characteristics

The characteristics of a-c arcs have been studied very extensively by many investigators, particularly with reference to oil-circuit-breaker operation. As many articles have been written on this subject,[3-5] only those salient points having a direct bearing on Petersen-coil system performance may be mentioned here. Consideration will be given only to unconfined arcs in air, as this is the type ordinarily dealt with on Petersen-coil systems.

The extinction of an a-c arc is almost entirely dependent on certain rapid changes which occur in a short interval of time near the instant of zero current. Previous to the instant of current zero, electrons are emitted in great numbers from a small area on the cathode (known as the cathode spot) in which the current density is approximately 4,000 amperes per square centimeter. This emission is due to the high potential gradient set up by space charges in the arc stream which result in a drop of 20–30 volts (known as the cathode fall of potential) across a very thin layer (10^{-4} centimeters) at the cathode. The remainder of the arc stream is known as the positive column and is a highly ionized region of approximately equal numbers of electrons and positive ions. The total voltage across the positive column is dependent on its length, being approximately 20 root-mean-square volts per centimeter.

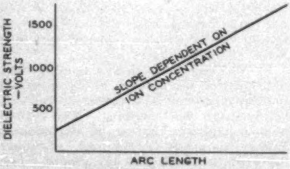
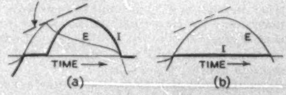


Figure 1. Dielectric strength of arc following current zero.

When, due to its cyclic variation, the voltage between the arc electrodes passes through zero, no potential is available in the arc column to move ions toward the electrodes. Consequently arc current and arc voltage pass through zero simultaneously regardless of whether the circuit is predominantly resistive, inductive, or capacitive.

When at a later instant, voltage of reversed polarity appears on the electrodes, the first movement of charges in the discharge space has characteristics quite different from those of the arc. The ions remaining from the previous period of conduction immediately start moving to their respective attracting electrode. However, as no cathode spot exists on the electrode that is now negative, there is at first no copious supply of electrons. The current density is low and the discharge is spread over a considerable area of the cathode. The total current is a very small fraction of the current value which would be noted in an arc. If before the air becomes deionized (from causes to be discussed later) the voltage between electrodes is raised sufficiently to cause a drop in the region of the cathode of 250 volts, a glow discharge will be established. This is a self-maintaining discharge in which large numbers of electrons and positive ions may be generated in the cathode layer. Once the glow discharge is established, the movement of the ions will set up space charges, and the potential gradient at the cathode will increase rapidly, soon resulting in the formation of a cathode spot on the negative terminal. The discharge then becomes an arc. The cathode fall of potential will again be about 20–30 volts while current density will be in excess of 10^3 amperes per square centimeter.

The voltage at which the glow discharge is established is spoken of as the breakdown strength or dielectric strength of the discharge path. This voltage is the sum of two voltages: the 250 volts required at the cathode for the establishment of the glow discharge plus the voltage which must be impressed across the region of the positive column in order to establish this 250 volts at the cathode. The voltage required across the region of the positive column is found to be approximately proportional to the column



(a) (b)

Figure 2. Circuit conditions following the instant of current zero

(*a*) Arc restriking
(*b*) Fails to restrike

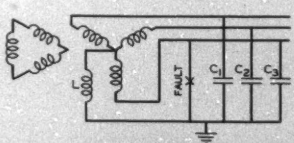


Figure 3. Petersen-coil-system circuit diagram

Paper number 39-64, recommended by the AIEE committees on power transmission and distribution, and protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 10, 1938; made available for preprinting December 27, 1938.

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The author wishes to acknowledge the assistance of Professor Edward Bennett and Doctor F. A. Maxfield of the University of Wisconsin, who offered many helpful suggestions in the preparation of this paper. Particular credit is due H. R. Hegbar, who assisted very materially in the preparation of the manuscript.

[1] For all numbered references, see list at end of paper.

length, the voltage per unit length increasing with decreasing ion density in the region. The dielectric strength of the ionized path is then about as shown in figure 1. Until this voltage is reached, space charges cannot be sufficiently intense to form a cathode spot necessary for the high current of the arc discharge. If following current zero, the voltage applied between electrodes does not exceed the dielectric strength of the path, the arc will not reignite and the path will soon become totally nonconducting. The fault will be cleared.

At all times the path of the discharge is losing ions by the action of the various deionizing agents present. In an oil circuit breaker these deionizing agents are very effective as the arc stream is in close contact with oil and barrier, where turbulence and surface recombination may be very important. In free air the loss of ionization is much slower and is probably due principally to volume recombination and heat loss to the surrounding air. The action of the deionizing agents over the entire cycle must be balanced by the ionizing agents which of course are most active during the conduction period of the cycle. During the period immediately following current zero, ion concentration decreases very rapidly as relatively no ionizing agents are in operation, whereas the deionizing agents are always in effect. This loss of ionization in the arc path results in an increase in the voltage required to establish the condition of the glow discharge. In other words the path gains dielectric strength with time. Circuit conditions following the instant of current zero are shown in an exaggerated form in figure 2.

As shown in figure 2, the arc path becomes almost totally nonconducting for a short period following each current zero. That is, the fault circuit is opened momentarily. In order that the arc should be reformed following current zero, the recovery voltage must at some instant exceed the dielectric strength. Obviously a low rate of rise of recovery voltage is favorable to arc extinction. The rate of rise of recovery voltage following current zero is determined by the transient characteristics of the electrical network.

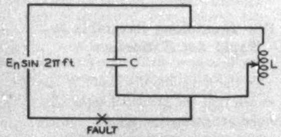

Figure 4. Equivalent circuit of Petersen-coil system (no losses)

III. System Characteristics

The essential parts of a Petersen-coil system (first approximation) are shown diagrammatically in figure 3. The current through the fault is the vector sum of the current in the capacitances C_1 and C_2 and in the inductance L. By making the inductance of suitable size, the inductive current may be made equal to the capacitive current, with the result that the net fault current is zero. The ground-fault circuit for this network (neglecting losses) is as shown in figure 4, in which C is the sum of C_1, C_2, and C_3, and E_n is the line-to-neutral voltage. With the inductance L adjusted to the correct value such that

$$2\pi fL = \frac{1}{2\pi fC}$$

no current will flow through the fault after conditions have once become stable. Under this condition the circuit through the fault may be opened without any effect on the circuit behavior. Even with the circuit open, current will continue to oscillate between the capacitance and the inductance at a frequency f, equal to that of E_n. With continued oscillation (zero losses), no voltage will appear across the arc terminals. That is, the rate of rise of recovery voltage is zero, and there is no tendency for an arc to be re-established at the point of fault.

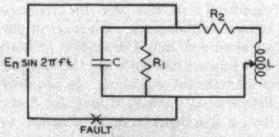

Figure 5. Equivalent circuit of Petersen-coil system (losses considered)

A diagram representing actual conditions more accurately is shown in figure 5. Leakage across insulators, corona loss, etc., are represented by the resistance R_1. Conductor resistance loss and loss in the Petersen coil are represented by R_2. For the purpose of analysis, these losses may all be combined into one resistance and the circuit represented, with a slight change in the value of the inductance, as shown in figure 6. Because of its convenience this circuit will be used in the subsequent discussion.

The root-mean-square value of current in the arc is no longer zero but has some value determined by the magnitude of the system losses. If the circuit through the fault is opened, current will continue to oscillate between the inductance and the capacitance, but this oscillation will die down in a finite length of time as the stored energy is absorbed by the resistance R. This transient decrease of amplitude of the oscillation in the *RLC* branch gives rise to a voltage between the arc terminals, which tends to re-establish the fault.

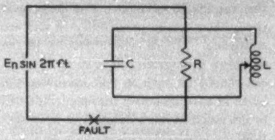

Figure 6. Simplified equivalent circuit of Petersen-coil system (losses considered)

In section II it was pointed out that following each voltage reversal, the current in an arc remains at substantially zero value for that short length of time required for the voltage between the electrodes to reach a value in excess of the dielectric strength of the discharge path. Hence in the Petersen-coil circuit, each time the current passes through zero, a transient is started in the *RLC* branch which results in an exponential decrease of the amplitude of the oscillatory voltage appearing on the capacitor. A voltage equal to the difference between the instantaneous value of neutral voltage and the instantaneous value of the capacitor voltage appears across the terminals of the fault, and tends to re-establish the arc. In a circuit such as is shown in figure 6, the current through the fault passes through zero at the instant that the voltage on the capacitor is zero. Until the arc restrikes, the voltage of the capacitor is[6] for the simplified case, figure 6,

$$e_c = E_n \epsilon^{-t/(2RC)} \sin 2\pi ft \qquad (1)$$

The recovery voltage across the fault terminals tending to cause the arc to restrike is

$$\begin{aligned} e_r &= [E_n \sin 2\pi ft] - [E_n \epsilon^{-t/(2RC)} \sin 2\pi ft] \\ &= E_n[1 - \epsilon^{-t/(2RC)}] \sin 2\pi ft \qquad (2) \end{aligned}$$

as is illustrated in figure 7.

The rate of rise of the *amplitude* of recovery voltage is

$$\frac{dE_r}{dt} = \frac{1}{2\,RC} E_n \epsilon^{-t/(2RC)} \qquad (3)$$

which has an initial value ($t = 0$) of

$$\frac{dE_{r0}}{dt} = \frac{1}{2RC} E_n \tag{4}$$

Obviously, the greater the loss in the resonant circuit (as indicated by a low value of R), the faster is the initial rise of the amplitude of recovery voltage and the greater is the posssibility of the arc restriking. Hence, where possible in the design of a Petersen-coil system, the layout should be such as will minimize the losses in the oscillatory branch. This will be discussed in more detail in a later section.

Next, consider the recovery voltage conditions in a simplified Petersen-coil system (no losses considered) in which the coil current does not exactly balance out the capacitance current. That is, the system is not in tune, or

$$\frac{1}{2\pi fC} \neq 2\pi fL_1$$

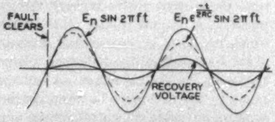

Figure 7. Recovery voltage across arc terminals, Petersen coil in tune (losses considered)

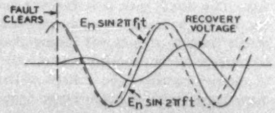

Figure 8. Recovery voltage across arc terminals, Petersen coil inductance less than "in tune" value (no losses considered)

Referring to figure 4, it may be observed that (considering zero loss) the current through the fault is either inductive or capacitive, depending on whether the coil inductance is less or greater than the "in tune" value. If the circuit is opened at the fault, current will continue to oscillate between the capacitance and the inductance, but the frequency of this oscillation is different from that of the power supply E_n. The oscillatory frequency will be

$$f_1 = \frac{1}{2\pi\sqrt{L_1C}}$$

If the fault circuit is opened at the instant of zero current (which now occurs at the instant of maximum voltage on the capacitor) and no losses in the oscillatory circuit are considered, the transient voltage on the capacitor will be

$$e_c' = E_n \cos 2\pi f_1 t$$

and the recovery voltage across the fault terminals will be

$$e_r = E_n (\cos 2\pi ft - \cos 2\pi f_1 t)$$

The circuit conditions for this case are shown diagrammatically in figure 8. With this type of circuit, if f and f_1 are not greatly different, the initial rate of rise of recovery voltage is quite low, in fact much lower than would be observed on a circuit of the types shown in figure 9 in which the voltage E_n and the fault current I are of corresponding value. This explanation undoubtedly accounts for the fact that inductive or capacitive arcs of considerable current value may be extinguished on a Petersen-coil system whereas arcs of similar current magnitude on other types of circuits (for instance, on an isolated-neutral system) might be very stubborn.

As in the case of the "in tune" condition, the "out of tune" condition is more accurately represented by figure 6 in which losses in the oscillatory circuit are considered. In this case the recovery voltage following the extinction of the arc at current zero is dependent on both the frequency difference and the damping of the transient oscillation. In practical Petersen-coil systems, it may be shown that if the inductance is adjusted to within ten per cent of the "in tune" value the initial rate of rise of the amplitude of recovery voltage is principally governed by the exponential decrease of the transient oscillation. Hence in any correctly adjusted Petersen-coil system it is desirable to minimize the losses in the oscillatory circuit.

If the arc occasioned by a line-to-ground fault is stretched out (as by the wind) before extinction, a considerable voltage may be present across the arc column. Because this voltage is usually nonsinusoidal, a rigorous treatment of the effect of this arc voltage on the circuit behavior becomes quite difficult. However, the conditions may be represented at least approximately by figure 10, in which R' represents the arc. The recovery voltage of this circuit when tuned to resonance is

$$e_r = E_n \sin 2\pi ft\left[1 - \frac{R}{R' + R}\epsilon^{-t/(2RC)}\right]$$

Again it is apparent that the amplitude of recovery voltage (particularly for the in-tune condition) is closely associated with the losses in the resonant circuit and decreases with decreasing losses.

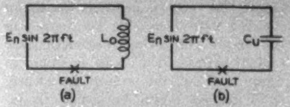

Figure 9. Circuits with steady-state characteristics equivalent to that of a Petersen-coil system which is not correctly compensated

(a) Overcompensated
(b) Undercompensated

As pointed out in section II, the root-mean-square voltage across a stable arc in free air is about 20 volts per centimeter (plus 10–20 volts cathode fall) over a considerable range of current magnitude. This voltage apparently is necessary in order to maintain the positive column, even in the presence of a cathode spot. If the arc length is increased by the wind,[7] an increasing voltage must be applied to the arc terminals to maintain the positive column. This might be approximated in figure 10 by a steadily increasing value of R'. Consideration of this circuit will show that it is impossible to increase the voltage across R' at a rate greater than that called for by equation 2. Hence arc extinction may occur if the voltage required by the positive column increases faster than the voltage which the circuit can deliver.

From the above discussion, it follows that arc extinction may result from two causes: (1) Following current zero, the recovery voltage across the arc terminals fails to reach a value equal to the dielectric strength of the arc path; (2) the arc length is increased by the wind (or otherwise) at such a rate that the voltage required to maintain the positive column of the arc increases at a rate greater than that which can be supplied by the circuit. Actually arc extinction in the Petersen-coil system probably results from a combination of both causes.

IV. Conditions Favorable to Rapid Arc Extinction

In the foregoing discussion it has been shown that the principal factors favoring rapid arc extinction are:

1. The capacitance current should be closely balanced by the Petersen-coil current. The current in the arc is then a minimum as far as reactive components are concerned, and the frequency of the resonant circuit is that of the power system.

2. The losses in the resonant circuit should be made as small as possible by proper design (to be discussed later). The in-phase component of the current in the arc will then be a minimum.

3. The damping of the oscillation in the

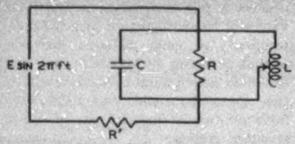

Figure 10. Approximate equivalent of Petersen-coil system, arc resistance considered

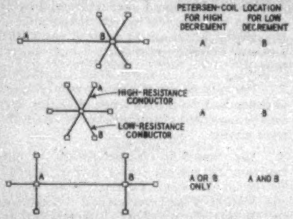

Figure 11. System arrangements in which coil location will affect rate of recovery voltage rise

resonant circuit should be low, thereby minimizing the rate of rise of recovery voltage.

4. The arc length should be as long as possible, and the gap across insulators or arcing horns should be so arranged that the arc length may be readily increased by air currents.

V. Control of Factors Which Govern Arc Extinction

1. Previous articles on Petersen-coil systems have discussed at considerable length the necessity and methods for the accurate control of the tuning of the Petersen coil at all times. While accurate tuning of the coil is probably the most important factor of all, the previous treatments of the subject make further discussion here unnecessary.

2. The control of the resonant circuit losses is a subject which has received practically no treatment in previous discussions. Within certain limits, it may be minimized by proper attention to this circuit. Champe and Von Voigtlander[8] have shown the method for setting up the zero-sequence circuits for a Petersen-coil system of any circuit arrangement. This zero-sequence circuit is of course the resonant circuit whose behavior under transient conditions has such a great effect on the rate of rise of recovery voltage. In setting up this circuit for the purpose of studying the rate of decay of the transient oscillation, it is necessary to include the resistive components of the circuit impedances, and where appreciable, the conductance due to corona losses and insulator leakage. An analysis of the transient characteristics of the equivalent circuit (figure 6) will then clearly demonstrate that the exponent of the damping factor,

$$-\frac{t}{2RC}$$

will in some cases be greatly affected by Petersen-coil location alone. Examples of typical system arrangements in which coil location will be of considerable importance are shown in figure 11.

In general, it may be stated that for best operation, each Petersen coil should be located as near as possible to the capacitance which it compensates, and the connection between the coil and the capacitance should be made through a line having low conductor loss. While practical considerations may in many cases dictate the location of the Petersen coils it must be borne in mind that arc extinction will be much more certain if the coils are so located that the resonant circuit losses are as low as possible. This of course is in direct contradiction to the claims of some writers who have stated that the choice of the coil location is merely a matter of convenience. Fortunately, the desire to locate the coils at points where there would be the least chance of disconnection by switching operations, has in most cases resulted in the installation of the coils at the capacitance centers.

It is of course obvious that the coils themselves and the star-delta transformers connecting the coils to the system should be designed for low loss, inasmuch as they form part of the resonant circuit.

3. The rate of decay of the oscillation in the resonant circuit will in general be determined by the circuit constants and the Petersen-coil locations. Artificial control of this rate of decay may, theoretically at least, be accomplished by the addition of low-loss capacitors across the terminals of the Petersen coil as shown in figure 12. The coil must then be tuned to balance the equivalent capacitance

$$C_e = C_n + C_1 + C_2 + C_3$$

Ignoring the loss in the capacitance C_n and the possible increase of loss in the Petersen coil itself, the exponent of the damping factor $-t/(2RC)$ (of the simplified circuit, figure 6) will decrease inversely as the equivalent capacitance C_e is increased. To reduce the exponent to 50 per cent of its original value would require the installation of approximately three microfarads per 100 miles of overhead line. The installation of capacitors for this purpose might be quite practicable, particularly on low-voltage circuits. Their use might be well justified on circuits having limited physical clearance on which ground fault arcs must be cleared quickly before they have opportunity to spread to the other conductors.

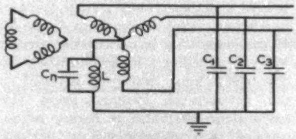

Figure 12. Capacitor added across Petersen-coil terminals to increase stored energy in resonant circuit

4. The length of the arc path, as has been shown, affects to a great extent the extinction characteristics. In fact it can be stated that the extinction of arcs on a given system probably occurs when the arc is increased by the wind or otherwise to some fairly definite length dependent on the recovery characteristics of the system. It would be expected that extinction would be much more likely on an overinsulated system than on an underinsulated system because of the difference in the striking distance across insulators. On systems already in operation, there is probably but little chance of improving the extinction characteristics by control of the arc length without major line changes. However, on new construction or in rebuilding, attention should be given to provide a design in which the arc may be stretched out by the wind with a minimum chance of producing line-to-line faults.

VI. Comparison of Theory With Operating Experience

The story of the recovery voltage on a Petersen-coil system is shown quite strikingly at the end of every oscillographic record of a line-to-ground fault.[2,9,10] As predicted by theory, normal conditions are not restored at the instant the fault is cleared, but are brought about through a transient. The exponential character of this transient is effectively demonstrated by plotting on semilogarithmic paper the oscillograph deflection of successive crests of Petersen-coil current or neutral voltage. Figure 13 shows the straight-line characteristics of the logarithmic plot of Petersen-coil current from published data obtained from the Consumers Power Company* and the Alabama Power Company,† and a

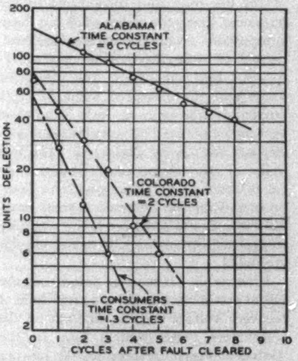

Figure 13. Damping of the oscillation at the end of a line-to-ground fault

similar plot of the displacement from normal of the line-to-ground voltage from the Public Service Company of Colorado.*

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* Figure 12 of reference 9.

† Figure 29 of reference 2.

Considerable information relative to the characteristics of the various systems may be computed from figure 13. The time constant of an exponentially decreasing function is defined as the time required for that function to diminish to $1/\epsilon$ or 37 per cent of its original value. For the circuit of figure 6, the time constant may be shown to be

$$\text{time constant} = 2RC \tag{5}$$

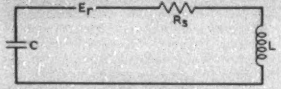

Figure 14. Resonant circuit in which residual voltage operates

The time constants for the three systems have been determined from figure 13, and are shown in table I. Dividing the system neutral voltage by the time constant gives the initial rate of rise of the amplitude of recovery voltage (equation 4). It may be noted that this initial rate of rise is quite different for the three systems investigated.

From equation 5 and the known values of the line-to-ground capacitance of the overhead lines, the value of R has been computed. From this, the in-phase component of fault current is at once determined. For comparison, the computed value and the measured value of fault current are shown in table I. The close agreement between these values seems to support the theory presented. The low value of the equivalent resistance R as calculated for the Consumers Power Company and for the Colorado system probably was due to excessive losses resulting from corona occurring with one conductor grounded.

From table I it may be noted that the initial rate of increase of the amplitude of recovery voltage is quite different on the Consumers Power system than on the Alabama Power system (3,700 kv per second as compared to 255 kv per second). From theory it would seem that for similar arc extinction characteristics, arc length and the rate of voltage recovery are associated in a relation of approximately linear character. Even when considering the difference in insulator clearance provided at 140 kv as compared to that at 44 kv, it appears that the extinction characteristics of the two systems might be quite different on the basis of voltage recovery alone. In addition the Alabama system had the advantage of low arc current, 2.5 amperes, as compared to 40 to 50 amperes on the Consumers Power Company system. It is of interest to note that on the Consumers Power system, arcs established by lightning frequently continued for several seconds even under the action of the severe air currents which ordinarily accompany storms. On the Alabama system, even during staged tests, the period of the arc was very short. In actual operation,[11] numerous disturbances were reported which were too short in duration to give complete fault records. It is possible that these were faults which were extinguished at the first current zero.

The circuit-recovery-voltage characteristics of a Petersen-coil system may be determined with a considerable degree of accuracy from the tuning curve obtained with no grounds on the system, provided however that corona is not excessive under the condition of one conductor grounded. Tuning curves obtained from normal operation require that the unbalance of transpositions result in a slight residual voltage in the system. It may be considered that this residual voltage operates in a series circuit as shown in figure 14. If the inductance is varied, the current will vary reaching a maximum when the circuit is in tune, exactly as is noted on the Petersen-coil system. From this curve alone, the value of R may be determined by straightforward calculations. Considering the transient characteristics of this circuit, it may be shown that the time constant is $2L/R_s$ from which the value of α and the other factors shown in table I may be determined. Using the tuning curve of the Colorado system, the time constant is calculated to be 0.0536, the initial rate of rise of the amplitude of recovery voltage (system in tune) to be 1,150 kv per second, and the fault current to be 12.5 amperes. Here again we may observe a fair comparison between the values of these important quantities determined by quite different methods. Corona occurring during the ground-fault condition which cannot be considered by this approach, would tend to increase both arc current and the rate of rise of recovery voltage.

VII. Conclusions

1. The extinction of arcs on Petersen-coil systems is dependent on the magnitude of the current in the arc, the rate of rise of the recovery voltage across the arc, and the arc length.

2. The rate of rise of the recovery voltage is dependent on the accuracy of tuning and on the transient characteristics of the resonant circuit.

3. The transient characteristics of the resonant circuit are under limited control by the system layout, particularly as regards the location of the Petersen coil. A method for altering the transient characteristics by the use of capacitors is suggested.

4. The magnitude of the uncompensated fault current and the rate of increase of the amplitude of the recovery voltage may be accurately calculated from the known line constants.

5. Satisfactory operation has been reported from Petersen-coil systems having considerably different rates of rise of recovery voltage, consideration being given to differences in arc length for systems operating at different voltages. A further study of system operation may establish the limits in which operation will be successful.

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6. Electric Discharges Waves and Impulse

Table I. Characteristics of Three Petersen-Coil Systems

		Consumers Power Company	Public Service Company of Colorado	Alabama Power Company
Time constant:	Cycles	1.3	2.0	6.0
	Seconds	0.022	0.033	0.10
1/(time constant)(α)		45	30	10
System voltage, kilovolts		140	100	44
Neutral voltage, kilovolts		81	58	25.5
Initial rate of increase of amplitude of recovery voltage (αE_n), kilovolts per second		3,700	1,740	255
Voltage across arc terminals at the end of one-fourth cycle, kilovolts		13.9	6.8	1.04
Capacitance of resonant circuit (from published data), microfarads		6.80	5.65	2.7
$R = 1/(2\alpha C)$ ohms		1,620	2,930	18,500
In-phase arc current:	Calculated E_n/R	50	19.5	1.4
	Measured	40–50	22	2.5

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* Figure 14 of reference 10.

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Discussion

E. M. Hunter (General Electric Company, Schenectady, N. Y.): In connection with the theory of the arc-quenching properties of the Petersen coil, Mr. Eaton shows that the time constant of the system recovery voltage can be used as a criterion of the performance to be expected. This time constant is equal to twice the product of the resistance and the capacitance in the zero-sequence circuit of the system, and to improve arc extinguishing, this time constant should be lengthened. Mr. Eaton suggests that this may be done by decreasing the losses in the circuit. A low-loss circuit will improve the Petersen-coil performance with regard to quenching ground faults, but may complicate matters during normal operation. On every three-phase system there is some slight unbalance in the three-phase voltages to ground so that there is some residual voltage between the neutrals of the system and ground. In normal operation the Petersen-coil reactance and the zero-sequence capacity reactance of the system form a series resonant circuit, and consequently, the installation of a Petersen coil in the system neutral amplifies the residual voltage. The increase in voltage at the neutral depends upon the same losses in the circuits which were previously mentioned. A low-loss circuit means a high residual voltage. To decrease this residual voltage, the system must be balanced electrostatically. On a low-loss circuit it may be a considerable problem to reduce the normal residual voltage to a level considered suitable for the system. All of this is to indicate that there are practical limits beyond which it may be undesirable to attempt to reduce the losses.

Mr. Eaton also concludes that the best results are obtained from Petersen coils when they are located as close as possible to the capacitance which they are compensating. A very practical significance of this which undoubtedly Mr. Eaton recognizes but which was not brought out in his paper is this. In protecting a given system with Petersen coils, a multiplicity of small coils properly located in the system will give a better performance than one large coil for the entire system. This principle was carried out on the Metropolitan Edison Company's application when three coils were installed. The operating experiences reported by Messrs. Rankin and Neidig, both with the system connected together and divided, justifies the larger number of coils.

The necessity of paralleling the Petersen coil with a low-loss capacitor to increase the stored energy in the circuit, as commented on in Mr. Eaton's paper, on every installation is questionable, but undoubtedly on some circuits where the losses of the circuit may be high due to corona or other causes, this additional shunting capacitor might be of some benefit. This capacitance might also be located on the high-voltage terminals of the transformer in the neutral of which the Petersen coil was located. In this location the capacitors would be so proportioned that they would balance the system electrostatically and thus overcome the previously mentioned objection of the low-loss circuit.

J. R. North and F. Von Voigtlander (both of Commonwealth and Southern Corporation, Jackson, Mich.): Mr. Eaton is to be complimented on his very clear exposition of the phenomena involved in the extinction of an unconfined a-c arc in air and for directing attention to the importance of rates of voltage recovery rise and their bearing on successful Petersen-coil operation, particularly on systems where appreciable magnitudes of in-phase components of fault current are involved.

The author suggests shunting the Petersen coil by low-loss capacitors to control the damping factor of the Petersen-coil system. It would appear that the size of such capacitors would be quite large as compared to the capacitance of the lines and might necessitate a considerable increase in the size of the Petersen coil necessary to balance the system capacitance together with this added capacitance.

From time to time tests have been considered to determine the upper limit of magnitude of the in-phase component of ground-fault current that can be successfully extinguished by the use of Petersen coils on various systems. The author points out clearly that not only is the magnitude of such currents an important factor, but the differences in recovery rates of various systems would probably widely affect the maximum currents that could be successfully handled.

Referring to conclusion number 5, it should not be overlooked that there are a number of other factors besides recovery voltage which must also be given careful consideration in analyzing the possible application or performance of Petersen coils on a given system. These include such factors as the magnitude of dynamic voltages experienced on nonfaulted phases, system operating voltage with respect to the corona limit, system network complexity, and required isolation of permanent faults.

A. U. Welch (General Electric Company, Pittsfield, Mass.): Mr. Eaton's statement that "Peterson coils and associated grounding transformers should be designed for low loss" might give the impression that special designs with abnormally low loss would improve the performance by lowering the voltage recovery rate.

However, the I^2R loss in a transmission line and its ground return when supplying charging current to ground is of the same order of magnitude and often higher than the loss in a normal-design Petersen coil. Furthermore, particularly in high-voltage systems, the corona loss with a ground fault is generally much higher than the resistance loss in lines, ground, and Petersen coil. Therefore, reducing the loss in the Petersen coil reduces the total loss only slightly and has negligible effect upon recovery voltage.

J. R. Eaton: Mr. Welch points out that it may be impracticable to reduce the losses in the resonant circuit beyond a certain point. Further attempts to decrease this loss may result in an increased cost entirely out of proportion to the advantage obtained. As mentioned by Mr. Hunter, a reduction of these losses will result in an increase in the residual voltage on the system during normal operating conditions. Hence we find here, as in almost all engineering problems, that we must give consideration to all factors involved and choose as our solution that value which will give the best over-all operation.

Mr. Hunter points out that if capacitors were used to increase the stored energy in the resonant circuit, they might be located at the line terminals of the grounding transformer bank. Although this connection would be satisfactory from the standpoint of system-recovery-voltage characteristics, it may be observed that capacitors so connected would be at all times subjected to full line-to-ground potential. If connected to the system neutral, as suggested in the paper, the capacitors would be subjected to line-to-neutral voltage only during the fault period.

The comments of Mr. North and Mr. Von Voigtlander are of importance in that they call attention to the necessity of a broad consideration of the problem before justifying an installation of Petersen coils on a system. The present paper treats only the factors affecting arc extinction. This of course is only one of the many problems which must be considered in Petersen-coil application.

Induced Current in Parallel Circuits and Its Effect Upon Relays

E. H. BANCKER
MEMBER AIEE

MUTUAL INDUCTION is very much like friction in that it is a great blessing under certain conditions and an unmitigated nuisance under others. If it were not for mutual induction there would be no a-c systems as we know them today because there would be no transformers. It is apparent, therefore, that mutual induction is the basis of an entire industry that could not exist without it. On the other hand it has been the bane of the communication industry where induction between circuits is highly undesirable. There is a story told about one of the early long-distance open-wire telephone circuits having several parallel lines in which the experimenter at one end spoke to the man at the other end and asked "Do you hear me?" The reply was, "Perfectly." The first man then asked the second, "Which line am I on?" and after a moment's hesitation the second replied, "All of them." These two illustrations show that mutual induction may be either a blessing or a curse depending upon the circumstances under which it exists.

In three-phase power systems, mutual induction is usually negligible for one or both of the following reasons. In a great majority of installations, the spacing between conductors is so small in comparison with the spacing between circuits that comparatively few of the lines of flux produced by one circuit link the other. The second reason is the presence of transpositions which are commonly employed to balance circuits and to minimize unbalances between circuits caused by mutual induction. If each conductor of each three-phase circuit occupies each of its nine possible positions with respect to each conductor of any other adjacent three-phase circuit for one-ninth of its length, the net effect of one balanced three-phase circuit upon another will be zero. Transpositions of transmission line conductors are normally worked out to approach this ideal quite closely.

The flux field generated by zero-sequence (or ground) current, that is, current flowing out over one or more conductors and returning through the earth, is quite different from that existing in a balanced three-phase system. In this case the circuit spacing of two parallel circuits on the same towers or the same right-of-way may be small in comparison with the conductor spacing since the conductors are the wires of the transmission line and the path of the current in the earth. Furthermore, no transpositions are possible. As a result the mutual inductance between circuits carrying zero-sequence current may vary from zero for widely separated circuits to perhaps 75 per cent of the self-inductance in the case of a double-circuit tower line. A mutual inductance of the order of 50 per cent or greater is far from negligible in determining the distribution of flow of current in the parallel circuits, as will be shown.

To most people resistance and self-inductance are readily understandable terms, but mutual inductance being less commonly encountered seems to be a little more difficult to comprehend. Perhaps the explanation which made its nature clear to the author will be helpful to others, that is, an exact definition of what mutual inductance is. Mutual inductance is the flux linkage with one circuit per ampere in another. Let us examine this statement as applied to a transmission line conductor and see what it says. Everyone knows that self-inductance is the flux linkage per ampere in the circuit itself. In other words, it is the total number of flux lines surrounding the current of one ampere flowing in the circuit. Returning now to mutual inductance, it will be seen that it is merely the number of flux lines around a conductor that results from a flow of one ampere in some other conductor. These statements apply to straight cylindrical conductors, such as transmission lines. When the conductor is in the form of a coil, such as in a transformer, the flux linkage is the sum of all of the flux lines times all of the turns through which they go, when a current of one ampere is flowing.

The transition from mutual inductance to mutual reactance is now very easy to see because reactance is merely the time rate of change of flux linkages per ampere. In other words having established a mutual inductance it is merely necessary to multiply it by $2\pi f$ to obtain the mutual reactance.

There is also present some mutual resistance because the return circuit for both the inducing and the induced currents is in the earth which has some resistance. Accordingly, two adjacent circuits have a mutual impedance containing both a resistance and a reactance term. The formula for calculating the mutual zero-sequence impedance between transmission circuits carrying zero-sequence current will be found in any standard reference book on transmission line calculation[1] and is

$$Z_{m0} = 0.00477f + j0.01397f \log_{10} \frac{D_e}{\text{G.M.D.}}$$

ohms per mile per phase

where f is the frequency, D_e is the distance between the equivalent conductor and its image, and G.M.D. is the N^2 root of the product of the N^2 possible distances between the N conductors of each of the two circuits. For three-phase circuits this becomes the ninth root of the product of nine possible distances.

Where there are many circuits to be calculated, much time may be saved by referring to figures 77 and 82 of the book "Symmetrical Components" by C. M. Wagner and R. D. Evans. From these figures the equivalent depth to the image and the mutual zero-sequence reactance may be obtained directly, and the only computation necessary is the derivation of the geometric mean distance between the conductors designated as G.M.D.

Whenever two circuits having mutual zero-sequence reactance are electrically connected at one or both ends, an equivalent circuit may be drawn showing both the self and mutual zero-sequence impedances,[2] and this will be found most convenient in setting up the impedance diagram for the purpose of calculating the flow of zero-sequence current. Where the lines are bussed at both ends and a fault exists along the length of one of the circuits, two equivalent impedances may be set up, each representing the network in one direction from the point of fault and the two equivalent networks connected together and reduced by the well-known wye-delta method or set up on a calculating table. See figure 1*b*.

The effect of mutual reactance between circuits is to cause circulating current, or a

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Paper number 39-30, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 26, 1938; made available for preprinting December 19, 1938.

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[1] For all numbered references, see list at end of paper.

redistribution of the ground fault current as calculated without the effect of mutual zero-sequence impedance. The first evidence of this probably manifested itself in connection with directional ground relays where an inexplicable lack of selectivity appeared. Some years ago, before the methods of calculating the flow of zero-sequence current were as well known as they now are, cases would frequently arise in which there was an apparent loss of selectivity between directional ground-current relays that, according to calculations, should have had plenty of time interval between them. In the light of subsequent knowledge it is easy to understand how the time interval became too small to retain selective action.

As the use of directional ground-current relays on parallel circuits is still very common, it may be worth while to show why it is that a fault on one of the lines will occasionally trip three breakers. For the sake of simplicity assume that the zero-sequence impedance external to the two parallel circuits of figure 1*a* is the same at both ends and that a fault occurs at *X* in line *A* near *H*. If the line zero-sequence impedance is fairly high in comparison with that of the grounding transformers at the ends, a large proportion of the zero-sequence current will initially flow through the neutral of the grounding transformer at *H* and the relays of circuit breaker 1. In the meantime current will flow in a tripping direction and possibly of tripping magnitude through breakers 3 and 4, but presumably breaker 1 will open before either of these operate or no kind of selectivity would be obtained. After breaker 1 has opened, it might appear that the major part of the ground current would now return through the neutrals of the transformers at *T* since these are apparently nearer the fault than those at *H*. As a matter of fact, if the mutual zero-sequence impedance between lines is one-half or a greater proportion of the self-impedance of one line, then one-half or more of the ground current will return to the neutrals of the transformers at *H*. This is true regardless of the length of the circuits providing only that the mutual reactance is one-half or more of the self-reactance, and also assuming equal impedance of the grounding transformers. See figure 1*b*. As a result of the opening of breaker 1, breaker 3 relay then gets more current than breaker 2 relay, but by no means so much more as might have been expected, and accordingly, breaker 2 relay, if set without consideration of the effect of mutual impedance, may succeed in tripping before breaker 3 has cleared the short circuit.

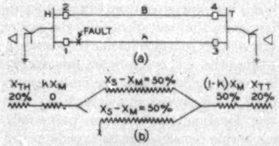

Figure 1

(*a*) One-line diagram of parallel line system

(*b*) Equivalent zero-sequence reactance diagram for ground fault after breaker number 1 opens

X_{TH}—Zero-sequence reactance external to *H*
X_{TT}—Zero-sequence reactance external to *T*
X_S—Zero-sequence reactance of one line
X_M—Mutual zero-sequence reactance between lines
k—Relative distance from *H* to fault

Mutual zero-sequence reactance is no respector of persons and is not concerned with whether the two circuits involved have any electrical connection with each other or not. Its action is not confined to parallel circuits of the same voltage or even the same frequency and this has occasionally resulted in some rather mysterious behavior on the part of directional ground-current relays. Figure 2 illustrates a condition which has been known to cause operation of directional ground relays on one overhead grounded-neutral system that is paralleled by an electrically separate grounded-neutral system for a part of its length. Ground faults on the second line cause flux linkages with the first line that generate a voltage between the ends of each of the three conductors. Since both ends of every conductor of the first line are connected to ground through the wye windings of wye-delta transformers, there is a comparatively low impedance path around which these induced voltages can force a flow of current. It will be observed that at one of the transformer banks the direction of current flow is from the ground to the neutral and up through the winding and out over the transmission line while at the other transformer, the flow is reversed, being in on the line and down through the transformer to the ground. The relative direction of the current in the neutral and the current in the line is the same in both cases and is such as to cause the relays to act as if there is a flow of fault current into the line at both ends. If this is sufficient in size and duration, one or both of the directional ground relays will operate to clear a circuit which is electrically isolated by a double transformation from the faulty section.

An even more mysterious and obscure case occurred on an underground system in a city where there are 25- and 60-cycle cables in the same duct bank under the street as shown in figure 3. One of the 60-cycle cables formed a portion of a loop circuit which was protected with directional ground-current relays. On several occasions faults on the 25-cycle system that caused ground current to flow in one of the 25-cycle cables in the duct would trip 60-cycle directional ground relays. At first thought it might seem that the metallic sheaths of the cables would act to shield the conductors of one cable from the flux field around the conductors of another carrying ground current. Actually the sheaths do have some such effect, but on account of the relatively high-resistance material of which they are composed, they are not a very effective shield. Accordingly, there were enough effective flux linkages with the conductors of the 60-cycle cable to force sufficient current around the loop of which it was a part to operate the ground relays. If the system is grounded at the source only, the ground-relay torques are in the directions indicated in figure 3, and increase progressively away from the grounding point. If the system has more than one ground the current circulating in the earth would change the torques, but half of the relays in the loop tend to trip.

Where two parallel identical circuits have unequal mutual coupling with a third circuit, any ground current in the latter generates a voltage around the loop formed by the two parallel circuits. This voltage produces a circulating current which is superimposed on any ground-fault current that may be flowing in the two circuits at the moment, thus tending

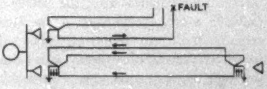

Figure 2. Flow of currents in a circuit exposed to zero-sequence induction

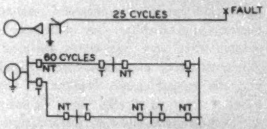

Figure 3. Flow of current in a loop exposed to zero-sequence induction

T—Tends to trip
NT—Tends not to trip

to increase the current in one line and reduce it in the other. Where the lines are protected by balanced current relays in the residual circuit of the current transformers, faulty operation may result from the superposed circulating current on top of the balanced through current. The circulating current may be calculated by subtracting the mutually generated zero-sequence voltages in the exposed section and dividing it by the loop zero-sequence impedance of the two circuits.

Three or more parallel circuits on the same right-of-way necessarily have unequal mutual zero-sequence impedances so that through ground-fault current will not divide evenly between them. The middle circuit will carry less current than the others which may cause false operation of ground-current relays.

Mutual zero-sequence reactance is open to indictment on still another count. It is one of the factors that make the use of distance relays for ground protection so complicated that it becomes very nearly impracticable. If it were not for the mutual reactance between circuits, distance relaying for ground faults would be no more complicated than for phase faults, but the fact that the voltage generated between the fault and the relay location in the faulty conductor may contain a large mutual zero-sequence component makes it necessary to take into account in each relay the zero-sequence current in all parallel circuits in order to secure accurate distance measurement. It is quite apparent that where there are several circuits on the same right-of-way, the problem becomes pretty complicated if the currents in all of them have to be conducted to the relays in all the others. It is quite probable that this is the chief reason why distance ground relaying has not become very prevalent.

Although not strictly induction, there is another source of unbalanced residual currents in parallel lines that must be considered when very fast relays are used. When one circuit of a pair is carrying load and the second circuit breaker on the other is closed to parallel them, its poles will strike sequentially electrically regardless of the excellence of mechanical adjustment. There is a moment during which only one wire of the incoming line is in parallel, then a second interval when two wires are completed and finally all three are closed. For the first two intervals there is a zero-sequence current in each line. Where the system neutral is ungrounded this current flows only in the loop formed of the two lines and is equal in each and, therefore, would not tend to operate residual balanced current relays. Where there are grounded neutrals on both ends of the lines there is a true zero-sequence current flow via the grounding transformers and the earth and it is unbalanced in the two circuits.

For the condition of one pole of the breaker closed, the current may be calculated from an equivalent circuit in which the negative and zero-sequence impedances as viewed from the breaker are placed in series and inserted in the positive-sequence network as a series impedance at the breaker location. When two poles are closed the negative- and zero-sequence impedances are paralleled and connected in series with the positive-sequence network at the breaker location. From calculations like these the effect on balanced and directional ground relays may be determined.

Ground-fault currents generate voltage in any mutually coupled conductor without regard to the use to which it is put. Pilot wires used for relaying, telemetering, and similar purposes may have excessive voltages induced in them if they are in close proximity to aerial or underground power conductors carrying zero-sequence currents. The voltage is usually very small between wires but the voltage to ground may be high on all of them, thus endangering the insulation.

Mutual resistance, too, has caused insulation failures in pilot wires and equipment connected to them. The fault current returning from the earth to the neutral of a grounding transformer passes through whatever resistance there is between true earth and the station grounding system to which the transformer neutral is connected. The resultant *IR* drop displaces the station ground from true earth potential. The cases of equipment connected to the pilot wires are ordinarily grounded to the station ground and the pilot sheath is usually grounded purposely or accidentally to the earth along its length. The insulation of the equipment and the pilot wire in series is subjected to the potential between station ground and earth and is stressed in inverse ratio to the capacitances between the equipment and station ground and between the pilot wires and their sheath. In most cases the equipment capacitance is small and its insulation gets most of the voltage across it. Sometimes the reverse is true.

Having outlined some of the effects of mutually induced current, it is quite appropriate to discuss some of the methods which have or may be applied to circumvent the harmful consequences. The simplest point of attack is in the relay setting. In the first instance discussed it will often be found that once the correct distribution of residual or zero-sequence current has been determined, a change in the settings of the relays will give the desired selectivity.

In figure 2 the relay at the station where both circuits are grounded can be made to recognize the true direction of fault current by energizing its polarizing coil from the sum of the secondary currents of current transformers in all of the power transformer neutrals. The ratio of these current transformers should be in the inverse ratio of the voltage ratings of the system in whose neutral they are connected so that an equal kilovolt-amperes in each system will give equal secondary currents. The actual fault current which is doing the inducing will be greater than the induced current (in kilovolt-amperes) and hence the net current fed to the relay polarizing coil will be reversed from what it would have been had it been energized from a current transformer in the neutral of only the power transformer to which its line is connected. At the other end of the circuit this remedy is not available because the fault current itself is not present in this station. For this location there is no universally applicable remedy, but an expedient has been used that should be successful in many installations. The line-to-neutral voltages of the circuit in which the induced current flows are usually higher during the induced-current condition than they are while a ground fault exists in the line itself. This fact may be utilized through the use of three instantaneous undervoltage relay elements, the coils of which are energized from the line-to-neutral voltages and the contacts of which are all in parallel and the group in series with the directional relay whose misoperation is to be prevented.

While the author is not aware of any installation made for the special purpose of reducing mutual zero-sequence reactance between circuits, it is a fact that counterpoises and good-conducting ground wires tend to reduce the mutual zero-sequence reactance. This will be readily apparent when it is considered that the presence of these conductors brings into proximity with the inducing current, a returning ground current of opposite direction. This returning current also has a mutual coupling and since it is in the reverse direction tends to cancel out part of the voltage generated by the outgoing current in the parallel circuit conductor. If all of the ground current could be persuaded to return in a ground wire or counterpoise spaced the same distance from the circuit in which the voltage is induced as the inducing circuit conductors, it would entirely cancel out the mutual reactance between the circuits. A perfect result is unobtainable but may be approached through the use of good conducting ground wires and counterpoises and by increasing the separation between the parallel circuits through the use of separate rather than twin circuit towers. It is not proposed by the author that this should be adopted as an economical remedy for false operation of ground relays, but is merely pointed out as another one of the advantages incident to the use of counterpoises and ground wires.

The use of conducting shields for pilot wires has been proposed since the cost is not prohibitive as it may be for power circuits. Where momentary interruptions are not harmful vacuum gaps or Thyrite resistors have been used to limit the voltage from pilot wires to ground. In other installations a higher insulation level has been provided, capable of withstanding the induced voltages. Occasionally neutralizing transformers[3] have been installed to allow station equipment to stay at station ground potential and the pilot conductors at true earth potential. Insulating transformers at the station boundary have also been employed where the quantity sent over the pilot wire is alternating current. The choice between the various remedies is one of economics for the particular installation.

Conclusion

Mutual impedance exists between adjacent circuits carrying zero-sequence or ground current. Its effect is to cause a different distribution of ground current than would have existed without it. Failing to take it into consideration has occasioned incorrect operation of ground relays, usually of the directional type. It has also caused false operation of balanced ground-current relays. Its existence makes distance ground-fault protection difficult. It also endangers the insulation of pilot wires and equipment connected to them. A number of remedial measures are available, but there is no universal, economical panacea. Each instance requires individual consideration to determine which of the several available remedies may best be utilized.

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Discussion

R. P. Crippen (Ebasco Services Incorporated, New York, N. Y.): In 1931 trouble was experienced from incorrect relay operation on the 13.2-kv system of the Tennessee Public Service Company in Knoxville, Tenn.

This system was fed from a single 110/13.2-kv substation of about 30,000 kva. Six overhead 13.2-kv tie lines ran from this substation to various other substations about the city. These substations were in turn joined together by other 13.2-kv tie lines. There resulted a network of 13.2-kv circuits, most of which were not more than two or three miles long and many of which were arranged two or three to a pole line. No transpositions were used. The system was grounded through a 20-ohm resistor in the neutral of one of the 10,000-kva transformer banks at the main substation.

Tests were made and it was found that for straight ground faults induced zero-phase-sequence currents up to a maximum of 90 amperes would flow in unfaulted lines. This indicated that ground relays on the lines thus affected should have a current setting which would allow for the induced currents.

A more difficult problem arose when simultaneous ground faults occurred at different points on the system on different phases, as sometimes happened. The resultant unbalanced currents in the lines were several times as great in magnitude as the current to a single ground fault, which was limited by the 20-ohm resistor. These heavy currents circulating between the two ground faults resulted in unbalanced currents in individual circuits which were much heavier than the straight zero-phase-sequence currents to ground faults. The resultant "mix-up" from unbalanced and induced currents was bewildering.

Reasonably good operation was obtained by increasing the current setting of certain of the ground relays, by applying the directional control feature to both phase and ground directional relays at certain points, and by installing faster-acting relays at two or three points.

The case illustrates what can occur under certain conditions and what is undoubtedly occurring to a lesser degree in many instances. Fortunately effects such as this are not usually sufficient in magnitude to cause any trouble to the relay engineer.

R. E. Neidig (Metropolitan Edison Company, Reading, Pa.): Mr. Bancker's paper will be of decided interest to every relay engineer who is faced with the problem of relaying grounded-neutral systems, and while the subject is not new, I believe that it has been given too little publicity in the past.

Several years ago it was discovered that mutual induction was the cause of incorrect relaying on one section of the 110-kv system of the Metropolitan Edison Company, and it is believed that a review of that instance at this time will be interesting.

Figure 1 of this discussion shows the central section of the aforementioned 110-kv system. The circuits between South Reading and Glendon are on two-circuit steel towers, and likewise are the circuits between Glendon and Gilbert. However, the latter tower line is on the same right-of-way with the South Reading-Glendon tower line for a distance of six miles out of Glendon. There are no ground wires on either tower line.

After experiencing a few cases of instability at Gilbert plant from 110-kv faults, high-speed balanced-phase and ground relays were applied at the three stations shown, and the oil circuit breakers modernized at Gilbert and Glendon. Immediately after this modernization program was completed, it was discovered that ground faults occurring on the South Reading-Glendon circuits were, in a number of cases, causing peculiar ground-relay operations on the Glendon-Gilbert circuits. In most cases where these peculiar operations occurred, the oil circuit breaker on one circuit at Gilbert was tripped by the balanced-ground relay, and the oil circuit breaker on the other line at Glendon was tripped by its balanced-ground relay. Occasionally only the oil circuit breaker at Gilbert was tripped. Obviously, something had to be done about it.

This type of operation indicated that the residual currents in the Glendon-Gilbert circuits was becoming unbalanced for some reason, and as this condition did not exist for faults beyond Gilbert toward West Wharton, it suggested induction in the six-mile section out of Glendon as the probable cause.

Inspection showed that the maximum induced voltage in the Glendon-Gilbert circuits would occur during a ground fault on the adjacent South Reading-Glendon circuit at a point where the two tower lines left the common right-of-way. Results of calculations made under these conditions are given in figure 1, showing that the loop current induced in the Glendon-Gilbert circuits caused as much as 61 per cent unbalance in their balanced-ground relays, which was far more than sufficient to cause operation.

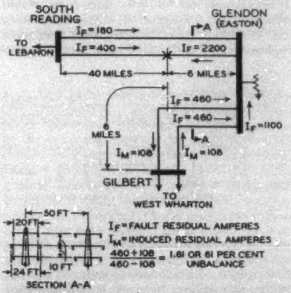

Figure 1. Section of 110-kv system of Metropolitan Edison Company affected by induced current and results of calculations for typical case

There were two apparent means for

eliminating the peculiar operations caused by this condition, (1) reducing the sensitivity of the balanced ground relays, requiring either changes in the relay or additional auxiliary relays, or (2) removing the balanced-ground relays from service. While the latter means of correction may seem more or less rash, it must be pointed out that the current obtained in the faulted phase during a ground fault at any location on the Glendon-Gilbert circuits is very high, and of sufficient magnitude to operate the balanced-phase relays. Inspection of operating records showed that in practically no case did a balanced-phase relay fail to operate together with the balanced-ground relay for ground faults on these circuits. This fact, determined quickly and easily, led to the temporary disconnection of the balanced-ground relays, although thus far they have not been restored to service. Each circuit, however, is still equipped with back-up ground-relay protection.

It will be interesting to note that ground faults on the Glendon-Gilbert circuits produced no apparent peculiar operations of the South Reading-Glendon circuits, due, no doubt, to the appreciably higher loop zero-sequence impedance of these circuits. The balanced-ground relays on these circuits are still in operation, although it was necessary to reduce the sensitivity of them due to the difficulty encountered from sequential operation of oil-circuit-breaker poles, a source of trouble also referred to in Mr. Bancker's paper.

Wm. E. Marter (nonmember; Duquesne Light Company, Pittsburgh, Pa.): Mr. Bancker has presented a very interesting paper on the effect of induced current on relaying. This problem has been encountered in many places on the Duquesne Light Company system and in one instance has been successfully solved by a scheme installed during 1932, and which to my knowledge has not been published.

Figure 2 of this discussion shows the arrangement of 66-kv lines on which trouble was encountered. The lines were on double-circuit towers over separate right-of-ways with one Pine Creek line and one North line on each tower line. A fault near North substation on a North-Colfax line caused a circulation of induced current in the pair of lines between North and Pine Creek and between Pine Creek and Colfax.

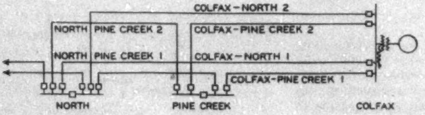

Figure 2

The protection used on the lines was seven-post balanced *CR* relay protection with both phase and ground relays. The circulating induced current in the lines added together in the neutral and caused incorrect operation of the ground relays, opening one breaker on the opposite end of each of the four lines in which induced current was circulating.

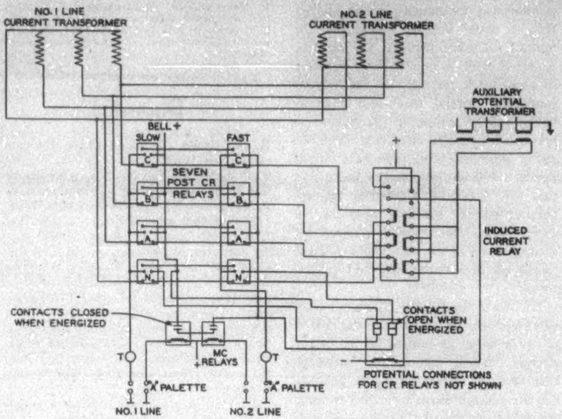

Figure 3. Induced-current relay used with seven-post CR balanced line protection

The protection used on the lines and the relay which was used to prevent incorrect operation is shown in figure 3. The fact that the induced current flowed in each of the three-phase leads to the balanced relays and this was the only condition which would produce three in-phase currents in the phase leads on a ground fault was used to determine the lockout condition. The current coils of three double directional elements were connected in series with the balanced phase *CR* relays and a zero-sequence potential from star-delta potential transformers was connected to the potential coils of each element.

On a ground fault, potential was produced due to the ground and if induced current existed all three directional elements operated in the same direction. The contacts on each side were connected in series and operated an auxiliary relay to prevent tripping by the ground relays.

On this system a 63-ohm neutral resistor is used so that the zero-sequence potential is always of about the same value and comparatively large. The ground current is limited in value and the ground relays have sensitive settings. The induced current was of sufficient magnitude to operate the ground relays but did not operate the phase relays and it was not necessary to provide a lockout for them.

This scheme was in successful operation for several years but has been removed due to line rearrangements on the towers which removed the induced current problem.

An additional installation was made about 1932 on the 22-kv system using the original shop model of the relay. Balanced protection was used on only one end of the pair of 22-kv lines and one relay could be used in this location to prevent induced-current operations.

W. A. Lewis (Cornell University, Ithaca, N. Y.): Mr. Bancker has performed a service to electrical engineering by collecting in a compact form the references to the numerous problems produced by mutual induction between parallel circuits.

Many of the engineers working in this field have accepted these problems as they arose and solved them one by one, without realizing that the uninitiated would find them difficult when suddenly confronted with one of the more unusual problems. Hence, by describing in a simple fashion all the related problems, Mr. Bancker has provided a convenient and useful reference.

In describing the case of two parallel lines, after one breaker opens, Mr. Bancker shows that the greater portion of fault current may come from the end nearest the fault, even though the circuit breaker at that end has already opened, particularly when the mutual impedance is greater than half the self-impedance. The reason for this is, of course, that the mutual impedance reduces the effective impedance around the loop. Our experience has been that on double-circuit lines, where both circuits are on the same tower or pole, zero-sequence mutual impedance is often as great as seven-tenths of the self-impedance, so that the effect described is usually very important.

Mr. Bancker has explained that the use of distance relays for ground fault protection has been retarded by the mutual effects. As I was a party to the first paper which explained, more than seven years ago, how distance relays could be used for ground-fault protection on parallel lines, to give an accurate determination of the fault location, it is perhaps appropriate to discuss additional reasons why more widespread use of distance relays for ground faults has not been made. The first of these is cost. With distance ground relays, three relays at each terminal location are required in order to protect against a fault on any phase. In some cases a smaller number of relays has been used, but some form of additional relay is necessary to provide switching so that the relay can be connected to the proper phase for the fault which occurs. This compares with a single ground relay in the schemes of ground protection now in common use. The second difficulty is the effect of fault resistance during a two-line-to-ground fault. Because of the phase relationship between the voltage drop in the fault resistance and the voltage drop in the line, it is found that even with a reactance relay a fault frequently may appear to be closer to the relay location than it actually is, thus sometimes producing an incorrect operation. To overcome this difficulty, it is necessary to arrange the relay so that it will not operate when the fault involves two phase conductors. This necessitates either considerable complication in the relay design or the use of some additional fault-selection device. However, I venture to predict that if single-pole switching of single-conductor faults comes into common use, distance relays will find an extensive application for ground protection.

Bert V. Hoard (Westinghouse Electric and Manufacturing Company, Newark, N. J.): Mr. Bancker has discussed a number of cases where mutual impedance between two or more lines or loops might cause false operation of directionally controlled ground relays. He also discussed certain methods of correction so that the relays would operate properly. Among these was the use of the summation current through all the transformer neutrals for polarizing the directional element, and the fact that if ground wires have been installed or the distance between the respective circuits is large, the effects of mutual induction are materially decreased.

There is available another relatively new method which, we believe, will become increasingly important as it becomes better known. This is the use of a negative-sequence directional element instead of the conventional zero-sequence directional element in a relay, which uses negative-sequence voltage and current for determining the direction of the fault from the relay. In a grounded system for any fault-to-ground there are always negative-sequence currents produced as well as zero-sequence currents. However, the negative-sequence currents must flow in the line conductors; hence their effect by mutual induction practically cancels in any adjacent circuit even though the spacing between circuits is small. It is only on those systems where the wires of the adjacent circuits are not transposed frequently enough and a fault occurs within a relatively long untransposed section that there might be appreciable unbalanced induction in one circuit due to negative-sequence currents flowing in the other.

This method of directional control is available in a new type *CRS* relay which consists of a conventional zero-sequence overcurrent element directionally controlled by a very sensitive negative-sequence directional element and its filters. It will be noted that only two potential transformers connected open delta are required for polarization, while for a conventional zero-sequence voltage-polarized relay three star-connected potential transformers are required; hence, considerable economic saving may be made for many installations when using the new relay.

For loop circuits an instantaneous negative-sequence fault detector may be required and can be obtained with the relay to produce selectivity between a fault in the loop and a fault external to the loop. As an example, in figure 3 of the paper assume the faulty circuit is 60 cycles and is connected to the 60-cycle bus, also that a generator is available and running at the remote bus. During a fault external to the loop this generator feeds power back through the loop, but the negative-sequence current flowing will be small when compared to that flowing when a fault occurs within the loop. The negative-sequence fault detector is adjustable so that it can be set to select between internal and external faults. Its contacts are in series with the directional element so that the zero-sequence overcurrent element cannot trip until the negative-sequence current exceeds the fault detector setting and is in the tripping direction for the directional element. If the above-mentioned generator is not operating, there should be negligible negative-sequence current flowing in the loop circuit for an external fault.

It will be seen that the big advantage of using negative-sequence currents for the directional element and fault detector is that there is a relatively large difference in the negative-sequence current flowing in the loop for an internal fault compared to a fault external to the loop; while if zero-sequence currents were to be used, the mutual induction between circuits would cause the relative currents to be more nearly equal in one branch of the loop for both internal and external faults and thus limit the possibilities of selectivity.

Edward W. Kimbark (Northwestern University, Evanston, Ill.): There seems to be a common impression that zero-sequence current is always associated with ground current. Mr. Bancker helps to correct this impression by giving two instances of circuits in which zero-sequence current is present without any ground current: (1) the loop circuit of figure 3, either ungrounded or grounded at one point, in which a zero-sequence current is induced from another system; (2) a similar loop circuit with a series open circuit of one or two conductors, due for example to the sequential closing of the poles of a circuit breaker. Nevertheless, Mr. Bancker lapses into the common error when he mentions "a true zero-sequence current" in case 2 if the circuit is grounded at two points. A third instance, one not mentioned in the paper, is a short circuit between a conductor of one circuit and a conductor of another circuit, as on a double-circuit line. (By "ground current" in the foregoing is really meant current in the neutral of a grounding transformer, for current will circulate in the earth beneath the loop of figure 3.)

I wish to discuss the circuit of figure 3 in which false operation of zero-sequence ground relays on the loop may be caused by induced currents from a ground fault on another system of the same or a different frequency. If the loop system is grounded through a high neutral impedance, it is especially susceptible to false operation of ground relays because the relays would be given low current settings. There are several respects in which conditions on the loop circuit during induction caused by a ground fault on another system differ from conditions during a ground fault on the loop itself, and some of these conditions might be used to discriminate between the two events. For example, during a line-to-ground fault on the loop itself, there is negative-sequence current and accompanying negative-sequence voltage. The negative-sequence current in the fault is equal to the zero-sequence current, though the distribution of negative-sequence current throughout the system may be somewhat different from that of the zero-sequence current. Negative-sequence relays are not susceptible to induced currents because the negative-sequence mutual impedance between the two systems is negligibly low. Negative-sequence quantities could be used to operate watt-type relays or to operate either the current element or the directional element or both of directional overcurrent relays. Relays working entirely on negative-sequence quantities would respond to all types of faults except three-phase, but this would do no harm. If set too low, they would respond also to unbalanced loads, and this fact, as well as the low value of negative-sequence voltage during line-to-ground faults, would prevent their use on a system grounded through high impedance.

On such a system another means of discrimination can be used. The zero-sequence voltage is high (practically equal to the normal positive-sequence voltage) during a line-to-ground fault, but is low during induced currents. Therefore a zero-sequence voltage relay could be used at each station of the loop to prevent operation of the zero-sequence ground relays unless the zero-sequence voltage should exceed a set value; or possibly the directional elements of the ground relays themselves could be adjusted so that they would not close their contacts except on fairly high torques.

J. A. Elzi (The Commonwealth and Southern Corporation, Jackson, Mich.): This paper calls attention to an important factor in the determination of the magnitude and distribution of zero-sequence currents where parallel lines are involved. The effects of mutual impedance are frequently quite surprising and make it very difficult to make a preliminary relaying layout until a complete study has been made. It is also very important that various system operating conditions and fault locations be investigated because a change in magnitude or direction of current in one line may greatly affect the currents in other lines.

Tests which were made recently on an extensive 22-kv grounded neutral system in which there are numerous parallel circuits in close proximity, demonstrated the above conditions very well. In this particular case, for example, a change of approximately two to one in current distribution between two lines occurred, due primarily to the differences in the effect of mutual impedance for two test conditions. The test values were in quite close agreement with calculated values, which indicates that the methods of calculation now commonly used and referred to in this paper give very satisfactory results.

E. H. Bancker: As it was hoped, the discussions brought out several interesting cases of induced current experiences to supplement those cited in the paper. Professor Kimbark's and Mr. Hoard's discussion also points out another solution that may be adopted that was overlooked in preparing the paper. Negative-sequence induced currents should be relatively small in comparison with negative-sequence fault currents and, therefore, devices responsive to the negative-sequence should be free from tendency to misoperate for short circuits on other systems. As Mr. Hoard points out, their use sometimes results in a saving because only two potential transformers are required and these may be on the low-voltage side of a power transformer if it is certain that they will be energized at all times.

The main disadvantage in utilizing negative-sequence quantities lies in the fact that methods for deriving them are relatively inefficient in comparison with the methods of deriving zero-sequence quantities. In securing zero-sequence the sum of three current transformer secondaries or of three potential transformer secondaries is added together, giving three times the original quantity. To derive negative sequence it is necessary to use either a network or take the difference of two quantities containing positive-plus negative-sequence terms and positive-minus negative-sequence terms. In both cases the problem of deriving the true negative-sequence component is much more difficult than trying to separate out the zero-sequence component. In some of the instances investigated it was found that the negative-sequence components were too small a proportion of the total current or voltages to permit them to be utilized successfully.

A negative-sequence directional relay constructed from induction-cylinder elements known as the type *CBP* is available for use where the negative-sequence quantities are at least 15 per cent of the positive-sequence quantities. This relay consists of two elements operating upon a common shaft. One element is so connected as to produce a torque equal to the sum of the positive and negative sequence powers, while the other element produces a torque equal to the difference of these two powers. The resultant torque on the shaft is, therefore, either positive sequence or negative sequence, depending upon the connections used. These connections may be switched by a zero-sequence current relay so that the directional relay is normally responsive to positive-sequence power and operates in accordance with this quantity for three-phase and line-to-line faults. The presence of ground current switches the connections of one of the elements so that it becomes a negative-sequence power directional relay for all ground faults.

Professor Kimbark suggests that zero-sequence voltage might be used to distinguish between induced and actual fault current. There are probably cases when it would serve as, for example, in the system shown in figure 3. However, it is not universal in its application because the inducing current may be from the same system, in which case there would be as much zero-sequence voltage in the circulating loop as if the fault had been there. Also, if the system is like figure 2 the current circulating in the transformers produces as much zero-sequence voltage as the same amount of fault current, which conceivably might exist under some different generating condition. Accordingly, zero-sequence voltage is another possibility to add to the list, but is not an invariable selector.

It is hoped that this paper and its discussions will be of value to those who encounter the problem for the first time and wish to find out something about other cases of induced current and the solution adopted for preventing incorrect relay operation.

High-Speed-Relaying Experience and Practice

ABOUT 1930 the manufacturers introduced the high-speed circuit breaker with operating times in the order of eight cycles and at the same time engineers awoke to the possibilities of high-speed or one-cycle relays. The general requirements for such relays were set forth at the time in two papers[1,2] presented before the Institute.

After nearly ten years, a review of the experience gained with such relays seems to be in order, and to that end the relay subcommittee of the AIEE protective devices committee recently circulated a questionnaire among its membership designed to determine this.

This report is based upon the 13 replies received from typical operating companies and also the experience of the manufacturing companies in furnishing high-speed relays during the period. Because of the wide scope of the subject only the salient features can be touched upon here. For more detailed discussion, those interested are referred to the bibliography.

Instantaneous Overcurrent Protection

Most of the reporting companies use instantaneous overcurrent relaying, especially for ground protection, and report excellent performance. The reliability of the system as well as the improvement obtained from minimizing voltage dips and preventing burning down of conductors makes this method distinctly advantageous.

Settings reported for phase relays range from 100 to 200 per cent of the maximum symmetrical through fault current. While considerable benefit in clearing times can be gained in the case of long lines even though using a setting of 175 to 200 per cent, nevertheless one company reports over 1,000 installations using the 100-per-cent setting. An accurate record of the performance of instantaneous overcurrent relays has been kept by this company for two years which shows on steel-tower lines that 85 per cent of all faults are cleared by the instantaneous relays. On the portion of the system using mostly wood-pole line this record showed that 70 per cent of all faults were cleared by the instantaneous relays.

This same company sets the instantaneous ground relays at 110 per cent of the maximum through current and obtains correct operation.

One interesting experience noted was in the case of cable protection utilizing instantaneous relays. Here it seems that the fault is cleared so quickly that the cable will stand all tests and still break down later in service.

Distance-Relay Protection

Distance relays of the high-speed zone type have been used quite extensively for transmission-line protection against phase-to-phase faults. There are two kinds available: the reactance type whereby the distance is indicated from a measurement of the circuit reactance from the relay to the fault, and the impedance type which is based upon the measurement of the impedance.

The information obtained from seven companies who have distance-relay installations indicates that they have, in general, experienced entirely successful and satisfactory operation. Some unnecessary tripping has been occasioned

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Paper number 39-15, prepared by the relay subcommittee of the AIEE committee on protective devices, recommended by the AIEE committee on protective devices, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 27, 1938; made available for preprinting December 6, 1938.

Paper prepared by J. H. Neher, chairman; C. A. Muller, L. F. Kennedy, G. W. Gerell, and R. M. Smith; all members of the AIEE relay subcommittee.

1. For all numbered references, see list at end of paper.

by out-of-step conditions which are, of course, liable to cause operation of any type of relay. Also, on some of the earlier type distance relays constructional defects have appeared and been eliminated.

Analyses of the effect of synchronizing surges and out-of-step conditions on the distance relay have been made so that trip-outs which were formerly classed as incorrect relay operations can now be shown to be the result of the complex conditions attendant upon out-of-synchronism situations.[3,4]

In addition to unnecessary tripping under out-of-step conditions, there have been trip-outs on wide angular swings in cases where the system would have pulled back into step if permitted to do so.

It should be borne in mind that the contacts of the directional and impedance units should be carefully adjusted to insure proper time co-ordination particularly to take care of restoration of normal conditions on a heavily loaded line. A few unnecessary trippings have occurred because of incorrect co-ordination adjustments.

The distance relay is not generally applicable to protection against single-phase-to-ground faults unless elaborate methods of compensation are employed,[5] and as a result, very few installations of this type of protection have been made. One company, however, reported a number of installations of uncompensated impedance relays used for ground protection of important lines. On the whole, these have been successful when carefully applied on steel-tower lines with ground wires, but not otherwise.

No doubt in this case the successful operation was occasioned principally by the presence of the ground wire and lack of multiple neutral grounds. The ground wire has the effect of bringing the zero-sequence and positive-sequence impedance values closer together, thus mitigating the necessity for compensation.

Pilot-Wire Protection

The use of pilot-wire protection for relatively short lines has been constantly increasing and general use has been made of both a-c and d-c systems. The a-c systems are of the differential variety comparing currents at the line terminals usually with a relay having a percentage characteristic. The d-c systems, like the carrier-current protective systems, are of the directional comparison types, but use has been made of both the blocking type paralleling the carrier-current protective equipment, and the tripping type.[6,7] The tripping type appears to be most popular largely because it uses simpler relay equipment, although its application is somewhat more limited than is the case with the blocking type. All of these systems use relays operating in 0.01 second or less with a very marked tendency to the greater use of one- or two-cycle relays. Operating experience with these systems has been very good. Most of the d-c systems employ telephone-type pilot wires and the reports indicate that these circuits are giving very reliable service.

A recent development in a-c pilot-wire protection involves the use of extremely sensitive pilot-wire relays fed from phase-sequence networks which permits the use of a single telephone pair as the pilot wire to obtain protection against all types of faults.

Carrier-Current Pilot Protection

Carrier-current relaying has not as yet been adopted by many operating companies. However, such experience as those companies using it have obtained, indicates that distinct operating advantages are possible. The equipment itself being comparatively new might be expected to disclose numerous difficulties. Operating records of one company with many installations indicate that 1.2 per cent of the operations were incorrect, about one-third of which were chargeable to the carrier equipment.[8] The majority of the faults in the carrier equipment were due to a certain physical arrangement of equipment which failed to provide adequate weatherproofing. The majority of the faults in the relays were due to imperfections in relay or circuit design which have since been corrected.

Balanced Protection of Parallel Lines

Modern high-speed balance relays have a very definite application to many power systems,[9] and many installations have been made of both the current-balance and the cross-connected directional relay types. Such protection, of course, is only effective for single-line faults at a time when both of the paired circuits are in operation, and other relays must be installed to provide protection during single-line operation or in the case of double-line faults.

The application of balanced protection must be considered carefully since there are a number of factors tending to cause incorrect operations. Experience has shown that attention should be given to matching reasonably the current transformer characteristics on the two or more lines supplying the relay. If this is not done a through fault may readily cause operation of the balance relay, such phenomena having been reported on at least one system.[10]

Considerable difficulty has been experienced in applying this type of protection to lines which terminate on different bus sections interconnected through a tie breaker which may be tripped automatically and thus destroy the balance of the lines.

Protection of Lines With More Than Two Terminals

Lines having more than two terminals usually present relaying difficulties. There are available no schemes or methods of relaying especially designed for this application. The high-speed schemes available for two-ended lines such as distance, instantaneous overcurrent carrier, and pilot wire are used on tapped lines, but the high-speed protection rarely extends to all posts of the tapped line. The high-speed coverage with distance and overcurrent to be expected depends on the relative line lengths from the tap point of the terminals and will vary, if the equivalent impedance beyond each terminal changes.

Carrier and pilot systems using a directional indication for the basis of discrimination may also present difficulties because power may initially flow out of the section at one terminal for an internal fault, thus blocking the tripping of all terminals. This trouble can usually be cured by the use of high-speed distance or overcurrent backup relays.

Nine companies reported that they had no tapped or branched lines on high-voltage systems, hence had no application of high-speed relaying for tapped or branched lines. The schemes submitted by the other four companies were all based on the fact that the magnitude of the short-circuit current for faults in the section where it was not desirable to relay the line was not sufficient to operate the high-speed relays. In cases where it was impossible to accomplish this by the above means, either pilot-wire or carrier-current relaying had to be used.

Bus Protection

High-speed relays of either the overcurrent or impedance types have been applied in various bus protective systems and experience seems to indicate that satisfactory operation is being obtained. This is discussed in a report on bus protection currently presented before the

Institute (AIEE Transactions, volume 58, 1939, pages 206–11).

The application of the complete differential system with instantaneous overcurrent relays is becoming more common, although it is recognized that there are conditions which may result in a sizeable difference current flowing in the differential circuit during the first few cycles of a fault. From the available data it appears relatively safe to use instantaneous overcurrent relays on the bus differential system if the current transformers are chosen high enough in ratio so that they are never subjected to more than ten times their rated current under any condition.

A new development during the past year is the harmonic-restraint type of relay which offers a method of obtaining fast clearing of internal faults and at the same time preventing incorrect operation on the initial transient in the case of through faults.[11]

Effect of Transients on High-Speed Relays

The fact that transients are encountered which may affect relay operation has been reported from several sources. The effect of these transients on high-speed relays may either cause incorrect or unnecessary operations or operate to prevent operation of the relay when desired until the transient has disappeared.

Many of these transients have been present without causing any incorrect operation with the older types of time-delay relays. They become of more importance with the high-speed devices capable of operating in a total time of one or two cycles.

The most commonly encountered forms of transients and the corrective measures available at the present time are given in table I. A few general comments in relation to this table may be in order. The effect of asymmetrical current waves on overcurrent or distance relays is, of course, obvious. Operating experience, however, indicates that so few short circuits produce sufficient offset to cause incorrect operation that the fact may in general be disregarded except in the case of equipment installed on generating station busses.

Transformer magnetizing current is a well-known phenomenon which all transformer differential relays for years have attempted to take care of either by time delay or desensitizing equipment. The new factors introduced within the past year are the use of a tripping suppressor attachment or a harmonic restrained relay which recognizes the difference in wave form which occurs during the period of magnetization.

Table I. Types of Transients Affecting the Performance of High-Speed Relays

Type of Transient	Type of Protection	Result	Corrective Measures
Asymmetrical current wave	Instantaneous overcurrent or distance relays	Over reaching Incorrect tripping	Use of transient shunt
Transformer magnetizing inrush	Transformer differential relays	Incorrect tripping	Use of a desensitizing arrangement, tripping suppressor, or harmonic restraint
Expulsion-gap operation	Instantaneous overcurrent or distance relays	Incorrect or unnecessary tripping	Add time delay relay in tripping circuit
Nonsimultaneous breaker-pole closure	Ground relays	Incorrect tripping	None
Current-transformer saturation	Differential protection, particularly for busses	Incorrect tripping on external faults	Change in current transformers or use of current or harmonic restraint
Potential-device secondary degree transient	Distance relays	Slow tripping	None

The use of expulsion gaps obviously subjects relays to short-circuit conditions for a short period of time, and if the relaying is to be fast under other conditions unnecessary tripping will result. It seems more desirable to use the existing high-speed devices for this service and enable them to ride over expulsion-gap operation by adding a definite-time auxiliary relay in the trip circuit.

In general nonsimultaneous breaker-pole closure has not been a source of trouble, although two or three cases have been reported where a transient maintained itself in the residual circuit long enough to cause unnecessary relay operations. The methods of correction have varied with each particular case, being largely dictated by the types of relays already installed. No general solution is available.

Potential devices in many cases will have a transient between the occurrence of a fault and the time the secondary voltage reaches its new level. This will prevent operation of distance relays until the voltage becomes low enough for the relay to operate. There is no solution to this available at the present time.

Conclusions

While it is apparent that the "ideal relay scheme" so earnestly sought after[12] has not yet been found, nevertheless it may be said that the efforts made in that direction have not been in vain, and that the following conclusions are evident.

1. The instantaneous overcurrent relay provides a simple inexpensive means of obtaining high-speed relay protection over a considerable portion of the line length and offers possibilities in the way of general relay-system improvement of which, to date advantage has not been fully taken.

2. Distance relays have proved themselves reliable if carefully applied and maintained, and are widely used. However, the relative complexity of this device, its general restriction to phase-to-phase fault protection only, and the fact that instantaneous protection is not provided for the entire line length, have caused engineers to look elsewhere for the ideal relay protection.

3. Carrier-current or metallic pilot protection appears to be the first choice of high-speed protective systems whenever the expense is justified. Experience has indicated that a remarkable degree of reliability can be expected from the communication channel whether it be of the carrier-current or metallic type.

4. High-speed bus protection can be successfully applied if proper steps are taken to prevent the operation of the differential relays on transients.

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12. RELAYING OF HIGH-VOLTAGE-INTERCONNECTION TRANSMISSION LINES, H. P. Sleeper. AIEE TRANSACTIONS, volume 52, 1933, page 808.

Discussion

S. L. Goldsborough (Westinghouse Electric and Manufacturing Company, Newark, N. J.): As is pointed out by the authors, some unnecessary and, at the time, unexplainable trip-outs of high-speed distance relays have been caused by out-of-step conditions. This should be no reflection on the distance-type relay, since it is obvious that it, as well as any other type of relay, should view the out-of-step conditions as representing an actual fault. Nothing effective was done to prevent the operation of distance relays on these conditions, other than the analysis of the situation, until the advent of the high-speed carrier systems. The availability of a carrier signal seemed to render the problem of supplying out-of-step blocking easy of solution. However, at least in the case of the carrier system employing a three-zone impedance relay, it turned out that the carrier signal played a very minor role in the operation of the out-of-step blocking scheme developed. In other words, the identical out-of-step blocking system used with the stepped-type impedance-relay carrier system can be used to provide out-of-step blocking for conventional impedance relays without carrier. The only function of the carrier signal in the out-of-step blocking scheme is to enable the relays to trip on an internal three-phase fault after the system is out of synchronism. Therefore, when this out-of-step blocking system is used on impedance relays without carrier, the ability to trip for three-phase faults during out-of-step conditions is secured only when the backup time element is arranged not to be blocked by the out-of-step relays.

Concerning the tendency of distance relays to trip on wide angular swings which would not result in out-of-step conditions, analysis has brought out that the probability of tripping on wide angular swings is minimized by using the same currents to actuate both the impedance element and the directional elements. For instance, if delta current is supplied to the impedance element then delta current should be supplied to the directional element and if star current is used it should be supplied to both elements. This precaution will not prevent all trippings on wide angular swings but the tendency to trip will be materially reduced.

While the 13 typical operating companies contacted are doubtless a fairly representative cross section of the field, it is felt that the paper would have been more valuable, both in the amount of information obtained and the number of conclusions deducible therefrom, if a large number of operating companies had been contacted.

V. E. Verrall (General Electric Company, Philadelphia, Pa.): This summary of high-speed-relaying practice comes opportunely after a period of intense development at a time when applications of high-speed relays will be made with increasing frequency. During this development we have met a succession of difficulties with transient electrical conditions, some in the relay windings and some in the protective circuit, conditions which were not important with the induction-disk relays because the transients were all over by the end of two to three cycles. In Europe the difficulties were avoided by introducing time delay of the order of six cycles before the relay is allowed to complete the tripping circuit. In this country we have devised means for overcoming each transient trouble. For example, modern distance-relay dephasing circuits are made electrically deadbeat; directional-relay potential circuits are provided with time constants which will give correct action. As time went on these snags have become fewer and fewer and now the one-cycle relay seems to be thoroughly practical.

Nevertheless, it may be wise to avoid the maximum relay speed when such speed will not add materially to the performance of the protective system. In general one-cycle relays can be adjusted to give a time delay of two or three cycles, thus achieving a desirable margin of safety. In a distance relay, for example, an extra cycle or two in the response of the directional element avoids the need for the refined co-ordination of directional and impedance contacts mentioned. The ill effect of nonsimultaneous breaker-pole closure upon ground relays will usually be avoided by using induction relays of the high-speed type with a few cycles time delay under these conditions instead of relays working in less than one cycle.

While the operation of carrier equipment has been successfully controlled during out-of-step conditions, nothing appears to have been done in this country to control distance relays. It is a simple matter, however, to prevent undesirable tripping of distance relays by a low-set fault detector which opens the trip circuit if the line impedance changes slowly indicating a power swing as opposed to the instantaneous change in impedance when the line is short-circuited.

About 80 General Electric reactance relays are now being used for ground faults. Satisfactory operation has been reported over the last three years. Reactance relays are much preferable to impedance relays on ground faults because there can be very appreciable resistance in the fault contact and ground circuit which is liable to vary between wet and dry periods.

In table I of the paper the use of the transient shunt is suggested for preventing undesirable operation on asymmetrical current waves. A simpler solution is the use of induction-type relays, such as the high-speed induction cup, which are substantially unresponsive to the d-c component of the current wave.

Paul O. Langguth (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): While the committee states that no solution to the problem of potential-device transients is available, our experience indicates that satisfactory relay operation may be obtained despite these transients. We have used these applications under field short-circuit conditions. The tests on the Indianapolis system[1,2] show that the high-speed relays energized from potential devices gave the high-speed clearance of faults for which they were installed. There were no false or delayed operations. These results would seem to indicate that with proper co-ordination of potential device, auxiliary transformer, and relay characteristics, satisfactory operation of high-speed relays from potential devices is entirely reasonable and to be expected.

Although transient disturbances may be expected in a loaded potential device (due to the combined network of capacitance and inductance) still the combined circuit of burden and potential device constitutes essentially a series circuit of R, L, and C. In such a circuit it is well known that as R is increased relative to L and C the transient becomes of shorter duration and the phenomena changes from oscillatory to deadbeat as R passes the critical value $R^2 = 4L/C$. The R in this case represents essentially the burden resistance. Thus it can be seen that at light burdens the resistance becomes high and the transient is nonoscillatory. Even with full rated burden on the devices the magnitude of the damped wave may be reduced to the point where it will have negligible effect on the operation of high-speed relays.

In view of the satisfactory performance obtained on the Indianapolis and other systems there must be some differences in the application which are not immediately obvious. Therefore, it would seem desirable to analyze further those systems giving slow operation in order to discover the specific causes of unsatisfactory operations.

REFERENCES

1. RELAY OPERATION FROM BUSHING POTENTIAL DEVICES, P. O. Langguth and V. B. Jones. AIEE, 1932.

2. WHAT THE TESTS SHOW, P. O. Langguth and R. N. Smith. *Electric Journal*, March 1933.

J. R. North (The Commonwealth and Southern Corporation, Jackson, Mich.): This report of the relay subcommittee is very interesting and decidedly encouraging as regards the use of high-speed relaying. However, referring to the second paragraph under "Distance Relay Protection," it might be inferred that distance relays have, in general, given entirely successful and satisfactory operation except for some unnecessary tripping occasioned by out-of-step conditions. Conclusion number 2 mentions the relative complexities of distance relays and the necessity for their careful application and maintenance.

The experience of one of our companies with an installation of some 30 high-speed zone-type reactance relays may be of interest.

These relays were installed in 1931 and, from the beginning, numerous incorrect and unexplainable (at the time) operations occurred. These operations were studied and discussed with the manufacturers. Several steps were taken to improve the performance of the relays and also the performance of the 161-kv bushing potential devices which supplied the relays with potential.

The potential burden of the relay starting unit was much higher at low voltage than at rated voltage and this accentuated the ratio and phase-angle errors of the bushing potential devices. The performance of the starting unit was improved by bringing three-phase potential to the relay and removing the tuned resistance-capacitor unit, which caused the high burden at low voltage. This change decreased the relay pickup from 12 to 6 amperes and it was therefore necessary to add a resistance to compensate for this.

The bushing potential devices were given elaborate phase-angle and ratio tests in the field, and it was found that their variations in ratio and phase angle not only gave incorrect ohmic indication, but that this ohmic indication varied for maximum and minimum fault conditions. The ratio error tended to reduce the relay pickup current, thus, making the relay more apt to operate under low-current conditions.

The bushing-potential-device networks were subsequently rebuilt to give more efficient operation but, even after these changes and with the relays having constant burden, there were still a number of incorrect operations. During the following year there were 8 correct and 3 incorrect operations on phase faults and 15 incorrect operations on ground faults. In this classification, which is based on careful analyses of oscillograms, faults which involved two or more phases, with or without ground, are termed "phase faults." The term "ground faults" applies only to single line-to-ground faults.

These relays were provided for phase fault protection only and were not intended to operate on ground faults; however, the oscillograms indicated that the secondary phase currents during ground-fault conditions on this extensive isolated-neutral system might be as high as 20 amperes. The phase angle varied widely but appeared to be generally in the range of 45 to 75 degrees, leading the power-factor position.

After further detailed study and discussions with the manufacturers, it was the consensus of opinion that possible causes of the trouble included:

(*a*). Improper angle of maximum torque of starting unit.

(*b*). Excessive wipe of ohm-unit *A* contact.

(*c*). Combination of load and fault currents giving incorrect distance measurements.

(*d*). Errors in potential devices and current transformers.

(*e*). Proximity effect of adjacent relays.

It was decided to make further changes in the relays to eliminate the difficulties. The auxiliary resistance in series with the potential-coil circuit, which had been installed to bring the pickup back to its original value, was replaced with a reactance unit in order to shift the angle of maximum torque from unity power factor to 45 degrees lag. This change in the angle of maximum torque tended to prevent incorrect operation on ground faults and to improve operation on two-phase and three-phase faults.

Auxiliary wye-delta-connected current transformers were installed further to reduce the operations on residual ground current.

The adjustment of the relay mechanism was carefully checked and the contacts readjusted where necessary.

The changes outlined herein have resulted in very much improved operation of these relays according to the records to date, and it is felt that the detailed analyses of the conditions in the field, including the tests on the bushing potential devices and relays, were decidedly worth while. No doubt this work has also assisted materially, although indirectly, in the development of the improved present-day designs of distance relays.

In view of this experience, it is our opinion that proposed applications of high-speed zone-type relays should be carefully investigated, weighing the over-all gains against the complexities and possible operating difficulties. The performance of the high-speed unit and the characteristics of the potential and current supply require careful co-ordination.

E. L. Harder (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): This report, having its basis in the actual operating experiences of numerous companies, forms a most valuable guide to protection engineers.

The effect of potential-device transient on distance-relay speed would bear further analysis. For faults over a considerable portion of the operating range of an impedance element the voltage does not need to fall nearly to its new level before a large operating force is available. In view of the extremely short time constant of decay of the error voltage the time extension is scarcely measurable in this region. Near the relay balance point the relay time increases even when tested with an abrupt voltage change. Due to the increased relay time in this region, the short-duration transient becomes of small importance. It would seem rather difficult to separate time extension due to proximity to the balance point from that due to potential-device transient. It would be interesting to know whether this phenomena has been observed and measured or whether it has been anticipated on theoretical grounds.

Under the heading of bus protection the guide given for freedom from current-transformer transients, namely current not to exceed ten times transformer rating, would need to be used cautiously.

It is apparent that the assumption of average design and ten times current leaves the rather important variable factors of d-c time constant and relay burden unaccounted for. The time constant may vary over the range 0.3 to 0.003 or possibly more in different instances, longer time constants of 0.1 to 0.15 seconds being typical of large generator busses. Burden, while varying less from an average value, is likewise a very important factor, particularly the resistance component.

Methods are now available for calculating without too great complication what differential current will occur. Where long time constants or high burdens are encountered the rough guide should be used with caution and it is considered preferable to calculate the differential current.

L. F. Kennedy (General Electric Company, Schenectady, N. Y.): Mr. North has presented the case history of a distance-relay application which did not operate successfully until certain changes were made as the result of field tests. The undesirable operations were due mainly to three causes, namely: (*a*) conditions existing on an extensive high-voltage, isolated-neutral system when one conductor was grounded, (*b*) variation in ratio of potential devices, and (*c*) improper relay-contact adjustment. The operations resulting from the conditions existing with one phase grounded were correct as far as the relays themselves were concerned, but actually they were the consequence of what was later recognized as an incorrect application of the relays as originally designed. In other words, the relay had not been correctly designed for this particular application. Often complete data are lacking in cases of early application of new devices so that changes such as outlined by Mr. North become necessary as greater experience is gained. This experience indicates the necessity for full knowledge by all concerned of system, relay, and associated equipment characteristics if correct operations are to be obtained.

In regard to the general use of potential devices, these are giving satisfactory operation when properly applied. To obtain the best results proper consideration must be given to the ratio, particularly under limiting fault conditions. There are existing data showing a transient in the voltage circuit that tends to delay operation slightly but as indicated by Mr. Harder, this is not generally noticeable with present over-all clearing times.

Mr. Langguth has mentioned that this transient may be controlled to some extent. It is not generally necessary to have such a refinement today, but it would be highly desirable to have this information more generally available.

Some of the discussers have indicated that it may be desirable to slow down the speed of relay response in order to eliminate some of the possibilities of incorrect operation. Through the co-operative efforts of manufacturers and users, we are now at the point where we have overcome most tendencies of high-speed relays to operate incorrectly. It is our feeling that the work done to date has reached the point where practically the same degree of reliable operation can be obtained with high-speed relays as with lower-speed devices.

With the availability of breakers operating in less than eight cycles any concerted move to slow down relay operation seems to be a backward move and it is our recommendation that the present policy of making high-speed relays thoroughly reliable even with one-cycle operating times be continued.

An Amplifier-Wattmeter Combination for the Accurate Measurement of Watts and Vars

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Synopsis: As part of a program of improvements made to the network analyzer in the electrical-engineering research laboratory at the Massachusetts Institute of Technology, an instrument to measure watts and vars has been devised which imposes a negligible burden upon the network, is rapid in response, and has an error less than one-half per cent of full scale. The instrument consists of (1) a semistock electrodynamic wattmeter, (2) a negative-feedback vacuum-tube amplifier, and (3) a phase-shifting network. When the equipment is once assembled the presence of the amplifier may be ignored and the instrument used thereafter as any portable instrument. The principle of instruments of this kind has other important applications in the field of electrical measurements.

Purpose of the Instrument

THE development of the instrument described herein forms part of a program of improvements intended to overcome the limitations of certain measuring instruments which were originally provided for use with the MIT network analyzer[1] when it was built jointly by the department of electrical engineering at MIT and the General Electric Company during 1928–29. Certain instruments which possessed the characteristics necessary for this application, and which could be constructed at that time with reasonable cost, had various undesirable limitations, the chief one being slow response.

The nature of the characteristics required of certain instruments used with a network analyzer can best be explained by describing briefly its function. Such a device permits electrical representation in miniature of an actual power system. The MIT network analyzer operates at 60 cycles, and is equipped with sufficient elements to represent the electrical essentials of a power system comprised of as many as 16 generating stations with interconnecting lines and loads. In most studies, whether they be load-distribution, steady-state, or transient-stability measurements of volts, amperes, watts, and vars at numerous points within the network are to be performed. Since an average load study may often necessitate a thousand or more readings during a day's work, the need for measuring instruments that are rapid in response without a sacrifice in accuracy is apparent. The accuracy requirement imposes unusual limitations upon the measuring instruments, and unfortunately it is not always satisfied merely by the use of accurate measuring instruments. In addition, the burden represented by the instruments must have negligible effect upon the quantities they are to measure.

When it is necessary to perform measurements at the terminals of the equivalent synchronous machines, the commercially available portable instruments of short response time can be used, since it is not difficult to correct for whatever effects their losses introduce. For convenience, these instruments usually remain connected at the machine terminals throughout a study and are ignored while measurements are made at points within the network.

Unfortunately, when commercially available portable instruments are connected at points within the network, their burden often appreciably disturbs the electrical conditions previously established. The labor then necessary to readjust the circuits to compensate for these disturbances, or the tedious calculations involved if the data are corrected analytically for them, in part defeats the purpose of a network analyzer. To render these disturbances negligible the impedance of the current coil of an instrument connected in the network and the admittance of the potential circuit of an instrument connected at the point of measurement in the network, must be much smaller than are normally encountered in commercially-available portable instruments having the desired sensitivity, accuracy, and speed of response.

When the MIT analyzer was built the quantities generally measured were volts, amperes, and watts. The low-burden voltmeters and ammeters then provided were thermocouple instruments and the low-burden wattmeters had suspended moving elements. All these instruments were slow in response on account of their necessarily low burden. After the analyzer had been used for several years, developments in the art of power-system analysis placed more emphasis upon the knowledge of vars than had been the case originally. To measure vars, a phase-shifting transformer was used to make available at the terminals of a wattmeter a voltage in quadrature with the voltage at the point of measurement. To speed the measuring process, a commercially available low-power-factor portable wattmeter having low current-circuit impedance was used as the indicating instrument for watts and vars, and its potential-circuit burden supplied from the phase-shifting transformer. This procedure relieved the network of wattmeter potential-circuit burden and permitted the use of a rapid-response instrument. However, it still necessitated: first, to measure watts, the manual adjustment of the secondary voltage of the phase-shifting transformer to equal in magnitude and angle the voltage at the measuring point; and second, to measure vars, the rotation of this voltage into quadrature with the voltage at the measuring point. Thus the time required to make these measurements was considerable.

The instruments originally used to measure voltage within the network have been replaced by a single a-c rectifier-type voltmeter of 1,000 ohms per volt. The burden of this voltmeter is negligible, it reaches full deflection in a fraction of a second, and is provided with an adjustable shunt across its moving coil to permit rapid standardization by comparison with a standard portable electrodynamic voltmeter. The instrument described hereinafter has been developed to replace the apparatus originally used to measure watts and vars, with the object of reduc-

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Paper number 39-8, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted June 30, 1938; made available for preprinting November 29, 1938.

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The authors acknowledge the assistance given them with this problem by many of their colleagues at the Massachusetts Institute of Technology. They also wish to thank R. N. Slinger of the General Electric Company for his helpful comments made during the preliminary discussions on this problem, P. J. Jacobs, Jr., now of the Allis-Chalmers Manufacturing Company, who carried out the early experimental work, and A. H. Wolferz of the Weston Electrical Instrument Corporation for his co-operation in obtaining a suitable wattmeter.

[1] For all numbered references, see list at end of paper.

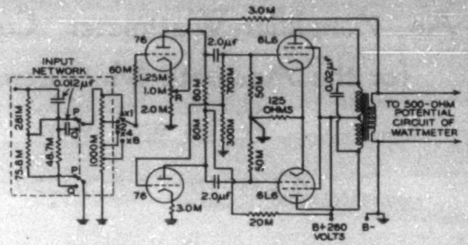

Figure 1. Circuit diagram of amplifier

ing the time required to make these measurements. In a manner discussed toward the end of the paper, this instrument is also used for the rapid measurement of current in polar or complex form.

Form of the Instrument

Since it was expected that only one instrument of this type would be needed for the particular application, it appeared desirable to use stock apparatus wherever possible in order to avoid excessive costs. In accordance with this idea it seemed logical to consider continuing the use of an electrodynamic wattmeter for the basic indicating member of the instrument. Then, to reduce the time to take measurements it was proposed to operate the wattmeter in conjunction with a vacuum-tube device that would supply all, or part of, the wattmeter energy for operation, and, at the same time, eliminate the phase-shifting transformer. Hence, a study was made to determine whether vacuum-tube units would be required for both potential and current circuits, and if both were not required, which would be preferable.

If a single vacuum-tube amplifier is used to energize the wattmeter current coil, its input circuit could consist of a low-resistance shunt connected in series with the network. The burden imposed by the wattmeter potential circuit must then be made negligible by decreasing its admittance and hence decreasing the ampere turns provided by the usual moving-coil system if assembled from stock wattmeter parts. Since the rapidity of motion of the moving-coil system in an electrodynamic instrument depends on the product of the ampere turns of the moving coil times the ampere turns of the stationary coil, a moving system of low burden and short response time necessitates more than normal ampere turns for the fixed coil, in this case the current coil. It was found that to provide sufficient ampere turns by suitable stock coils would overload the current coil to a degree that would make its temperature rise excessive. On the other hand, if a single vacuum-tube amplifier is used to energize the wattmeter potential coil instead of the current coil, its design is less difficult. The admittance of its input circuit can be made small enough to impose a negligible burden on the network, and the impedance of the wattmeter current coil can be made small enough to comply with the limitations imposed by the analyzer application, and both restrictions thus satisfied with one amplifier. A 90-degree phase-shift circuit can be inserted in the amplifier input network to make possible the measurement of vars. However, when the impedance of the current coil of a wattmeter of conventional design is reduced, the number of turns in the coil is usually reduced. Thus, the potential coil must supply more than normal ampere turns to give the ampere-turn product required by a rapid and rugged moving-coil system. Fortunately, the potential circuit in most designs of stock wattmeter operates with a larger margin of safety in regard to heating than does the current circuit, and it is possible to establish an ampere-turn product in the potential coil sufficient to provide a satisfactory instrument without serious heating.

For these reasons, the instrument consists of a single amplifier which provides the potential-coil current of a semistock electrodynamic wattmeter whose current-coil impedance is small enough to have negligible effect upon the electrical conditions of the network.

Design of the Wattmeter

Because there appears less freedom in the design of a wattmeter from stock parts than in the design of an amplifier, the wattmeter was selected first and the necessary amplifier then designed. The specifications established for the wattmeter limit its current-coil impedance and specify its sensitivity. The ampere-turn product then required, and hence the ampere-turns to be provided by the potential coil, necessitates a compromise between the allowable time for the pointer to come to rest and the degree of ruggedness desired in the instrument.

The criterion which limits the magnitude of the current-coil impedance is the magnitude of the smallest impedance element used in the network analyzer. More specifically, if inserting a current coil into a network is not to disturb appreciably the electrical conditions of the network, the current-coil impedance Z_i must be negligible compared with the impedance Z_L measured looking into the network from the point where it is opened up to insert the coil. The criterion established was that the disturbance would be considered negligible provided the value of the impedance Z_i was less than one per cent of the impedance Z_L. The impedance values encountered in a study are a function of the base quantities and with the analyzer connected for the customary 200-volt two-ampere base, unit impedance* is 100 ohms. Operating experience has indicated that impedance elements less than 0.10 per unit on the system base are seldom encountered, although in an extreme case two circuits each represented by about a 0.10 per-unit impedance might be connected in parallel. For this condition it can be shown that the minimum value of the impedance Z_L ranges around 10 ohms; therefore the impedance of a current coil must be less than 0.10 ohm.

The current coil of a 5- or $7\frac{1}{2}$-ampere wattmeter for low power factors has an impedance close to this value and was therefore selected. When used with the analyzer the working coil current is determined by the magnitude of the quantities to be measured rather than by temperature considerations. Since unit power is 400 watts or a convenient multiple thereof, and, since it is desired to indicate with reasonable accuracy about a 0.01 per-unit load or 4 watts, an instrument having a sensitivity of about 75 watts full scale, 100 volts, unity power factor was considered reasonable.

A moving-coil system was next desired that would have a restoring torque sufficient to provide a reasonably fast and rugged instrument, and yet not so large as to require an abnormal number of potential-coil ampere turns to provide the necessary ampere-turn product with about 0.75 ampere in the current coil. The moving system of a Weston model 310 portable electrodynamic wattmeter for low power factors was made suitable for this purpose by (1) use of restoring springs that have approximately 70 per cent of normal torque, (2) operation with approximately twice the customary potential-coil current, and (3) slight modification of the air-damping system. The wattmeter as assembled gives full-scale deflection at unity power factor with 0.60 ampere in the current coils, and is calibrated zero to 60 watts. A series-parallel current-coil connection and potential multipliers provided as described hereinafter, permit appreciable increase in range. For convenience in calibration a 100/200-volt potential circuit is provided. Table I summarizes the principal design constants of the wattmeter. Although the above modifications to a stock portable wattmeter may make it somewhat less rugged, they are not considered serious because it is removed from the central metering desk only when taken to a standardizing laboratory.

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* Power-system computations are usually made in terms of per-unit quantities, where unit or base kilovolt-amperes is an arbitrarily chosen magnitude, unit voltage is normal voltage, unit current is equal to $\frac{\text{base volt-amperes}}{\sqrt{3}\ \text{volts line-to-line}}$, and unit impedance is equal to $\frac{(\text{volts line-to-line})^2}{\text{volt-amperes}}$. To study a system on the MIT analyzer, a single-phase representation is used and convenient unit quantities are 400 volt-amperes, 200 volts, two amperes, and 100 ohms. Thus, on this analyzer 400 volt-amperes represents unit system kilovolt-amperes, 200 volts represents unit system voltage, and two amperes represents unit system current. Other analyzers use different base quantities.[6]

Design of the Amplifier

If an amplifier is to be suitable for use as a component of a measuring instrument for a network analyzer, it should satisfy the following specifications:

(*a*). A constant ratio of input voltage to output current throughout its working range.

(*b*). A high degree of stability during long periods of time.

(*c*). A high degree of freedom in regard to the interchangeability of tubes of the same type.

(*d*). A negligible phase angle between input voltage and output current.

A type of amplifier whose performance approaches the above specifications is one whose circuit employs the principles of negative feedback,[2–5] and is accordingly used in this instance. A circuit diagram is given in figure 1. The form in which the negative feedback is employed in figure 1 results from the operating conditions encountered in this application. To avoid supplying an excessive amount of energy from the amplifier, it is desirable to energize the wattmeter potential circuit at as low a voltage as practicable. However, it is not satisfactory to energize only the potential coil, unless the change in its resistance due to temperature rise has negligible effect on the desired ratio of coil current to amplifier input voltage. Since temperature rise may often be appreciable (see table I) the amplifier could be designed using inverse feedback in a manner that tends to maintain the ratio desired to a high degree in spite of the effects of temperature rise in the potential coil or other elements which form part of the output circuit. To do this a resistor having negligible temperature coefficient would be connected in series with the potential coil, and a percentage of the voltage drop across this resistor fed back into the input circuit of the amplifier, so as to oppose the applied input voltage.

To determine the quantity of feedback voltage required, it is necessary to investigate the dependence of the amplifier performance on the expected variation in output-circuit resistance resulting from temperature rise. The elements which probably contribute most to this effect are the potential coil and the output-transformer windings. Using the customary methods of analysis[2,3], the voltage drop E_2 developed across the feedback resistor in the output circuit, is related to the voltage E_1 applied to the amplifier by the expression

$$E_2 = \frac{\mu E_1 - I_e Z}{1 + \mu\beta} = I_e R_0 \qquad (1)$$

where

μ is the ratio of the voltage appearing at the primary of an equivalent 1:1 output transformer, to the grid-cathode voltage of the first tube

β is the fraction of the voltage E_2 fed back into the input circuit

I_e is the potential-coil current

Z is the sum of the impedance Z_T of the output transformer and Z_e of the potential coil

R_0 is the feedback resistor of negligible temperature coefficient connected in the output circuit

By rearranging equation 1 the transconductance g_m, which is equal to I_e/E_1 becomes

$$g_m = \frac{\mu}{R_0(1 + \mu\beta) + Z} \qquad (2)$$

From which

$$\frac{dg_m}{dZ} = \frac{-\mu}{[R_0(1 + \mu\beta) + Z]^2} \qquad (3)$$

From equation 3, the absolute fractional change $\Delta g_m/g_m$ in g_m caused by a change ΔZ in Z, when all other parameters are constant, is,

$$\frac{\Delta g_m}{g_m} = \frac{\Delta Z}{R_0(1 + \mu\beta) + Z} \qquad (4)^*$$

Table I

Potential circuit	
Potential-coil current (ampere)	0.08
Ampere turns	24
Resistance of moving coil (ohms)	45
Temperature rise of moving coil (deg C)	12.7
Inductance of moving coil (henry)	0.0041
Torque per spring at 100 degrees (milligram-centimeters)	70
Number of springs	2
Current circuit	
Working current series connection (ampere)	0.6
Maximum current series connection (amperes)	5
Number of current coils	2
Turns per coil	34
Resistance per coil (ohm)	0.034
Ampere turns (with usual working current)	40.8
Movable system	
Undamped period (seconds)	1.02
Damping factor	18.3
Time to get reading at two-thirds scale (seconds; approximate)	2.5
Torque/weight$^{1.5}$ (milligram-centimeter for 100 deg/grams$^{1.5}$)	33.4

Equation 4 permits calculation of the magnitude of the resistor R_0 necessary to maintain any desired amount of constraint on g_m for the predicted variation ΔZ resulting from temperature rise, when appropriate values are assigned to μ and β.

In any actual circuit the parameters appear as complex quantities, but for this particular amplifier a real value is required for g_m because the potential-coil current must be in phase with the voltage applied to the amplifier. Measurements performed on an amplifier assembled so as to make $\Delta g_m/g_m$ essentially independent of anticipated changes in Z due to temperature rise, showed that the phase angle in g_m was not negligible. A phase-shift network could have been inserted in the feedback circuit to make the phase angle negligible, but it was not inserted because the phase angle becomes negligible if the drop across the circuit consisting of the resistor R_0 and the potential coil Z_e in series is used as the feedback voltage E_2. This connection depresses the effect of temperature rise in the coil only to the extent that it is masked out by the magnitude of R_0 then required, but it allows β to be a real quantity and simplifies making the small adjustments to the amplifier gain required when a standby wattmeter must be used. This latter feedback connection is the one adopted, and the temperature rise of the coil is made negligible by using an automatic-release on-off reversing switch to substitute the coil and the resistor R_0 for a dummy load of equal resistance only during the short interval required to read the instrument. This switching arrange-

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* Similar expressions for variations with respect to μ can be derived.[5]

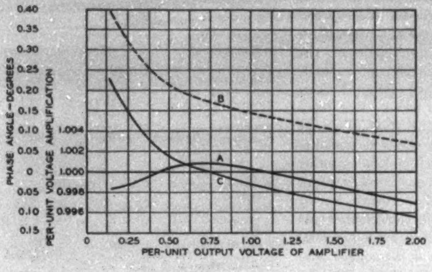

Figure 2. Characteristics of amplifier

Curve *A*—Per-unit voltage amplification

Curve *B*—Phase angle between input and output voltages

Curve *C*—Phase angle between input voltage and current in wattmeter potential coil

ment is also necessary to protect the coil against accidental burnout in the hands of operators of the network analyzer who might unknowingly overload it considerably for long periods.

The magnitude of the resistor R_0 necessary to mask the errors due to temperature rise in the coil to 0.5 per cent if operated continuously at normal working current, is about 450 ohms. This magnitude is used for the feedback resistor since it is not excessively large compared with that required to render negligible the effects of temperature rise in the remainder of the output circuit, and does not impose excessive burden on the amplifier. The wattmeter potential-circuit multiplier is used for the resistor R_0, by being tapped at a point which makes the coil resistance plus R_0 equal to 500 ohms. The amplifier output terminals are connected to this circuit, which represent a normal burden of approximately three watts at 40 volts.

The voltage amplification without the feedback and for the impedances shown in figure 1 is approximately 40. The negative feedback was increased deliberately to within a safe margin of the limit of stable operation of the system. This condition results in a net voltage amplification of about 2, and constrains the amplifier so that variations in its performance caused by changes occurring in tube characteristics are reduced to about five per cent of what they would be were no inverse feedback employed. For this over-all amplification, the voltage applied to the input of the amplifier is approximately 20 with normal current in the potential coil of the wattmeter. The cathode-bias resistors have no shunting capacitors because, if used, they introduce undesirable phase shift. The primary of the output transformer is tuned to reduce phase-shift caused by its exciting current.

The essential characteristics of the amplifier are indicated by the curves of figure 2. As seen from these curves the voltage amplification is essentially constant, and the phase angle between input voltage and potential-coil current is negligible throughout the operating range. Tubes of the same type having normal characteristics may be interchanged with a negligible change in performance. The instrument is ready to use within about one minute after applying voltage to its plate and filament circuits. The change in performance for a plus or minus four per cent change in voltage supplying the plate and filament circuits is negligible.

Input Networks

It is desirable to have the same wattmeter calibration and multiplying factors with the potential circuit supplied by the amplifier as with it supplied directly from the network. To give the desired multiplying factors for both watts and vars, two voltage-divider input networks are provided ahead of the amplifier, and each is adjusted so that approximately 20 volts is applied to the input of the amplifier when 100 volts is applied to the input terminals of either network. When the grid of the amplifier tube is connected to one input network, the current in the potential coil of the wattmeter is in phase with the voltage at the measuring point and allows the measurement of watts. When the grid of the amplifier tube is connected to the other input network, the current in the potential coil of the wattmeter leads the voltage at the measuring point by 90 degrees and allows the measurement of vars.

These input networks must impose a negligible burden upon the power-system network under study. To satisfy this condition, the criterion established was that their burden would be considered negligible if their magnitude was less than the magnitude of the uncertainty of the wattmeter reading. Thus, with a meter giving full-scale deflection for 60 watts and accurate to within one-fourth per cent of full scale, the burden imposed by the input networks of the amplifier should be less than 0.15 watt at 100 volts. The input network used to measure watts consists of wire-wound resistors, and imposes a burden of approximately 0.03 watt at 100 volts. The input network used to measure vars consists of resistances and capacitances and imposes a burden of 0.045 volt-amperes at 100 volts. The characteristics of this network are frequency sensitive; but, for the values of the circuit parameters, a change of one per cent in frequency changes the magnitude of the voltage applied to the amplifier by about one per cent and the phase-angle by about 0.2 degree. Since the frequency of the source varies less than ±0.2 per cent, the effect of frequency variations upon the characteristics of the phase-shift network is negligible.

The instrument is made to indicate either watts or vars by connecting the amplifier to the proper input network through a conveniently mounted switch. To extend the range of the instrument the grid of the amplifier tube is connected by another switch to different taps on the voltage-divider circuit as shown in figure 1. These provisions give the instrument a range from zero to 960 watts or vars.

In certain studies it has been found convenient to use one scale multiplier throughout a study, and to make the meter indication correspond directly to a convenient multiple of system base kilovolt-amperes by appropriate selection of analyzer base quantities. For example, if effective use of the analyzer equipment results when unit system kilovolt-amperes is 100,000, and unit analyzer volt-amperes is 400, the instrument reads system megavolt-amperes directly when the scale multiplier is four. Other convenient scale multipliers may be used.*

Calibration of the Instrument

Calibration of the amplifier-wattmeter combination instrument is accomplished readily. The wattmeter is first calibrated in the standardizing laboratory in the usual manner without regard to its ultimate use as part of the combination instrument. Then, when it is used as part of this instrument and its potential-coil current is supplied by the amplifier, the accuracy of the combination instrument is determined by comparing its indication

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***H. P. St. Clair of the American Gas and Electric Service Corporation was the first to emphasize the value of this feature and as a result of his suggestions, the wattmeter dial has been graduated for zero to 24 as well as zero to 60 full scale to extend the applicability of this feature.**

of a definite measurement with the indication of the same measurement given directly by the wattmeter when the appropriate tap on the potential circuit of the wattmeter is connected directly to the point of measurement. The voltage at the point of measurement must have the same value when each indication is read. The amplifier is made to have the desired amplification by adjustment of the tap connection on the resistance *R* of figure 1, which varies the feedback coefficient β. Because of the ease with which a check on the accuracy of the instrument can be performed it is done daily; but no readjustment of the feedback circuit has been found necessary during a ten-month period of use.

Application to the Measurement of Current

The instrument is useful to measure current rapidly in complex or polar form. To measure the in-phase and quadrature components of current the potential circuit of the amplifier is connected to an alternating-voltage source, preferably of 100 volts, whose phase angle may be regarded as the reference angle of the network under study. The current coil of the wattmeter is connected into a branch of the network. The instrument then indicates 100 times the in-phase and quadrature components of the current in this branch when the switch on the amplifier is connected to the "watts" and "vars" positions respectively. If the voltage is supplied by a phase-shifting transformer the angle of this voltage can readily be made any desired value.

To measure current in polar form, the switch on the amplifier is first connected to indicate vars. The phase-shifting transformer is then connected to the input circuit of the amplifier and the phase angle of its voltage adjusted until the wattmeter reads zero. The short time required for the pointer of the instrument to come to rest makes the operation a fairly rapid one. The voltage of the phase-shifting transformer is now in phase with the current in the current coil of the wattmeter. With the phase-shifter terminal voltage maintained at 100 volts, the instrument indicates 100 times the current when the switch on the amplifier is transferred to the "watts" position. The phase angle indicated by the phase-shifting transformer is the phase angle of this current with respect to the reference voltage of the phase-shifting transformer. The magnitude of the voltage is chosen as 100 while using this instrument to measure currents only to simplify the division of watts by volts to obtain amperes.

Conclusions

By employing a negative-feedback amplifier it has been possible to obtain an instrument using essentially stock parts which has the speed of indication of commercially-available portable instruments, and, at the same time, sufficiently low current-coil impedance and potential-circuit admittance that its burden is negligible compared with the quantities to be measured on the network analyzer. The instrument is accurate to better than one-half per cent of full scale for any range when indicating watts or vars. It is not unlikely that a device of this type has other important applications in the field of electrical measurements.

This instrument with associated equipment so expedites the metering procedure on the MIT network analyzer that the speed at which data can be reliably recorded controls primarily the time required for such a study. In addition, this equipment makes it practicable to include numerous refinements in the procedure employed when performing certain studies.

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Discussion

R. N. Slinger (General Electric Company, Schenectady, N. Y.): The authors of this paper are to be congratulated for their excellent work in making use of the negative-feedback amplifier circuit with a 60-cycle semistock portable wattmeter. While their primary objective was evidently to improve the speed and accuracy of their network analyzer instrument system they have perhaps also pointed the way to further fields of usefulness for precision type amplifiers of this type.

As they have indicated, the requirements for a wattmeter-amplifier combination suitable for application to a network analyzer are quite severe. Accuracy, speed of response, and extremely low instrument burden are all exceedingly important. The instrument combination also must be capable of retaining its calibration within close limits over relatively long periods of time and the indicating instrument must be of such form that it can be read easily and rapidly many times a day without undue eyestrain. In fact, almost the only common instrument requirement not included is the frequency-response characteristic, which is not of importance in this instance only because the analyzer reactor and capacitor circuit elements make a constant-frequency power source one of the essential components of a network analyzer.

Another important requirement not specifically mentioned in the paper is the ability to withstand frequent and severe momentary overloads. The range of currents that must be measured in the course of an average system study may easily be as great as 500 to 1. With magnitudes of the current and to a lesser extent the voltages, differing so widely, even the best of operators will occasionally inadvertently connect the instrument into circuits carrying several times the current or voltage they had expected, especially when the instrument is of a type specifically designed for high-speed operation.

It is not clear how the instrument described in this paper will be protected from serious overloads, particularly on low-power-factor readings, or how it will be arranged to cover a sufficiently wide range of currents. By proper design of the output characteristics of amplifiers for either current or potential circuits, they can be made to protect the instrument by limiting the maximum output to values that will not result in serious damage. It would be of interest to know how the authors have solved these problems.

They have indicated the convenient method in which the instrument lends itself to the measurement of components of current. It would seem equally important to be able to measure components of voltage as well. To do so, would necessitate connecting the current coil of the wattmeter to an adjustable power source to provide a reference current. The potential coil of the wattmeter could then be connected to the network at any desired point for the measurement of voltage components.

In the design of the input network, a slightly different arrangement of the constants might result in a better instrument working range. Most of the voltages in an analyzer representation of a power network will ordinarily fall within the range of 90 to 110 per cent of base voltage. Therefore, it might be better to design the input network to give a full-scale wattmeter deflection with 0.6 ampere in the current coil and 150 per cent of base voltage on the terminals of the input network. For load study readings this would ordinarily permit the use of only one potential-coil multiplier for all readings without danger of overloading the potential-coil circuit. The other multipliers for use in other types of studies could then be convenient multiples or fractions of this multiplier.

The authors' statement in regard to the interchangeability of tubes is quite in line with experience obtained on the General Electric Company's network analyzer in Schenectady. This analyzer, using a similar circuit for the instrument amplifiers, as described in a paper presented at the winter convention a year ago, and using a light-beam type wattmeter constructed from conventional parts has now been in almost constant operation for a year and a half. The instruments have been periodically checked for calibration and some of the tubes in the amplifiers have been replaced as they showed signs of diminishing emission. However, no difficulty whatever has been experienced in operating the amplifier with some new tubes and some old ones and very little adjustment of any kind has been required since the instrument system was put into operation.

Eric A. Walker (Tufts College, Medford, Mass.): In the measurement of small amounts of power there is one source of error introduced by the wattmeter itself, which can be neglected only if certain precautions are taken to make this error as small as is possible. This error is one resulting from what might be called the transformer effect of the coils. The flux set up by the current coil of the meter generates a voltage in the voltage coil in addition to that impressed by the external circuit. This additional voltage causes an additional current in the voltage coil.

The same action takes place in the opposite direction: the current in the voltage coil causes a flux which gives rise to a current in the current coil. Both of these effects can be checked by energizing either the voltage or current coil in the proper manner and short-circuiting the other coil.

These two effects cause errors which are not constant but which are effected by a number of factors. These are: (1) the voltage impressed on the voltage coil; (2) the current flowing in the current coil; (3) the amount of coupling between the windings which changes with the position of the coils; (4) the impedance of the circuit connected to the voltage coil; (5) the impedance connected to the current coil.

As was said before, the error is not important in wattmeters designed for large amounts of power especially when the power factor is near unity. However, for wattmeters having a full-scale deflection of about five watts at power factors of 0.01 and below, the errors so introduced may be larger than the actual reading.

The difficulty may be overcome by arranging the circuits so that a high impedance is connected in series with both windings and the currents then caused by the transformer action are small.

W. O. Osbon (nonmember) and **W. W. Parker** (both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors are to be congratulated for the thorough manner in which they have described the application and design of the amplifier-wattmeter combination for use with the MIT network analyzer. The paper contains data which will be interesting and useful to those concerned with both network calculators and negative-feedback amplifiers and their application.

The special interest in this paper results from the recent development of a negative-feedback amplifier but along somewhat different lines. This amplifier unit and associated instruments are shown in figure 1 of this discussion. Current feedback instead of voltage feedback is used which avoids difficulties and circuit complications due to heating of the instrument coils.

Without sacrificing quality of performance the amplifier assembly has been made very compact, being only 9 by 10 by 12 inches. It includes a power pack and two separate two-tube amplifiers, one for the voltmeter and the wattmeter voltage coil and the other for the ammeter and wattmeter current coil.

Due to the use of both current and voltage amplification it has been possible to use standard instruments as far as torque, speed of response, and damping is concerned.

The necessity for a large number of instrument readings in calculator studies makes it desirable to have direct readings to cover the range of values required for any one problem. This is done by using instruments which have two overlapping uniform scales. Direct readings are obtained of current, volts, watts, and vars without the necessity of multipliers for problems using the normal calculator voltage level. When the lower voltage level is used (primarily for short-circuit studies) the infrequent wattmeter readings are multiplied by 0.2, all other readings being direct. Phase angle and component quantities of currents and voltages are obtained on the "universal vectometer" which is used in conjunction with the amplified instruments. The "universal vectometer" has been previously described in the *Electric Journal* for May 1933.

G. S. Brown and **E. F. Cahoon**: The authors appreciate the many interesting points raised in the discussions by Messrs. Slinger, Osbon and Parker, and Walker.

Mr. Slinger's emphasis of the severe requirements imposed upon an instrument used in a network analyzer, and his mention of the experience obtained on the General Electric Company's analyzer in regard to the freedom from tube troubles, are much appreciated. In answer to Mr. Slinger's question concerning the protection of the instrument against serious overloads, we would like to mention again that the current coils of the wattmeter are essentially those used in a stock meter rated for five amperes. As stated in the paper, this relatively large rating results from the low-current-coil-impedance criterion imposed by the analyzer application. Since the parallel connection of the coils permits working up to ten amperes, and since no generator on the MIT analyzer is rated more than six amperes, the danger of burnout of the current coils is not great.

As for the potential circuit, a small overload-alarm device is connected in parallel with the dummy amplifier load, this device and the dummy load being connected to the amplifier except when a reading is being made. The alarm device was not described in the paper in order to save space. It consists briefly of a relay energized through a diode rectifier from the output of the amplifier. The relay operates a warning light whenever the output voltage is (1) large enough to endanger the wattmeter potential coil, or (2) more than the amplifier can supply without departure from the linearity or phase-angle requirements. The warning light is located adjacent to the key that is operated to substitute the potential-coil circuit for the dummy load, and hence is readily noticeable.

Again referring to Mr. Slinger's discussion, the potential coil taps on the input network are used principally to change the range of the instrument, and seldom to match with some particular voltage base. If our design had permitted a simple means to change the current sensitivity the suggestion of an input network adjusted for about 150 per cent normal voltage as maximum voltage, might have become a practical matter.

The point raised by Professor Walker is an extremely important one. It is rarely mentioned in discussions of instruments to measure watts or vars, perhaps because the total potential circuit and total current circuit impedances are relatively large in the conventional instrument applications. To demonstrate the importance of this point, the deflection of the pointer in the wattmeter of the instrument described is about one-tenth full scale when the *potential coil* is short-circuited and rated current is applied to the current coil. However, with the 450-ohm feedback resistor connected in series with the coil and the series combination short-circuited no deflection can be detected. Similarly, the pointer deflection is again about one-tenth full scale with normal potential-coil current applied and the *current coil* short-circuited, but with 0.5-ohm impedance in series with the current coil and the combination short-circuited, no deflection can be detected. Since the equivalent current-circuit impedance is usually in excess of 10 ohms, as discussed in the paper, the instrument is believed to be free from the transformer effect of the coils. However, the authors are glad that this point was raised, because they are aware of other attempts to build an instrument of the type described that were unsatisfactory because of the relatively large currents induced by the transformer effect.

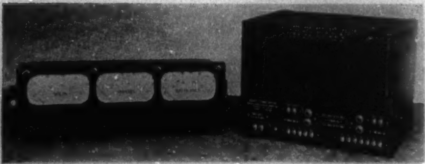

Figure 1

Notes on Emergency Ratings

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MEMBER AIEE

COSTS of money, depreciation, and taxes now represent almost two-thirds of all electricity-supply costs. Other costs have been reduced to the point where it is difficult to effect further large reductions in them. It appears, therefore, that the major opportunities for improving the competitive position of electricity must now lie in the direction of lowering the fixed costs by reducing to a practical minimum the investment required to deliver adequate voltage and the nearest practicable approach to continuous power supply.

Increased load ratings may represent one logical step in the solution of this problem. Such a step requires primarily the reconsideration of emergency load conditions, since these alone have almost traditionally determined when additions to load-carrying plant should be made. There are so many phases and so many different types of apparatus to be considered in any general program for increased load ratings that it would be quite impossible, if it were the intent of this paper, to go beyond a simple discussion of a few pertinent relationships and to outline some thoughts that may be helpful to others also working on this problem.

Almost more frequently than for any other single element in the distribution system, it becomes necessary to determine the extent to which cable of some sort determines the load rating which may be applied. In their relation to the system plan, most of the load-rating problems are so nearly parallel to those for cables that a careful discussion from the point of view of the cable plant not only permits drawing upon a wealth of convenient illustrations but at the same time should be helpful to the engineer who faces a similar problem for other apparatus. Let us consider for a moment the place of emergency ratings in the system plan.

Place of Emergency Ratings in the System Plan

Economy in fixed charges can be attained in each of two directions. The first is to provide economically the capacity needed to carry the transient load occasioned from time to time in the normal course of events when a sub-station supply cable, transformer, or some other link in the supply system, fails in service. This sort of emergency is almost a routine affair, to the extent that it is usual practice to select a design which will meet such a situation with the minimum practical need to disturb the load being supplied under the conditions. The gradual extension of the network principle to points nearer the customer's service take-off simply extends the limits within which such failures of equipment may occur without causing a customer's service interruption. The value of the emergency rating in this case lies in permitting each link in the supply system to carry as high an average day load as is possible, without in any way reducing the ability of the system to meet such transient emergency loads. The duration of such emergencies may range from, say, 12 hours to locate and repair a cable fault, or 24 hours to replace a substation transformer bank, up to perhaps three months to repair a large transformer and return it to service if a spare unit is not available.

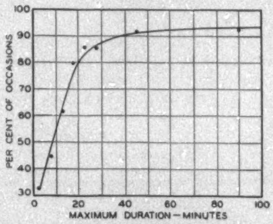

Figure 1. Per cent of occasions when alternate source is needed, versus their maximum duration

The second type of emergency includes the group of situations which accompany a major system disturbance. Under such conditions, the system operators may need to use every available supply link to the practical limit of its capacity, until it is possible to isolate the fault and return the load to its normal source of supply. Figure 1 summarizes for one system about 100 observations of the time during which it has been necessary to supply distribution substations from an alternative source, over emergency tie lines that are provided for such use while the normal source of supply is being restored to regular operation after a major disturbance. It will be seen from this that about 85 per cent of such cases require use of the alternative source for less than one-half hour. Hence, there may be occasional need for very high short-time emergency ratings. These may have little value for increasing the average daily loading of the normal supply elements of the system but will serve to reduce the extent of customer inconvenience to a minimum during a major system disturbance. It may even be helpful to have a one-hour and a two-hour as well as a one-half hour rating, so as to provide the utmost flexibility for redistribution of load, before it should be necessary to reduce loads to the lower permissible values for all-day emergency operation. One plan that has some attractive possibilities in operation of a supply cable system would permit following any one of the following emergency load schedules, in per cent of normal load:

(*a*). Two hundred per cent for one-half hour, followed by 130 per cent for the rest of the load cycle;

(*b*). One hundred sixty-five per cent for one hour, followed by 130 per cent for the rest of the load cycle;

(*c*). One hundred fifty per cent for two hours, followed by 130 per cent for the rest of the load cycle; or,

(*d*). One hundred thirty per cent for one complete load cycle.

Thus, the schedule to be followed in each given instance would be determined automatically by the magnitude of the initial load. Radial distribution feeders ordinarily may not require sufficient use of one-half hour or one-hour ratings to justify provision for them. However, the 150 per cent–130 per cent load schedule has the merit of providing time for load transfers in the field when this becomes necessary for the best redistribution of the emergency loads between the remaining circuits in an area affected by a service failure of one. This does not by any means exhaust the possibilities that will face the engineer in each of the wide variety of possible occasions for the use of emergency ratings. It does serve to

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Paper number 39-68, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 1, 1938; made available for preprinting December 29, 1939.

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The author is indebted to many writers on temperature, particularly to V. Karapetoff,[1] V. M. Montsinger,[2] Wallace B. Kirke,[3] Elwood A. Church,[4,5] and William H. McAdams,[6] whose works were most helpful. He is especially grateful to his associates for their contribution to such of his observations as may prove to have value.

1. For all numbered references, see list at end of paper.

illustrate the occasional needs for meeting both short and comparatively long-time emergency load situations.

Perhaps the most important principle to be kept in mind in the consideration of emergency ratings is that the maximum emergency loads to be carried should not affect the ability of the system to operate satisfactorily under such extreme conditions, or be sufficiently severe, with due regard to their probable magnitudes and frequency of occurrence, to cause an unreasonable reduction in the average life expectancy of the facilities required to carry such loads.

Economy alone may often urge even more severe treatment of the cables and other load-carrying equipment than would be acceptable if its attainment should require measurable sacrifice in safety, dependability, or flexibility. The underground substation supply cable in a metropolitan system, for instance, may represent no more than 30 per cent of the total installed cost of line switching sections, useful duct space, manholes, and cables. It may be good business, therefore, to take advantage of an appreciable increase in average load per cable at the expense of a possible corresponding increase in cable maintenance costs. In many respects the situation is quite the same for substation equipment, etc. Decision as to the practical limit of this economy naturally requires consideration of all the factors in the light of experience and seasoned judgment. In appropriate circumstances, however, an above-average failure rate may represent more careful engineering than would a low failure rate.

The Magnitude of Emergency Loads

One of the facts that appears to be most confusing to those charged with direct responsibility for cable or apparatus ratings in electricity distribution is the usually very low average ratio of peak load to installed capacity. Surveys often show, for instance, that the average load per cable at the time of maximum station output is less than 70 per cent of its all-day emergency rating. Two factors, service continuity and load growth, almost entirely explain this difference between installed capacity and the average yearly peak load per cable. Nearly all additions to distribution plant investment are made to provide for the supply of load growth and to maintain, or improve, service continuity. Perhaps it would be helpful to illustrate the effect of these two factors by discussing their effect upon substation supply cable loading.

Let us assume that the load on a substation in one year is just equal to the normal load capacity of two supply cables. Also assume, tentatively, that the emergency and normal load ratings are identical for these cables, so that a third cable has been provided in order to have two cables available, should one fail. In the following year, however, a small increment of load growth requires the installation of another cable and, in that year, four cables are needed to supply but little more than a two-cable load. Load growth will then gradually absorb the spare capacity, until a time is reached when the load on the substation will very nearly equal the capacity of three cables. During the interval between the installation of the fourth cable and the time when its capacity is required for adequate reserve alone, then, there has been an average excess of installed cable capacity above load that is about equal to the capacity of $1^1/_2$ cables. Stated in more general terms, this amounts to saying that any substation which has a growing load and is supplied by n cables will have an average of 1.5 cables excess above load hence the average ratio U of total installed emergency load capacity to the yearly maximum demand on the substation would be:

$$U = \frac{n}{n - 1.5} \tag{1}$$

This ratio holds for any group of three or more identical lines, transformers, circuits, etc., so long as the number does not become large enough to justify consideration of coincident emergency loss of more than one unit in the group. It is always larger than would be expected by those who are much more accustomed to the lower instantaneous ratio u of emergency to normal loads:

$$u = \frac{n}{n - 1} \tag{2}$$

Few substations are supplied by more than seven lines or have more than four transformer banks and some have only two. While it is true that the number of primary distribution circuits is large, it is seldom that the load of one circuit can be transferred to more than three adjacent circuits if it should fail ($n = 4$) and, similarly, it is often impractical to transfer the load of one distribution transformer to more than two adjacent transformers ($n = 3$). Hence, in each part of the distribution system, we find we are dealing with groups of units in which the values of (n) range from 2 to about 7. By substituting such values in equation 1, we may find directly that the resulting ratio of emergency load rating to the average yearly maximum demand per unit (table I) ranges from 4 to 1.27 for each such group. The corresponding values of the instantaneous ratio u are also shown.

Thus, there may be many substation supply cables in a given underground duct bank but, if the average number of supply cables per substation is four, the sum of the normal noncoincident maximum demands on all the supply cables in that duct bank probably will not reach a total greater than 62.5 per cent (100 per cent/1.6) of the combined emergency load ratings of those same cables. The same general relationship operates inexorably for each other class of load-carrying equipment in a growing distribution system and, to some less extent, in the bulk-power system.

Table I

Number of Units Normally in Service	Average Ratio (U) Emergency Load Rating to Average Yearly Maximum Demand per Unit	Instantaneous Ratio (u) of Emergency to Normal Load
2	2 to 4.0*	2.00
3	2.0	1.50
4	1.60	1.33
5	1.43	1.25
6	1.33	1.20
7	1.27	1.17

* Note: Formula not entirely satisfactory for this case.

It is possible that economy in load-carrying facilities may be improved to some extent, however, by judicious application of emergency load ratings that are somewhat higher than the load ratings which would represent sound engineering practice for regular daily operation. Equation 1 is also helpful in illustrating the maximum ratio of emergency to normal load ratings which would be required in order to bring the average yearly maximum demand up to a value equal to the installed normal load rating of each group of two or more units normally in service. Higher ratios have no practical value. Ratios as high as 1.6 and 2.0 would represent hopeless requirements, however, if emergency loads and their effects upon operating temperatures did not differ materially from normal load conditions.

We may, therefore, conclude from the foregoing discussion that the average cable loading and load losses in any given duct run are materially lower than might be expected if load growth and the usual provisions for continuous supply were not present. The emergency load losses lie in the range of 1.4 to 4.0 times the normal load losses, or in such a relative magnitude as to require special consideration. Further, the severity of the all-day emergency load occasioned by failures of load-supply elements in service decreases materially as to the number of elements per group increases. Hence, the need for high emergency ratings practically disappears as the number of cables exceeds five or six per substation.

An ordinarily practical engineering approach to increased load ratings would be to determine first what maximum normal load rating may be permitted under the circumstances, then consider whether the probable emergency-load transients will be sufficiently frequent or severe to require that they, rather than the normal load conditions, determine the need for installing additional capacity. Emergency operations differ from normal conditions chiefly in two respects:

1. Emergency loads are transient in nature and, usually, short lived.
2. The probability is usually very remote that an emergency will occur at the time when loads, ambients, and the limitations of installed capacity would combine to require operation of the equipment at the highest possible temperature which is selected as the design limit.

Nearly all loads are variable in their nature rather than constant. Hence, the maximum temperature reached lasts for a very short time and is in most cases considerably lower than would obtain if the usually appreciable thermal capacity of the apparatus were not present to reduce the temperature changes that accompany changes in load.

The problem of emergency load ratings, therefore, reduces to consideration of such factors as the operating temperatures to be expected and their possible relative effect upon the life of the apparatus in question, as determined not only by the temperature-life characteristics of the apparatus, but also the probability of occurrence of transient loads sufficient in magnitude to require operation at above rated temperatures, or with abnormal voltage regulation.

Approximate Frequency of Cable Failures in Service

Perhaps to a greater extent than for any other equipment, the cable engineer has access to a sufficient volume of data to determine with reasonable assurance the probable failure rate for any cable or group of cables. There are, for instance, about 15 cable sections and joints per mile of an underground cable installation, within which distance there may be exposure to a wide variety of conditions such as hot spots, electrolysis, water, abrasion, bending, etc. Relatively few miles of such construction are therefore needed to give the law of averages a reasonably good chance to work.

In any given system, the deviations from the average failure rate usually may be explained. The author is familiar with an instance in which 22 miles of 13.2 kv cable were recently operated for about three years at very nearly twice the former average daily load and at maximum conductor temperatures ranging from 90 to 105 degrees centigrade. The failure rate averaged $4^1/_2$ times the prior ten-year average for this cable, but the station economies made possible by such extraordinary operation far outweighed the increased cost of cable maintenance. By far the chief causes of cable failure in this instance were sheath cracks at cable bends in the manholes or sheath abrasion over rough spots in the duct, both of which were due to the longitudinal movement of the cable. Only one failure occurred in a section of cable that showed definite signs of insulation injury by the inordinately continued repetition of high-temperature operation. There are no doubt other such cable sections that have not come to light and might ultimately accelerate the failure rate but the usual sheath crack was the direct cause of this failure. From these observations there is some indication that, for less severe conditions, the cable failure rate should be expected to increase almost in direct proportion to the thermal movement of the cable, or in proportion to the square of the load carried.

We can now estimate roughly the probable failure rate for cables supplying a given substation. Consider a group of five parallel supply cables, operating regularly at 80 per cent of their combined normal load rating, in which a cable failure may be expected about once in 450 load cycles. If the load were allowed to increase until the five cables would operate regularly at 100 per cent of their normal load rating, the average failure rate due to normal load operation might increase to about $(100/80)^2$ times the previous rate, or one in 290 load cycles. Should a failure then require operation over one load cycle in 290, at a failure rate that is ten times as severe as for the average normal load cycle, the over-all failure rate might further increase to one in 280 load cycles. In such an instance, the chief cause of the increased failure rate, therefore, would appear to be the increased normal loads and temperatures.

Similar analyses in respect to other types of equipment apparently develop similar results. In some cases it may be well to recognize the appreciable life loss due to all causes other than temperature, such as obsolescence, natural old age, mechanical damage, etc. In some instances this may be found to contribute as much to the average rate of life loss as do normal load temperatures. If the magnitude of this factor can be judged, its effect upon the existing rate of life loss should be deducted before estimating the change in life loss chargeable to a change in operating temperatures.

Distribution Load Characteristics

Seldom in the distribution system does the load correspond to the conditions which determine the name-plate rating of the load-carrying apparatus, or the conditions often assumed in establishing load ratings for cables. In some respects questions of seasonal load variations, as well as the daily load changes, involve conditions so complex that it is almost impossible to subject this phase of the problem to rigid analysis. Furthermore, the present growth of electric-range, off-peak, and air-conditioning load introduces factors which have a profound influence upon the shape of the daily load cycle. The development of such loads already has reached the point where the load curve at one location in the distribution system may bear very little direct relationship to that at another. It is no longer possible to select a load cycle that may be said with assurance to be typical or representative of any location except that where it was encountered. This becomes increasingly evident in the smaller supply units of the distribution system, such as substation circuits, distribution transformers, and low-voltage circuits where nearly all degrees of relative saturation of these new loads already exist.

To some extent, however, the range of load variation in a given case may be judged from load curves such as in figure 2*a* for all-day loads, or in figure 2*b* for short-time loads. These illustrate respectively the distribution of normal daily maximum demands and hourly loads throughout the year on one substation. All extraordinary loads were excluded from the data used in their preparation. Substations having identical load factors may have different load curves, although the area under the solid curve of figure 2*b* is proportional to annual load factor. Such curves become quite helpful for use in analyses to determine the approximate probability of meeting given load, voltage, or temperature conditions. The dotted curves shown in these figures illustrate the corresponding loads at each of five levels while the yearly maximum demand is growing, say, from 80 per cent

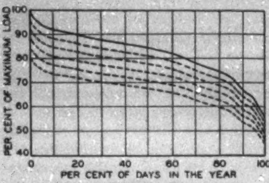

Figure 2a. Illustrative occurrence of daily maximum demands on supply cables, for yearly peak loads ranging from 80 per cent to 100 per cent of normal load rating

to 100 per cent of the installed load rating in a given case.

Approximate Probability of Emergency Load Occurrence

The probability of load occurrence is one of the most important factors in consideration of emergency ratings. Three truths must be appreciated in dealing with this factor, however. First, the necessity for dealing with averages makes the conclusion no better than the average from which it is derived. Second, an occurrence that may be expected no oftener than once in 100 years may confront the calculator within a month. Third, a loading condition which may be expected only once in 25 years in a given group of cables will be faced on an average of twice a year in a system which has 50 such groups of cables. These, however, serve only to emphasize the importance of selecting design limits for emergency operation that are no higher than the proponent is willing to face.

One use of probability will now be illustrated by a sample calculation for five cables supplying a distribution substation. For example, assume a supply-cable failure may be expected on an average of 1.4 times per year and that five years is required for the peak load on that substation to increase from 80 per cent to 100 per cent of the normal peak load capacity on all five cables. To be satisfied with such a design, the engineer must be willing to face the possible need for carrying an emergency load that can reach a maximum of 1.25 times the normal load design limit, should one cable be out of service at the time of the peak in the last year. Let us assume this is reasonable, and calculate the probability that an all-day outage of one cable would require the others to carry a peak load as high as 1.10 times their normal load rating. This will occur whenever the load on the substation would exceed 88 per cent of the total installed normal load rating of the five cables. By reference to curves such as shown in figure 2*a*, it may be found that the normal daily maximum demand in this instance will not exceed 88 per cent of rating at any time during the first or second years, but will during 3.2 per cent of the third year, 11.2 per cent of the fourth year, and 28.5 per cent of the fifth year, or an average of 8.58 per cent per year. With 1.4 cable failures per year uniformly distributed, then, the probability of cable failures during daily load cycles whose normal maximum loads exceed 88 per cent of rating becomes 0.0858 times 1.4 or 0.120. In this instance, then, all-day cable outages might be expected to require the remaining cables to operate at loads above 110 per cent of normal load rating on an average of once in about eight years. Other points may be calculated similarly and have been shown in the lower curve of figure 3. Should one-half day be ample to repair a cable fault, the corresponding time intervals for this curve would double.

For short-time emergencies the method of calculation is about the same as for the case just illustrated and may give a result quite like the dotted curve of figure 3. However, if figure 1 should represent the probable duration of such short-time emergencies, it is apparent that only one in seven will last as long as one-half hour. Hence, the probable interval between occurrence of loads requiring one-half hour or longer operation at the given or a greater load becomes seven times as long as the probable interval between the short-time emergencies. This latter relationship is also shown in figure 3.

As earlier pointed out, such calculations should not be used as a basis for making close decisions. These calculations, for instance, do not recognize the certainty that unbalance of loads between cables may load some heavier than others, etc. However, such curves become quite helpful in judging the maximum amount of life loss which is reasonable to accept, in selecting the design limits for emergency and normal load ratings. In the instance just used for illustration, the author considers the emergency-load design limits sufficiently conservative to permit their use as often as once each year, without any apparent effect upon cable maintenance costs and without any ominous increase in the likelihood of such operation causing a second cable failure during such a state of emergency.

The foregoing discussion can serve only to point out some of the factors which work together to make the probability of severe loads and temperatures much more remote than the apparatus engineer is apt to realize. Variations in the rate of load growth seem not to have an important effect upon the answer. Neither is it necessary to have an accurate knowledge of the failure rate since variations in the range of two to one can only halve the number of years between the occurrence of a given or higher load. In any event it is apparent that the probability of severe loading conditions is quite remote.

Since the major purpose of emergency ratings is to permit increasing the average-day loading of the load-carrying elements in the system and this may increase voltage regulation, as well as operating temperatures, it becomes necessary to give some passing thought to voltage.

Voltage Regulation

Voltage regulators can be made to take care of increased regulation elsewhere and, with present available methods, can also reduce the spread between the first and last customers on the circuit. Small fixed boosters, shunt capacitors, and step-type regulators are now available for use by those who find that increased load ratings introduce normal-voltage regulation problems that cannot be solved as well by orthodox copper addition or rearrangement.

With the gradual extension of some form of the network principle beyond the substation bus and farther out into the distribution system, emergency voltage regulation, however, becomes one of increasing importance. This is particularly true of aerial distribution. It is the more urgent because the number of parallel paths normally available for the supply of the load in this part of the system often is as few as two, hence the instantaneous ratio of emergency to normal load approaches values as high as 2.00.

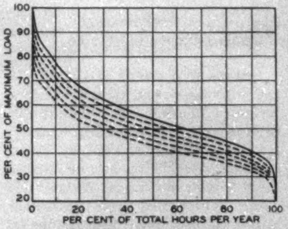

Figure 2b. Illustrative substation supply-cable load duration, for peak loads ranging from 80 per cent to 100 per cent of normal load rating

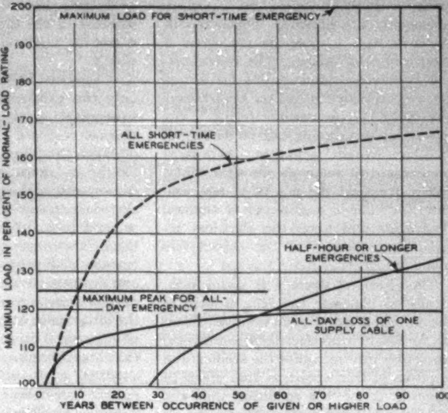

Figure 3. Approximate probability of load occurrence for five cables supplying one substation

The principal gain possible by "networking" is its contribution to increased service continuity. This, however, is a voltage problem in its own right, since service discontinuity is the simple case of no voltage at all. To illustrate further, let us assume that there are two circuits supplying adjacent areas with voltages which lie outside the established limits of normal voltage variation only when one of the circuits fails to deliver any voltage at all, while trouble-men labor to clear the fault. By suitably "networking" these two circuits it may be possible to make the "no voltage" occasions much less frequent. The cost may well be unreasonable, if not almost prohibitive, however, should the design be made sufficiently rigid to prevent any possibility of ever delivering to the last customer a supply voltage that lies outside the accepted limits for normal operation.

This raises the rather natural question whether it may not be good business to improve service continuity just so long as no step thus taken will result in delivering to the last customer less voltage than he needs to permit his refrigerator motor to start; should its thermostat call for power in the rare instance where none would be available if the network had not been provided to supply no less than the bare necessities, until conditions can be restored to normal. The probability that apparatus failures will conspire with loads to precipitate such a situation, during the hour or less required to clear the fault in such instances, may be determined without much difficulty, as for figure 3. Thus, this phase of emergency ratings would appear to suggest the occasional need for considering some design limits for emergency voltage regulation that may be quite apart from those for normal operation. As in temperature considerations, it may often be found that conditions necessary to satisfy the normal voltage requirements will need no assistance to keep utilization equipment operating in the rare instance when an emergency may cause temporarily abnormal voltage regulation at some extreme locations.

Temperature Transients

Contrary to the author's expectations, when he approached the emergency-rating problem several years ago, it has become increasingly evident that the probable interval between occasions when it would be necessary to operate at high emergency temperatures is often so long that the question of whether such an operation would cause 10 or 300 times the normal rate of life loss for the day in which it should occur reduces to one of comparative unimportance. Even as high a rate as 300 times normal life loss over one load cycle may be no more severe than one year of normal operation. Montsinger's evidence[2] that insulation life loss may in some cases double for each 8-degree-centigrade increase in temperature, however, seems to be sufficient to indicate that temperature calculations used as the basis for emergency ratings may occasionally require a practical answer that lies somewhere within 10 degrees or 20 degrees centigrade of the actual. The accumulation of sufficient data in practical operation to indicate which of the present theories of life loss is best may require some time. It will require reasonably accurate knowledge of the temperatures responsible for that life loss.

Published methods for direct calculation of operating temperatures for cables, transformers, etc., are available. Mathematical analysis, however, can give no better answer than is permitted by the accuracy with which the various thermal coefficients and temperature corrections are known. These often cannot be evaluated deductively without disquieting assumptions and considerable labor. Hence there appears to be some need for a little better general understanding of means which permit the engineer to determine empirically from test or other available data, all the coefficients needed to estimate the thermal response of the assembly in question to any regular or irregular transient load impulse. This can be accomplished by the use of an equivalent circuit which is not materially different from that usually assumed in calculations of steady-state load temperatures. Since load transients require consideration of copper losses only, it is helpful to treat the hot-spot temperature θ in two components:

1. The constant component or "threshold temperature" T_0 which is the sum of the ambient temperature and the steady-state temperature rise caused by such factors as no-load losses and that part of the copper-temperature rise which corresponds to the average daily copper losses of the cable itself and its associates in the given conduit run.
2. The variable component θ_1 caused by the instantaneous difference p between the impressed copper losses and those copper losses, if any, which contribute to the "threshold temperature."

The "threshold temperature" component may be developed exactly as the calculator has been in the habit of estimating normal-load temperatures. This method of approach makes it necessary to know the transient load characteristic of the assembly for a total period equal only to the maximum probable duration of the emergency. There is seldom need to deal with transients which persist longer than one day.

Now, let us consider the thermal circuit for the dissipation of the copper (variable) losses through the various successive steps encountered in the transfer of these losses from the conductor out to ambient. In a cable installation, for instance, there is a series of successive steps, such as copper, insulation, sheath, etc., which can be represented by the electric-circuit analogy of figure 4. This approaches an exact representation, as the steps become smaller. In this circuit the thermal dissipation coefficients $s_{12}, s_{23}, \ldots, s_n$ are analogous to electrical conductances, while $k_1, k_2, \ldots, k_n$ are analogous to electrical capacitances. The instantaneous copper-loss input p has the nature of an electric current and the temperature rise θ_1 is the resultant "voltage" rise needed at the sending end of the circuit to cause "current" p to flow. As in most electric circuit analyses we shall tentatively disregard the effects of temperature upon the s coefficients of thermal dissipation and upon the electrical resistance component of the copper loss input p.

The circuit of figure 4 is useful principally to illustrate the general form of the natural response to an impulse. From his familiarity with electric-circuit theory, the reader may recognize the sending end response of this circuit as that given in equation 3, for the condition that the input p is held constant at some value P:

$$\theta_1 = AP(1-\epsilon^{-at}) + BP(1-\epsilon^{-bt}) + \ldots + NP(1-\epsilon^{-nt}) \quad (3)$$

Those who do not recognize this response may observe the development of that for a "two-chunk" circuit, in appendix *B*. Theoretically, each additional step in the circuit of figure 4 will require an additional term in equation 3. Where there are many steps, or the circuit constants are distributed rather than lumped, it is often necessary to use some mathematical expedient to evaluate equation 3. In his analysis of radial heat flow, for instance, Church[4] has shown how it is possible to obtain an expression for the harmonic impedance function for each homogenous step in the thermal circuit and, from these data, to determine the position of each of several points along the rectangular impulse characteristic. He has used a method so powerful that it can digest nearly any combination of steady-state harmonic functions that will represent the circuit at hand, and will produce the numerical position of any desired point along the curve of its response to a rectangular impulse. This method of harmonic analysis gets the answer, one point at a time, but seldom gives the response directly in its natural algebraic form. By whatever expedient the various points are evaluated, however, the characteristic response of the thermal circuit in figure 4 has the algebraic form given in equation 3.

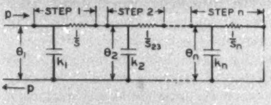

Figure 4. Thermal circuit

It has been known for some time that only two exponential terms are needed to represent quite accurately the over-all thermal response of all the parts in an oil-filled transformer assembly. Similarly, it is known that aerial conductors, disconnects, and other assemblies whose current-carrying parts are sufficiently exposed to ambient temperatures, have thermal responses that may be quite accurately represented by the use of only one exponential term. Consequently, it does not seem to require undue use of the imagination to conclude that the form of equation 3 may be found to represent the natural heating characteristic for any situation to be encountered in electric power distribution. In any event, it should be possible to judge approximately the extent to which this is true throughout the range of a given set of observations. Let us now consider briefly the manner in which as few as two equivalent parts of such a circuit may be made to represent the infinite series of figure 4 and the following simpler equation:

$$y = \theta_1/P = A(1-\epsilon^{-at}) + B(1-\epsilon^{-bt}) \quad (4)$$

In appendix *A* there is developed a method for determining the effective values of the parameters *A*, *a*, *B*, and *b*, which will make equation 4 approximately fit any group of test observations for a given rectangular load impulse. In table II are shown the values of the parameters used in plotting the four curves drawn in figure 7, for underground cables. The curves are drawn to show the loci of equation 4 with respect to the observed points, also shown for comparison along the heating characteristics. In this case the observations shown were calculated by Church's method (AIEE TRANSACTIONS for 1931, page 982, and for 1935, page 1166) as modified to recognize some additional features to which the author's discussion of Church's latter paper called attention (AIEE TRANSACTIONS for 1936, page 398).

Such faithful correlation, in figure 7, for the difficult case of the underground cable, has led the author to conclude that equation 4 can represent the rectangular load impulse characteristic of any assembly, for periods up to 24 hours duration, with sufficient practical accuracy to leave little need for rigorous analysis when suitable test data are available or can be obtained. If this is true, there is promise for considerably simplifying calculations of the emergency temperature transients which accompany other than rectangular load impulses.

Table II. Equivalent Thermal Circuit Constants for 13.2-Kv Three-Conductor 350,000-Circular-Mil Cable in an Underground Duct Bank

	Hottest of Eight Equally Loaded Cables		Hottest of Two Equally Loaded Cables	
	Cables Dry	Cables in Water	Cables Dry	Cables in Water
1. Empirical circuit constants (Appendix A)				
A	7.48	3.52	7.00	3.56
B	12.9	13.7	10.4	5.92
a	0.819	2.08	0.857	2.07
b	0.118	0.0304	0.136	0.0480
2. Approximate circuit constants (Appendix B)				
K_1	0.132	0.130	0.133	0.130
S_{12}	0.090	0.256	0.096	0.260
K_2	0.763	2.34	0.884	3.58
S_2	0.108	0.0753	0.143	0.178

The approximate circuit of figure 5 most nearly corresponds to the actual thermal circuit, but has no particular advantage over the empirical circuit of figure 6 which has about the simplest arrangement of thermal capacities and resistances that will give exactly the same sending-end response to all regular or irregular impulses. The coefficients for the empirical circuit of figure 6 are known immediately upon finding the parameters which make equation 4 fit the observed data, as by the method of appendix *A*. An advantage of the empirical circuit lies in the fact that the instantaneous input is the same for the *A* part as for the *B* part, while the instantaneous response of the *A* part is absolutely independent of that for the *B* part, so that:

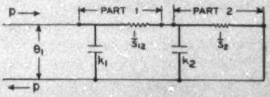

Figure 5. Approximate circuit

$$\theta_1 = \theta_a + \theta_b \quad (5)$$

while,

$$\Delta\theta_a = (apA - a\theta_a)\Delta t \quad (6)$$

and,

$$\Delta\theta_b = (bpB - b\theta_b)\Delta t \quad (7)$$

For those who are sufficiently interested, appendix *B* gives the corresponding instantaneous relationships found in the approximate circuit of figure 5, as well as the relations between the equivalent thermal constants of this circuit and the parameters of the empirical circuit which has an identical response to all transients. The following relations are included:

$$K_1 = \frac{1}{aA + bB} \tag{8}$$

$$S_{12} = K_1(a + b) - K_1^2ab(A + B) \tag{9}$$

$$S_2 = \frac{S_{12}}{S_{12}(A + B) - 1} \tag{10}$$

$$K_2 = \frac{S_{12}S_2}{abK_1} \tag{11}$$

The calculator need know no more about the thermal characteristics of the assembly. Should he desire, he may now develop the approximate harmonic impedance function directly from figure 6, by analogy to the electric circuit of parallel resistance and capacitance. For instance, let $X_a = aA/\omega$ and $X_b = bB/\omega$, where ω equals $2\pi f$ radians per hour. Then the impedance $\boldsymbol{Z}$ of the empirical circuit becomes:

$$\boldsymbol{Z} = -j\left[\frac{AX_a}{A - jX_a} + \frac{BX_b}{B - jX_b}\right]$$

$$= \left[\frac{AX_a^2}{A^2 + X_a^2} + \frac{BX_b^2}{B^2 + X_b^2}\right] -$$

$$j\left[\frac{A^2X_a}{A^2 + X_a^2} + \frac{B^2X_b}{B^2 + X_b^2}\right] \tag{12}$$

However, equations 6 and 7 offer means for a simple and direct tabular calculation that avoids any need for dealing with complex numbers or breaking the instantaneous values of p down into the various harmonic components. For instance, assume that: $B = 24$ degrees centigrade ultimate rise due to rated-load copper losses, while $b = 0.24$ and that it is desired to calculate $\Delta\theta_b$ for quarter-hour intervals ($\Delta t = 0.25$ hour). Substituting these values in equation 7 gives the following guide for calculating the approximate temperature change across resistance B during each quarter-hour interval:

$$\Delta\theta_b = 1.43p - 0.060\theta_b \tag{7a}$$

Table III shows a sample calculation for one illustrative load cycle, for which the load value at the beginning of each interval has been expressed in terms of the ratio R of load to rating. In instances where the "threshold temperature" does not need to include any part of the copper losses, it is often convenient to express the instantaneous load values in times rating R since this method gives the maximum temperature rise directly in terms of rating. The effects of temperature upon the values of p or the coefficients of the thermal circuit seldom have any practical importance in such calculations, so long as the parameters of equation 4 are based upon observations at constant load throughout the range of temperature changes within which it is desired to operate. The parameters then include the average temperature correction. The tabular calculation of θ_a may be made exactly as that for θ_b. Their maxima will not be coincident, but questions of such time-phase displacement take care of themselves. The fact that neither θ_a nor θ_b has any practical significance if taken alone, is of no disadvantage. The algebraic sum of their instantaneous values is always equal to the hot-spot rise θ_1 above the "threshold temperature" T_0. When dealing with cable-temperature calculations, in which the instantaneous value of p equals the total copper losses less the average daily copper losses in that cable, the values of p will be negative whenever the total copper losses fall below the average daily copper losses, if the calculator should elect to investigate temperatures during light-load periods. This method, however, makes it usually not necessary to start tabular calculation at a time when the copper losses are less than the average for a normal day.

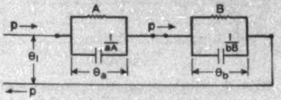

Figure 6. Empirical circuit

Table III. A Sample Calculation Using Equation 7a as a Guide

Time (t)	Load (r) in Times Rating	1.43 p (1.43 r²)	−0.060 θ_b	$\Delta\theta_b$	θ_b
4:00 p.m.	0.34 R	0.167 R^2	−0.167 R^2	0*	2.78 R^2
4:15	0.55 R	0.432 R^2	−0.167 R^2	0.27 R^2	3.05 R^2
4:30	0.57 R	0.465 R^2	−0.183 R^2	0.28 R^2	3.33 R^2
4:45	0.69 R	0.682 R^2	−0.200 R^2	0.48 R^2	3.81 R^2
5:00	0.71 R	0.720 R^2	−0.228 R^2	0.49 R^2	4.30 R^2
5:15	0.74 R	0.785 R^2	−0.258 R^2	0.53 R^2	4.83 R^2
5:30	0.86 R	1.06 R^2	−0.290 R^2	0.77 R^2	5.60 R^2
5:45	0.95 R	1.29 R^2	−0.34 R^2	0.95 R^2	6.55 R^2
6:00	1.00 R	1.43 R^2	−0.39 R^2	1.04 R^2	7.59 R^2
6:15	0.99 R	1.40 R^2	−0.46 R^2	0.94 R^2	8.53 R^2
6:30	0.92 R	1.21 R^2	−0.51 R^2	0.70 R^2	9.23 R^2
6:45	0.95 R	1.29 R^2	−0.55 R^2	0.74 R^2	9.97 R^2
7:00	0.89 R	1.13 R^2	−0.60 R^2	0.53 R^2	10.50 R^2
7:15	0.82 R	0.960 R^2	−0.630 R^2	0.33 R^2	10.83 R^2
7:30	0.82 R	0.960 R^2	−0.650 R^2	0.31 R^2	11.14 R^2
7:45	0.80 R	0.918 R^2	−0.668 R^2	0.25 R^2	11.39 R^2
8:00	0.83 R	0.985 R^2	−0.684 R^2	0.30 R^2	11.69 R^2
8:15	0.71 R	0.720 R^2	−0.700 R^2	0.02 R^2	11.71 R^2
8:30	0.74 R	0.785 R^2	−0.705 R^2	0.08 R^2	11.79 R^2 maximum
8:45	0.68 R	0.660 R^2	−0.71 R^2	−0.05 R^2	11.74 R^2
9:00	0.62 R	0.550 R^2	−0.70 R^2	−0.15 R^2	11.59 R^2

* Note: This is often a safe assumption, if load changes are small prior to the period under observation.

The time interval Δt that is used in equations 6 and 7 for practical calculations of cable temperature, seldom needs to be less than one-fourth to one-half hour, depending chiefly upon the number of observations of p that are needed to give a fair representation of the variations in copper losses. Adequate recognition of the thermal circuit itself is apparently retained so long as the interval Δt thus selected is not much larger than $(A + B)/4(aA + bB)$. When the value of a is large and a/b is greater than nine as for many transformers, however, it is readily possible that a more convenient value of Δt will make $a\Delta t$ greater than three. When this is true θ_a from equation 6 will so nearly reach its ultimate value pA within each step as to permit assuming this simplification of equation 6, and using fewer steps.

While the foregoing method appears to be direct and convenient for transient-temperature calculations, it is only one of several. It has been helpful to the author, in occasional difficult situations and has been outlined with the thought it may be helpful to others who are not already familiar with a method that produces satisfactory results. Many time-saving opportunities will appear to the calculator, as he becomes more familiar with the method he elects to use.

A possible use of the circuits of figures 5 and 6 for underground cables lies in the ease with which they permit a direct though somewhat approximate estimate of what would be the corresponding thermal circuit for some other size or type of cable in the same situation. An inkling of this possibility may be had from table II in which it will be observed that K_1 has the same value in each of the four situations, where S_{12} changes only with the addition of water in the duct. For underground installations, therefore, part 1 appears to be the cable itself. The per-

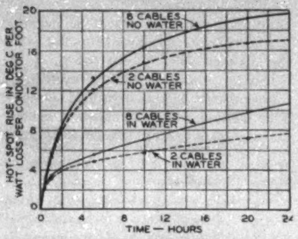

Figure 7. Hot-spot response to constant load losses, for groups of equally loaded 13.2-kv three-conductor 350,000-circular-mil cables in duct

centage change in the thermal capacity coefficient of the cable, also the corresponding percentage change in thermal conductance from copper out through the sheath, may be estimated in a few minutes. The effect of these changes in K_1 and S_{12} upon the parameters a, b, A, and B, may then be determined (appendix B) with almost the same confidence as though the original observations represented such cable.

A Word About Tests and Thermal Corrections

There are a few points regarding tests and thermal corrections that may be worthy of passing mention. Until one becomes thoroughly familiar with his data, it is usually best to express input in watts, thermal capacity in watt-hours per degree centigrade, thermal dissipation in watts per degree centigrade, and temperature rise in degrees centigrade per watt. Otherwise the test observations of rising temperature at constant load will appear to give a slightly faster moving response than similar observations after the load is removed. This is because of the greater acceleration caused by the effect of thermal increases in electrical resistance upon input in the former case. When using test data expressed in such units, there can be no confusion as to when to make some approximate final adjustment for the higher electrical resistance at the higher temperature. If the correction is excluded from the test data, it should be recognized in the calculation.

Where the hot-spot temperature cannot be read directly during a test on a given installation, the average copper temperature may be assumed to equal the hot spot, with sufficient accuracy for cable-temperature analyses. In oil-filled transformers, the hot-spot temperature at any time should be about equal to the average winding temperature plus the oil temperature rise from that at a level opposite the midpoint of the winding to that of the top oil. Thus it is usually possible to determine the approximate hot-spot temperature at each point of the test curve for cases where it cannot be read directly. It is seldom necessary to test any one assembly at more than one constant load value. If observations are taken on the heating curve, it may be helpful in some cases to continue them on through the cooling cycle after the load has been removed. In this way the one set of observations may be used as a check on the other.

Conclusion

These notes have been prepared with the thought that they will serve their purpose if they show in a general way some of the conceptions that should be included in a broad review of the opportunities to effect system economies by the judicious application of emergency ratings.

List of Symbols

A, B, etc. = parameters
a, b, etc. = parameters
f = harmonic frequency, in cycles per hour
K_1, K_2, etc. = thermal capacity coefficients, in (watts)(hours)/(degrees centigrade)
P = maximum value of copper loss transient, in watts
p = instantaneous value of copper loss transient, in watts
R = ratio of maximum load to rating
r = instantaneous load
S_{12}, S_2, etc. = thermal dissipation coefficients, in (watts)/(degrees centigrade)
T_0 = threshold temperature, in degrees centigrade. It includes all constant components of the hot-spot temperature
t = time in hours
U = ratio of total installed emergency-load capacity to average yearly maximum demand
u = instantaneous ratio of emergency to normal load
x = dy/dt
y = thermal response (θ_1/P) to an impulse, in (degrees centigrade)/(watts)
Z = harmonic impedance of the thermal circuit
ϵ = base of Napierian logarithms
θ = hot-spot temperature (T_0 plus θ_1), in degrees centigrade
θ_1 = variable component of hot-spot temperature, in degrees centigrade
θ_a and θ_b = arbitrary components of θ_1
ω = $2\pi f$ radians per hour

Appendix A. Empirical Analysis of Temperature-Time Characteristic for a Rectangular Load Impulse

Given the test curve of temperature, such as shown in figure 7, which represents the temperature-time characteristic of a given assembly when subjected to a rectangular load impulse. The problem is to find the values of A, B, a, and b, which when substituted in the equation:

$$y = A(1 - \epsilon^{-at}) + B(1 - \epsilon^{-bt}) \tag{13}$$

will make it fit the curve with reasonable fidelity for all values of t within the range of the observed data.

Select five points on the curve: one (t_1, y_1) which lies well up on the fast moving part of the curve; one (t_0, y_0) at $t = 0.5t_1$; and three others at about $t_2 = mt_1$, $t_3 = 2mt_1$, and $t_4 = 4mt_1$, in such a way that m is greater than 4, but $4mt_1$ includes no more than 80 per cent of the transient's apparent life. Graphically determine the slope x of the curve at each of the three latter points.

Now solve for b by considering the points (t_2, y_2), (t_3, y_3), and (t_4, y_4), which have been selected in such a way that they should be in the region where (at) is greater than 4, hence, for practical purposes,

$$y = A + B(1 - \epsilon^{-bt}) \tag{13a}$$

and

$$\frac{dy}{dt} = Bb\epsilon^{-bt} = x \tag{14}$$

From the observed slopes x_2, x_3 and x_4, then,

$$\frac{x_2}{x_3} = \epsilon^{b(t_3 - t_2)}, \quad \frac{x_2}{x_4} = \epsilon^{b(t_4 - t_2)}, \quad \frac{x_3}{x_4} = \epsilon^{b(t_4 - t_3)} \tag{15}$$

From these three relationships it is possible to write directly the value of b which most nearly satisfies these observations.

$$b = \frac{1}{3}\left[\frac{\log_n(x_2/x_3)}{t_3 - t_2} + \frac{\log_n(x_2/x_4)}{t_4 - t_2} + \frac{\log_n(x_3/x_4)}{t_4 - t_3}\right] \tag{16}$$

To solve for B substitute the value of b from equation 16, also (y_2), (y_3), and (y_4) respectively in equation 13a, as:

$$y_2 = A + B(1 - \epsilon^{-bt_2}),\ y_3 = A + B \times (1 - \epsilon^{-bt_3}),\ y_4 = A + B(1 - \epsilon^{-bt_4})$$

then

$$y_4 - y_2 = B_{24}(\epsilon^{-bt_2} - \epsilon^{-bt_4})$$

$$y_4 - y_3 = B_{34}(\epsilon^{-bt_3} - \epsilon^{-bt_4})$$

$$y_3 - y_2 = B_{23}(\epsilon^{-bt_2} - \epsilon^{-bt_3})$$

Hence

$$B = \frac{1}{3}\left(\frac{y_4 - y_2}{\epsilon^{-bt_2} - \epsilon^{-bt_4}} + \frac{y_4 - y_3}{\epsilon^{-bt_3} - \epsilon^{-bt_4}} + \frac{y_3 - y_2}{\epsilon^{-bt_2} - \epsilon^{-bt_3}}\right) \tag{17}$$

To solve for A, substitute B, b, and the observed values of y_2, y_3, and y_4 in equation 13a, as:

$$A_2 = y_2 - B(1 - \epsilon^{-bt_2})$$

$$A_3 = y_3 - B(1 - \epsilon^{-bt_3})$$

$$A_4 = y_4 - B(1 - \epsilon^{-bt_4})$$

$$A = \frac{A_2 + A_3 + A_4}{3} = \frac{y_2 + y_3 + y_4}{3} - B + \frac{B}{3}(\epsilon^{-bt_2} + \epsilon^{-bt_3} + \epsilon^{-bt_4}) \quad (18)$$

To solve for a, consider the points t_0 and t_1 between the origin and the apparent "knee" of the curve. In this region it should be found that ϵ^{-bt} is very nearly equal to $(1 - bt)$. Otherwise select more suitable points nearer the origin, then substitute the new values of (t_1), (y_1), and $(\epsilon^{-bt_1} = 1 - bt_1)$ in equation 13 as follows:

$$y_1 = A(1 - \epsilon^{-at_1}) + Bbt_1$$

similarly,

$$y_0 = A(1 - \epsilon^{-at_0}) + Bbt_0$$

and from these,

$$a = \frac{1}{2}\left[\frac{1}{t_1}\log_n\left(\frac{A}{A + Bbt_1 - y_1}\right) + \frac{1}{t_0}\log_n\left(\frac{A}{A + Bbt_0 - y_0}\right)\right] \quad (19)$$

Equations 16, 17, 18, and 19 permit evaluating all the constants needed to make equation 13 represent the actual conditions about as well as the data from which they were developed. A sample calculation follows.

Sample Calculation of A, a, B, and b for Hottest of Eight Equally Loaded Cables in Duct in Earth

Given:

Point	t(Hours)	y	(dy/dt) = x
	0	0	
t_0	0.5	3.34	
t_1	1.0	5.60	
t_2	5.0	13.30	0.978
t_3	10.0	16.30	0.434
t_4	20.0	19.20	0.190

$$b = \frac{1}{3}\left[\frac{\log_n 2.25}{(t_3 - t_2) = 5} + \frac{\log_n 5.15}{(t_4 - t_2) = 15} + \frac{\log_n 2.29}{(t_4 - t_3) = 10}\right] = 0.118 \quad (16a)$$

$$B = \frac{1}{3}\left[\frac{19.2 - 13.3}{\epsilon^{-0.59} - \epsilon^{-2.36}} + \frac{19.2 - 16.3}{\epsilon^{-1.18} - \epsilon^{-2.36}} + \frac{16.3 - 13.3}{\epsilon^{-0.59} - \epsilon^{-1.18}}\right] = 12.90 \quad (17a)$$

$$A = \frac{1}{3}[13.3 + 16.3 + 19.2 - 38.7 + 12.90(\epsilon^{-0.59} + \epsilon^{-1.18} + \epsilon^{-2.36})] = 7.48 \quad (18a)$$

$$a = \frac{1}{2}\left[\frac{1}{1}\log_n\left(\frac{7.48}{7.48 + 1.52 - 5.60}\right) + 2.0\log_n\left(\frac{7.48}{7.48 + 0.76 - 3.34}\right)\right] = 0.819 \quad (19a)$$

$$y = 7.48(1 - \epsilon^{-0.819t}) + 12.90(1 - \epsilon^{-0.118t}) \quad (20)$$

A plot of equation 20, which is the upper curve drawn in figure 7, shows the manner in which it represents the observations from which it was derived.

Appendix B. Direct Analysis of the Two-Chunk Series Thermal Circuit with Constant Coefficients

The electric circuit which most nearly represents the relations between the usual thermal coefficients for the respective parts of the equivalent thermal circuits for transient load losses in transformers as well as cable installations, etc., is shown in figure 5 of this paper, in which (S_{12}) and (S_2) have the nature of conductances, (K_1) and (K_2) are thermal capacity coefficients, (p) has the nature of an electric current, and θ corresponds to the voltage rise required to cause "current" (p) to flow. Of the heat (pdt) flowing into part one, some $(K_1d\theta_1)$ is absorbed by part one and the rest $(\theta_1 - \theta_2)S_{12}dt$ flows on into part two, hence:

$$pdt = (\theta_1 - \theta_2)S_{12}dt + K_1d\theta_1 \quad (21)$$

and similarly, the heat entering part two $(\theta_1 - \theta_2)S_{12}dt$ is partly absorbed and partly dissipated through (S_2), or,

$$(\theta_1 - \theta_2)S_{12}dt = \theta_2S_2dt + K_2d\theta_2 \quad (22)$$

By solving (21) and (22), first to eliminate (θ_2) and then (θ_1), the two following instantaneous relationships are obtained, one in terms of the hot-spot rise (θ_1) and the other in terms of (θ_2):

$$\frac{d^2\theta_1}{dt^2} + \left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)\frac{d\theta_1}{dt} + \frac{S_{12}S\,\theta_1}{K_1K_2} = p\left(\frac{S_{12} + S_2}{K_1K_2}\right) \quad (23)$$

and

$$\frac{d^2\theta_2}{dt^2} + \left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)\frac{d\theta_2}{dt} + \frac{S_{12}S_2\theta_2}{K_1K_2} = p\left(\frac{S_{12}}{K_1K_2}\right) \quad (24)$$

When the values of the S and K coefficients are constants and $p = P$ one solution for each of these linear equations takes the following form:

$$\theta = AP(1 - \epsilon^{-at}) + BP(1 - \epsilon^{-bt}) \quad (25)$$

In either case the values of a and b will be as follows:

$$a = \frac{1}{2}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right) + \sqrt{\frac{1}{4}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)^2 - \frac{S_{12}S_2}{K_1K_2}} \quad (26)$$

$$b = \frac{1}{2}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right) - \sqrt{\frac{1}{4}\left(\frac{S_{12}}{K_1} + \frac{S_{12} + S_2}{K_2}\right)^2 - \frac{S_{12}S_2}{K_1K_2}} \quad (27)$$

while the values of A and B which make equation 25 represent θ_1, are

$$A = \left(\frac{1}{a - b}\right)\left[\frac{1}{K_1} - b\left(\frac{1}{S_{12}} + \frac{1}{S_2}\right)\right] \quad (28)$$

$$B = -\left(\frac{1}{a-b}\right)\left[\frac{1}{K_1} - a\left(\frac{1}{S_{12}} + \frac{1}{S_2}\right)\right] \quad (29)$$

Conversely, if equation 25 accurately represents test observations of the hot-spot temperature (θ_1), the effective values of K_1 K_2, S_{12}, and S_2 become:

$$K_1 = \frac{1}{aA + bB} \quad (8)$$

$$S_{12} = K_1(a + b) - K_1^2ab(A + B) \quad (9)$$

$$S_2 = \frac{S_{12}}{S_{12}(A + B) - 1} \quad (10)$$

and

$$K_2 = \frac{S_{12}S_2}{abK_1} \quad (11)$$

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Discussion

R. J. Woodrow (Philadelphia Electric Company, Philadelphia, Pa.): In his analysis Mr. Kidder has shown that the approximate thermal circuit given by figure 5 of his paper can be replaced by and used interchangeably with the empirical circuit of figure 6 in order to analyze the temperature rise θ_1, and has given equations in appendix B by which the constants of either circuit can be calculated from those of the other. This is particularly valuable in investigating the thermal characteristics of a number of types of electrical equipment such as transformers, regulators, oil circuit breakers, and cables, whose thermal heating circuits are for all practical purposes composed of two parts as shown in figure 5.

In order to determine rapidly and simply the constants a, b, A, and B of the empirical

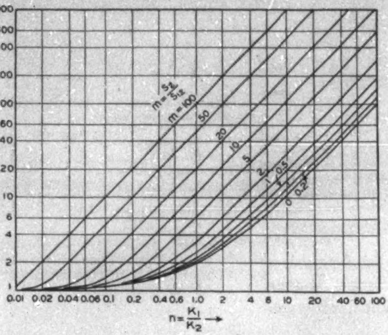

Figure 1

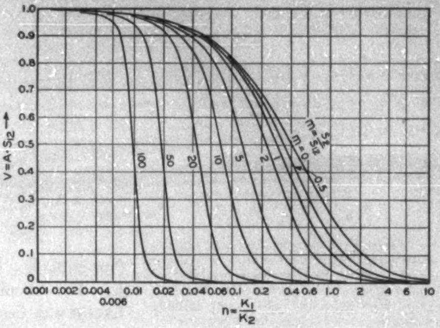

Figure 2

circuit of figure 6 of Mr. Kidder's paper from the constants S_{12}, S_2, K_1, and K_2 of the circuit of figure 5, three sets of curves have been drawn and are shown in figures 1, 2, and 3 of this discussion. By entering these curves with $n = K_1/K_2$ and $m = S_2/S_{12}$, A and B can be determined directly in per unit of the corresponding thermal resistances $1/S_{12}$, and $1/S_2$ of figure 5, and a and b in per unit of the corresponding reciprocal time constants S_{12}/K_1 and S_2/K_2. The relations which apply when using the curves are as follows:

Enter curves with $n = \dfrac{K_1}{K_2}$ and $m = \dfrac{S_2}{S_{12}}$

Read values of U, V, and W.

Then

$$a = U \cdot \frac{S_{12}}{K_1} \qquad b = \frac{1}{U} \cdot \frac{S_2}{K_2}$$

$$A = V \cdot \frac{1}{S_{12}} \qquad B = W \cdot \frac{1}{S_2}$$

As stated by Mr. Kidder the temperature differences θ_a and θ_b of the empirical circuit of figure 6 have no real significance and actually do not exist in the equipment. There are some cases, however, in which these temperature differences are so nearly the same as those that actually do exist across parts 1 and 2 of the circuit of figure 5 that, practically, they may be assumed to be the same. From the curves it may be seen that when $n = K_1/K_2$ is less than 1/50, and $m = S_2/S_{12}$ is less than 1/1, the constants of the empirical circuit and those of the actual circuit do not in any case differ by more than five per cent. Thus in an oil-filled transformer, for example, where the above limiting values of m and n are somewhat representative, there is not much error in considering the thermal circuits as if the constants of figure 5 were identical to those for figure 6 (that is, $U = V = W = 1.0$).

Equation 4 of Mr. Kidder's paper can be rewritten slightly as follows to give the temperature θ_1:

$$\theta_1 = P[A(1 - \epsilon^{-at}) + B(1 - \epsilon^{-bt})]$$

in which there are two exponential terms with different time constants. It should be pointed out that not only does the solution for θ_1 have two exponential terms, but θ_2, the temperature rise of part 2, and $\theta_1 - \theta_2$, the temperature difference across part 1 of figure 6, also have two exponential terms with the same time constants in their solutions. Thus:

$$\theta_2 = \frac{P}{S_2}\left[1 - \frac{1}{a-b}\left\{a\epsilon^{-bt} - b\epsilon^{-at}\right\}\right]$$

$$\theta_1 - \theta_2 = \frac{P}{S_{12}}\left[1 - \frac{1}{a-b}\left\{\left(\frac{S_{12}}{K_1} - b\right)\epsilon^{-at} + \left(a - \frac{S_{12}}{K_1}\right)\epsilon^{-bt}\right\}\right]$$

Again taking transformer heating as an example, this means that the top oil temperature rise (θ_2) has both a slow-moving and a fast-moving term in its response to a steadily applied load, the fast-moving term being opposite in sign to that of the slow-moving term and thus causing θ_2 to rise less rapidly for a short time after the load is applied. Likewise in the temperature rise ($\theta_1 - \theta_2$) of winding above top oil temperature there are both fast-moving and slow-moving exponential terms. However as previously pointed out, with n approximately 1/50 and m approximately 1/1, the error is not large in considering the top oil temperature as having a single slow-moving term in its response, and the winding rise above top oil as having a single fast-moving term in its response.

Although it is approximately correct to assume that the heating of an assembly having a thermal circuit such as given by figure 5 of Mr. Kidder's paper is practically the same as given in figure 6 with no change in constants if n is less than 1/50 and m less than 1/1, when these ratios increase the error rapidly becomes larger. For example if $K_1 = K_2$ and $S_{12} = S_2$ (that is, $m = n = 1.0$), then the following values result:

$$a = 2.62 \cdot \frac{S_{12}}{K_1} \qquad b = 0.38 \cdot \frac{S_2}{K_2}$$

$$A = 0.11 \cdot \frac{1}{S_{12}} \qquad B = 1.89 \cdot \frac{1}{S_2}$$

V. M. Montsinger (General Electric Company, Pittsfield, Mass.): I wish to comment on the statement made in the last paragraph preceding the conclusion to the effect that in oil-filled transformers the hot-spot temperature at any time should be about equal to the average winding temperature plus the difference in temperature between the top oil and the oil opposite the midpoint of the winding.

When first reading this rule, I felt that it would not be a safe rule to follow under heavy overload conditions. Further investigation, however, showed that in general it gives approximately correct results up to 150 per cent load. Nevertheless, I wish to qualify the statement about its being a safe rule to follow under all conditions. While

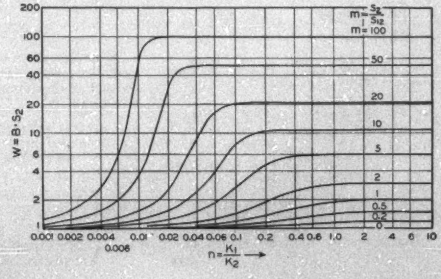

Figure 3

there is a certain relationship between the hot-spot temperature (and by hot spot I mean the difference between the maximum and the average winding temperatures) and the difference between the top oil and average oil temperature, factors other than the vertical temperature gradient affect the hot-spot temperature. One of these factors is current density in the copper coupled with the type of coil. One design may have a low current density, while another design has a high current density, yet both may have the same vertical oil gradient. The hot spots may be quite different in the two designs.

Another factor which the rule does not take into consideration is eddy-current losses which may not be uniformly distributed throughout the windings. Hot-spot temperatures will in general range from four or five degrees to ten degrees at normal load, the average probably being about seven degrees.

To see how the rule worked I took a typical case assuming that at normal load both the vertical oil gradient and the hot spot was seven degrees and found that at 125 per cent the rule was in error by approximately one degree (too low) and at 150 per cent the rule was in error approximately two degrees. This is close enough for all practical purposes.

On the other hand, if the hot spot is ten degrees at normal load and if the oil gradient is seven degrees, the rule, when used for 125 per cent load, would be in error by approximately five degrees. For 150 per cent load the error would be approximately eight degrees.

Since the rule covers only the vertical oil gradient factor that affects the hot-spot temperature, some care should be used in applying it in the field. It is easy to use if the user knows where the midpoint of the winding is by placing two thermometers on the outside of the tank—one at the top and one opposite the midpoint of the windings.

E. R. Thomas (Consolidated Edison Company of New York, Inc., New York): Mr. Kidder is to be congratulated in assembling together the factors to be considered in designing a transmission and distribution system. In obtaining equations 1 and 2, I assume that the derivation is based only upon a first contingency. As the number of units increases numerically to values greater than seven, as for example in the number of feeders being considered into an a-c network area, operating experience has shown that it is not uncommon to have a second contingency. I would like to ask the author whether, in his opinion, consideration should be given to second contingencies as affecting the average ratio of emergency load rating to normal rating when five or more units are being treated as a design combination.

F. H. Buller (General Electric Company, Schenectady, N. Y.): Mr. Kidder's primary purpose in presenting this paper appears to be the development of a method whereby the efficiency of utilization of existing plant may be increased. It is evident that considerable savings will result if it is feasible to carry increased loads on existing plant without overloading the aforesaid plant, and Mr. Kidder proposes to do this by what amounts to a material reduction in the amount of spare capacity available for use during emergencies.

This of course may result in the overloading of the plant when such emergencies actually arise; and Mr. Kidder has attempted to discover just about how much such overloads are likely to damage the existing cable.

He has shown in his paper that an overload of approximately 25 per cent, as compared with the maximum the cable should carry according to the AIEE temperature rules, will produce a very material increase in cable failures. Just what proportion of the $4^1/_2$ times figures which he gives can be directly attributed to the overload, cannot be established very clearly without knowing the peak loads which the cable was carrying in the ten years prior to the application of the long-time overload; but it must be a very considerable proportion of the figure in question.

Furthermore, Mr. Kidder points out that the failures which have resulted from the overload to date have almost all been sheath failures, caused by abrasion, or by bending of the cable in the manhole, particularly in the neighborhood of the duct mouth. We would expect his failure rate to increase materially with time rather than to decrease for two reasons. First, the lead sheath is materially weakened by high temperatures; and while three years of operation would probably weed out most of the weak spots in the lead sheath, and also the spots which are materially overstressed due to repeated bending as compared with the rest of the cable, it is possible that other weak spots have been developed in the sheath of the remaining cable due to the high temperatures, over and above those which have been found already; and these weak spots might never have existed had the cable been operated at a more normal temperature. Moreover, the bends in the manholes at the duct mouths are still in operation and will probably contribute just as much to the cable failure rate as they ever did, if the high-temperature load cycles are maintained.

Second, the wide range of temperature which is encountered on these cycles will unquestionably cause considerable sheath stretching and compound migration even where no sheath failure actually occurred. This means that void formation and ionization will be much more severe under these wide temperature cycles than would be the case if the AIEE limitations were respected. Now void formation and ionization usually tend to work quite gradually in producing cable failures, unless they are extremely severe from the beginning; and the fact that few failures have occurred as yet due to deterioration of the insulation does not mean that a large number of failures may not occur in the future due to these causes.

With regard to the mathematical derivation given in appendix *B* and covering a direct analysis of the two-chunk series thermal circuits, this analysis should probably work out pretty well for low-voltage cables and for cables of intermediate voltage.

Mr. Kidder points out that for overhead lines without insulation that one exponential term is sufficient. The reason for this is self-evident. The thermal resistance of the copper in such a circuit is exceedingly low, and there is no thermal resistance of the insulation; consequently all the thermal resistance is concentrated at the surface of the conductor, and is therefore actually a concentrated constant. The thermal capacity of the copper is the only thermal capacity involved in the heat dissipation of a system of this sort; and this is also for all practical purposes a concentrated constant. For two concentrated constants of this type a single exponential term is sufficient to represent the performance of the system mathematically, just as a single exponential term is sufficient in an electrical circuit containing concentrated resistance and capacitance.

If the insulation of the cable possesses appreciable thermal resistance and capacitance, however, we have a condition more nearly analogous to an electrical transmission line with distributed resistance and capacitance than to an electric circuit with concentrated constants. If the insulation is thin, it is possible to get reasonably accurate results by the use of a single exponential term to represent the entire cable, just as it is possible to represent a short transmission line with distributed constants by an equivalent circuit with concentrated constants. When the line gets long, or when the insulation gets thick, due allowance must be made for the fact that the constants are actually distributed. Various people, including the present writer, Mr. Church of Boston, and Mr. Miller of Chicago, have developed methods of doing this, but they all lead to a series of the general form of equation 3 in Mr. Kidder's paper and are quite complicated to apply in practice.

Incidentally, it should be noted that while successive terms of this series may converge quite rapidly toward zero the rate of change in each term does not converge nearly so rapidly. The use of distributed constants is therefore more important in problems involving the rate of change in temperature than in problems involving only the temperature itself. Experience indicates that these additional terms contribute materially to the rate of change in temperature for a period ranging from 5 minutes to 30 minutes after the application or dropping of load; this may affect the choice of the first point on the curve when analyzing time-temperature characteristics empirically in accordance with the method outlined in appendix *A* of Mr. Kidder's paper.

Mr. Kidder's second exponential term represents the cooling of the duct bank rather than that of the cable itself. Empirical information of this sort is extremely valuable, since it is very difficult to establish accurate mathematical equations for the cooling of a duct bank, and even if these equations were established, they would be of little value without the corresponding constants which can only be obtained empirically

One point on page 9 of Mr. Kidder's paper deserves a little further consideration. The writer is of the opinion that the threshold temperature T_0 should correspond to the sum of the ambient temperature, a steady-state rise caused by no-load losses, and the copper-temperature rise corresponding to the actual cable temperature at the time of the application of the overload rather than to the average daily copper loss. For the sake of safety, it would probably be advisable to assume that the overload occurs at or near the peak of the daily load cycle, since there is absolutely no guarantee that it shall not occur there, and since if the overload persists for as long as 24 hours it is absolutely certain to hit the peak of the daily load cycle at some time or other. Just how close it should be assumed to the peak is a matter for individual judgment; but certainly it would appear that it should be assumed to hit the cable at some temperature higher than that caused by the average daily losses.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): The author's approach to the problem of emergency ratings is interesting and should help in working out individual situations. However, to deduce generalities by means of probability laws requires many basic assumptions, some of which are not as yet well established. A more definite idea on the matter will come from more experience than just the three years of operation of 22 miles of cable at high temperatures cited by the author.

As pointed out in my paper ("Load Ratings of Cable," AIEE TRANSACTIONS, volume 58, 1939, pages 535-56) cable movement is not directly proportional to the square of the load, as was assumed by the author. Also, the rate of sheath troubles due to repeated daily bending in the manhole increases more rapidly than the first power of the magnitude of the cable movement. Furthermore, consideration of troubles should not only include the very important matter of sheath cracks but should also include the effect of high temperature on the insulation. This is especially important for some of the old high-voltage cables which have high dielectric losses. In some of the newer cables with thick insulation and oil fed at the joints, the effects of oil movement and sheath expansion must also be considered.

Any problem of loading including emergency loading must consider the economics for the system as a whole and the probable shortening of the life of the cable by the use of the higher temperatures.

A. H. Kidder: As should perhaps be expected for a paper of this sort, the discussion has been the more helpful because space did not permit a complete summary of the thoughts behind each of its observations. The points raised will be reviewed in the order in which they were brought out in the discussion.

V. M. Montsinger's comments on the author's suggestion for determining the approximate hot-spot temperature in a test on an oil-filled transformer, give a valuable indication of the probable magnitude of error. One to eight degrees centigrade at 150 per cent load is usually well within the over-all limits of error faced in making the practical compromises between accuracy and convenience that assume important proportions in most electric distribution problems. The author's method assumes that the vertical gradient in the winding is that of the oil alone. The method will, therefore, be in error to the extent that current density and eddy currents distort the vertical temperature gradient. The author's only claim for his method is that it provides a convenient means for approximate recognition, during a test at constant load, of the oil temperature gradient as an important component of the instantaneous hot-spot rise above average winding temperature.

E. R. Thomas's suggestion of the need for considering second contingencies should not be overlooked in dealing with large numbers of units n in equations 1 and 2. Their probable frequency of occurrence and their effect upon the curves in figure 3 of the paper may be evaluated approximately by the method outlined for first contingencies.

The severity of second-contingency loading naturally decreases to some extent when the number of units increases in a given design combination, while the likelihood of facing a second contingency increases with the number of units in the group.

F. H. Buller's thoughts on the possible causes of cable failures at high temperatures are quite similar to those which have occurred to the author from time to time. The instance of daily repeated high-temperature operation for 13.2-kv cables probably stands by itself in that it is much more severe than would be justified in any ordinary situation. Should the cable have been destroyed already, the station economies have been adequate to finance their replacement with new and larger cable. In the meantime, the experience being accumulated from such a full scale "accelerated life test" should in time answer approximately some of the interesting questions Mr. Buller has raised. The point that the author apparently failed to drive home, however, is that the cables cited in this instance have already done on at least 600 occasions what they would not be expected to do once in 100 years, if they were in the service represented by figure 3 of the paper which assumes strict observance of the AIEE limitations for all regular daily operations.

Mr. Buller's suggestion of the need for care in selecting the first point on the curve, when analyzing temperature-time characteristics empirically in accordance with the method of appendix A, raises an interesting point. Practical tabular calculations of the temperature transients which accompany normal or emergency load cycles do not require close agreement between the approximate and the true responses to load impulses of less than one-half hour duration on any assembly, because any possible effect of the circuit transient phenomena which are responsible for inaccuracies in this region will disappear within so very short a time in comparison to the duration of the load cycle under consideration. Emergencies which expire within one hour are almost always easiest and best treated as if the accompanying change in load losses were a rectangular impulse, for which the correct value of the response is directly proportional to the readings available on the true temperature-time characteristic.

The threshold temperature T_0 to which Mr. Buller refers was taken to be that level above or below which all temperature changes move because of deviations p from average copper losses in the cable in question. For safety the author prefers to start the tabular calculation of cable temperature at the time during the day when the normal values of p are changing from negative to positive quantities and to assume arbitrarily that the initial value of Θ_1 is zero, instead of the slightly negative value it would have at that time. To determine the approximate copper temperature at any other time it is then necessary only to use in the tabular calculation, the successive values of p which represent the anticipated time sequence of the instantaneous normal and emergency loads. In this way the calculation automatically will include proper recognition of the temperature attained by the time the emergency load is applied, and without need for redefining threshold temperature.

The maximum temperature is usually reached well toward the close of the day's normal heavy-load period and often within an hour or two after the peak. The highest emergency temperature, therefore, accompanies the state of emergency which begins at such a time that normal loads cannot be restored until just after the close of the peak load period. Often the time required for restoration of normal conditions is short enough so the emergency values of p need not be introduced in the tabular calculations prior to the time when Θ_1 may safely be assumed to be zero, as described above. The calculation may also begin at the same point on the load curve when it is desired to initiate emergency loading still earlier in the day, but should follow the normal values of p in reverse time sequence until the instant the emergency values of p will be applied and then proceed as usual. There is always the alternative of calculating one complete temperature curve for a normal day's load cycle, as a means for determining the time of maximum temperature and the conditions at the time it is then decided to start the emergency temperature calculations.

R. J. Woodrow's discussion contributes material which should be very helpful to those investigating the thermal characteristics of a number of different types of electrical equipment.

The data assembled by Mr. Halperin unmistakably support his comments on cable movement and its effect upon formation of sheath cracks. Each other possible cause of cable failure probably likewise has its own special characteristic, so that the over-all effect cannot be calculated deductively without first establishing all of these relationships. There is much to be gained from actively accumulating the facts needed to evaluate these relationships. In the meantime, however, cases of high-temperature operation such as cited by the author should give a fair, though empirical, indication of the approximate manner in which the over-all failure rate will follow moderate changes in the average-day loading of similar cables. Whether three years, or about 600 load cycles, of high-temperature experience can give a practical answer to the effect of temperatures upon the over-all failure rate, would appear to depend appreciably upon how many such high-temperature load cycles will be imposed upon the cable in question. It is the author's opinion that the AIEE limits are none too conservative for normal operating conditions, also that the possibility of infrequent temperature excesses during emergency load periods should give no cause for alarm.

Economical Loading of High-Voltage Cables Installed in Underground Subway Systems

E. R. THOMAS
MEMBER AIEE

AN underground high-voltage cable system is made up of two major components: the cable with its joints and accessories and the underground duct and manhole structure. The question of what size of conductor should be chosen for a particular transmission feeder or as a general standard for distribution feeders has confronted every planning group. The voltage for the system is frequently limited to one or to relatively few different values already existing as a system standard, deviations from which would result in increased cost to obtain terminal facilities. Frequently some underground facilities already exist and when only a few additional feeders are being considered it might seem logical to make use of them. A study of the annual charges may show, however, in some cases, that it will be more economical to build additional new underground facilities rather than further to congest already existing structures.

Table I. Cable Technical Data

Kilovolts	Number of Conductors	Size (Thousands of Circular Mils)	Insulation (Mils)	Maximum Copper Temperature (Deg C)	Dielectric Loss (Watts per Foot)	Thermal Constants (Deg C per Watt per Foot)		
						H_1	H_2	H_3
13.6	3	350	156 × 78	76.0	0.7	1.044	1.74 to 2.00	0.75
13.6	3	800	203	82.0	0.8	0.50	1.28 to 1.58	0.75
13.6	1	2500	203	82.0	0.3	0.732	1.55 to 1.84	0.75
27.0	3	350	297	74.4	1.3	0.80	1.51 to 1.67	0.75
27.0	3	500	297	74.4	1.3	0.792	1.36 to 1.63	0.75
27.0	1	1500	297	74.4	0.4	1.260	1.81 to 2.05	0.75
45.0	3	500	225	75.0	0.4	0.49	1.25 to 1.86	0.75
132.0	1	600	560	70.0	1.2	2.02	1.70 to 1.92	0.75

Since some of these questions arose in connection with the planning of tie feeders and network distribution feeders in New York City the cables in the voltage classes which were standard on that system were investigated.

Procedure

The ratings of cables installed in ducts[1] for a given load-cycle and ambient temperature will vary with the number of cables installed in the duct bank and with the size of the duct bank. A typical set of curves for one size of cable is shown in figure 1. It will be readily recognized that for any duct bank the load per cable which may be carried will become a decreasing function as more cables are installed in the bank. This causes the cost per unit of load for cables alone to increase as more cables are added due to the decrease in their rating. At the same time the proportional cost per cable for the duct bank is decreasing due to greater occupancy. The most economical combination will be determined by the ratio of costs between these two component

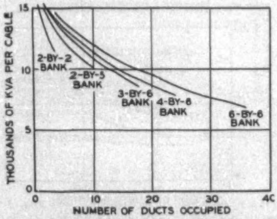

Figure 1. Ratings of cable in ducts

Three conductors, 800,000 circular mils, 13.6 kv

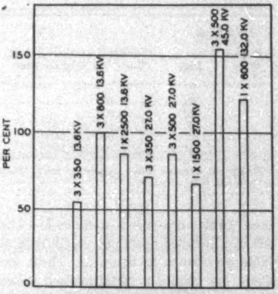

Figure 2. Relative installed cost of cable per cable foot

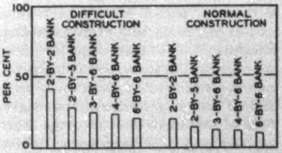

Figure 3. Relative cost of subway per duct foot

Ordinate scale same as figure 2

Paper number 39-69, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 30, 1938.

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The author greatefully acknowledges the help furnished by the various members of the electrical-engineering department in the preparation of this paper. He is particularly indebted to J. M. Comly in the supervision of the assembly of the data and to J. P. Neubauer and J. W. McCloskey for making the detailed calculations and numerous curves.

[1] For all numbered references, see list at end of paper.

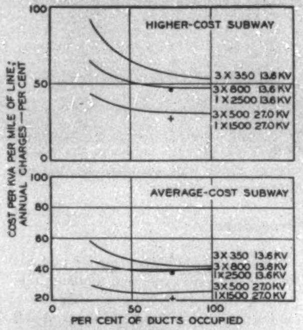

Figure 4. Comparative transmission cost of different cables installed in two-wide by two-high duct bank

parts and the respective changes in cost with increment number of cables.

The sizes of cables which have been selected in this study are typical of those found in practice and a number of them represent the largest size for that type and voltage which are suitable for installation in ducts four inches in diameter. Physical and electrical characteristics of these cables are given in table I. The relative installed costs per unit length of cable are shown in figure 2. These are based on common metal-market prices of copper 12⅝ cents per pound, lead 4 cents per pound.

Table II. Duct Bank Technical Data

Duct Bank		
Ducts Wide	Ducts High	Thermal Constants $H_4 + H_5$ (Deg C per Watt per Foot)
2	2	1.500
2	5	0.857
3	6	0.667 to 0.702*
4	6	0.600 to 0.653*
6	6	0.500 to 0.571*

* Weighted average duct constant used when inner ducts were occupied.

Duct-bank and manhole costs vary over a considerable range due to the character of the soil affecting excavation and to congestion with other subsurface structures. Two unit costs for these were used in this study, one a higher-cost subway system where excavations were difficult due to soil conditions and congestions of subsurface structures, the other an average-cost subway system where excavation conditions were normal. The relative values of these are shown in figure 3.

The annual carrying charges have been calculated on the basis of 14 per cent of the installed cable cost and 12 per cent of the subway cost. These values assume an average life of 25 years for the cable and 35 years for the subway system and include interest, insurance, taxes, depreciation, operation, and maintenance. The evaluation of losses was not included since the trend changes due to cost of wasted energy are small compared to the effect of losses in limiting cable ratings.

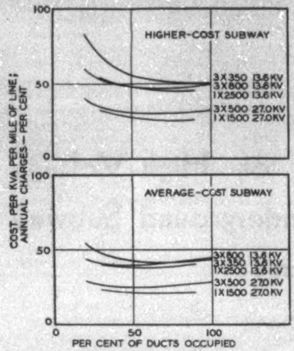

Figure 5. Comparative transmission cost of different cables installed in two-wide by five-high duct bank

Ordinate scale same as figure 4

The ratings of cables used in this study have been calculated for normal allowable copper temperatures, soil ambient of 15 degrees centigrade, cable and duct thermal constants as given in tables I and II, 40 per cent loss factor, and 85 per cent attainment factor. The loss factor is the ratio of average to peak loss in the duct bank. The attainment factor is used because the rise in temperature of the copper above the idle duct may be less when the peak load occurs periodically instead of continuously. The ratio of this actual rise to the ultimate for a continuous load is called the attainment factor. These values of loss factor and attainment factor were selected as being representative of the usual loading of duct banks and cables encountered in an underground distribution system. All

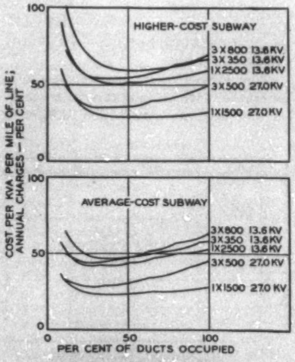

Figure 7. Comparative transmission cost of different cables installed in six-wide by six-high duct bank

Ordinate scale same as figure 4

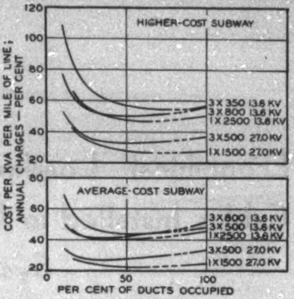

Figure 6. Comparative transmission cost of different cables installed in three-wide by six-high duct bank

Ordinate scale same as figure 4

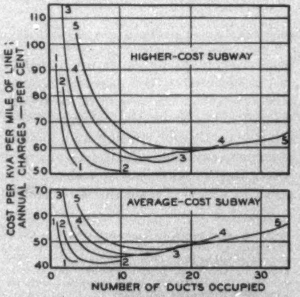

Figure 8. Comparative transmission cost, three-conductor 350,000-circular-mil 13.6-kv cable installed in different-size duct banks

Ordinate scale same as figure 4
Curve 1—Two-by-two bank
Curve 2—Two-by-five bank
Curve 3—Three-by-six bank
Curve 4—Four-by-six bank
Curve 5—Six-by-six bank

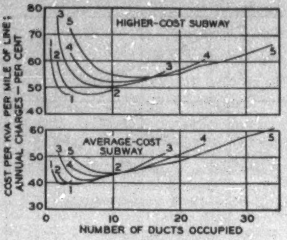

Figure 9. Comparative transmission cost, three-conductor 800,000-circular-mil 13.6-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

of the ratings assume three-phase 60-cycle operation. There may be special conditions of operation where a higher loss factor may be encountered. Since the loss factor of all the cables in a bank principally affects the temperature rise of the bank, these special conditions may indicate a somewhat lower rate of occupancy of the bank than with the load factor studied.

Results of Calculations

The annual charges for several types and voltage ratings of cable installed in common-size duct banks are shown in figures 4, 5, 6, and 7, showing relative values both for the higher subway costs and for average subway costs. These data show in general that the largest-size cable for a given voltage class which it is practical to install in a duct results in the most economical high-voltage transmission. Large single conductor cables are somewhat more economical than three-conductor cable but no account has been taken of the greater voltage regulation which obtains with single-conductor cable and which frequently may make it unsuitable for parallel operation with three-conductor cable.

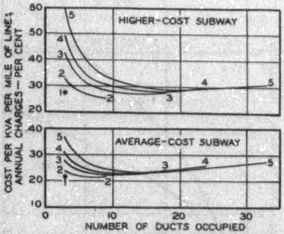

Figure 12. Comparative transmission cost, single-conductor 1,500,000-circular-mil 27-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

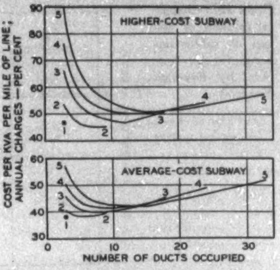

Figure 10. Comparative transmission cost, single-conductor 2,500,000-circular-mil 13.6-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

When an underground high-voltage cable system is made up of one standard size of cable it becomes of interest to know when it is more economical to build new underground facilities to take care of additional cables as compared to installing them in existing facilities. The relative annual charges for several different-size duct banks are shown in figures 8, 9, 10, 11, and 12. As an illustration, it will be noted in figure 9 that if six three-conductor 800,000-circular-mil 13.6-kv cables are already installed in a two-by-five duct bank of average-cost subway and there is need for adding two, three, or four additional cables that it is more economical to build a new two-by-two duct bank to take care of these additional cables. However, if the duct bank were of higher-cost subway it would be more economical to continue to utilize the existing facilities in the two-by-five duct bank for all except the addition of four cables. It will be noted from the various data for duct banks in these figures that two-wide bank construction results in lower over-all annual charges on underground high-voltage cable systems than any of the wider bank widths. This is a somewhat fortunate condition as racking facilities in manholes are greatly improved

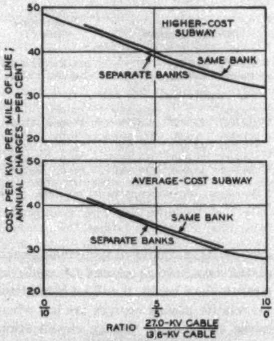

Figure 13. Comparison of transmission cost, 27-kv and 13.6-kv cables co-occupying and separately occupying two-wide by five-high duct bank

Ordinate scale same as figure 4

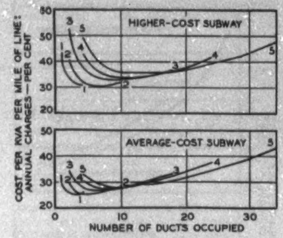

Figure 11. Comparative transmission cost, three-conductor 500,000-circular-mil 27-kv cable installed in different-size duct banks

Ordinate scale same as figure 4

For curve designations, see subcaption of figure 8

Table III. Effect of Load Division Between Cables in a Two-by-Five Duct Bank Containing Five Three-Conductor 500,000-Circular-Mil 27-Kv Cables and Five Three-Conductor 800,000-Circular-Mil 13.6-Kv Cables

Load (Kilovolt-Amperes)			Copper Temperature (Deg C)	
27-Kv Cables	13.6-Kv Cables	Total	27-Kv Cables	13.6-Kv Cables
90,000	0	90,000	74.4	
74,000	47,000	121,000	74.4	72.2
72,000	50,000	122,000	74.4	75.8
70,000	52,500	122,500	74.4	79.0
68,000	55,000	123,000*	74.4	82.0
65,000	55,500	120,500	72.9	82.0
60,000	56,500	116,500	68.9	82.0
55,000	57,500	112,500	65.2	82.0
0	63,000	63,000		82.0

* Maximum kilovolt-amperes is obtained when all cables are at their normal copper temperature.

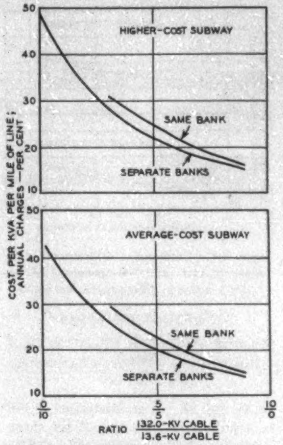

Figure 14. Comparison of transmission cost, 132-kv and 13.6-kv cables co-occupying and separately occupying two-wide by five-high duct bank

Ordinate scale same as figure 4

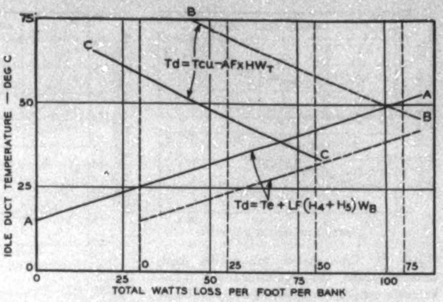

Figure 15. Graphical solution of ratings of cables co-occupying a two-wide by five-high duct bank

Solid line—Five three-conductor 500,000-circular-mil 27-kv cables

Dashed line—Five three-conductor 800,000-circular-mil 13.6-kv cables

when there is no crossing of cables over duct positions as frequently occurs when duct-bank widths of greater than two are used.

Usually more than one high-voltage class of cable exists on a system and it has been common practice to install cables operating on these different voltages in the same underground duct facilities. These cables have different maximum allowable copper temperatures and different thermal properties. Different voltage classes of cable which are not widely different in their thermal properties and which are installed in two-wide duct banks usually will transmit the maximum kilovolt-amperes through the bank when the load is so proportioned between the two classes of cable that they both reach their allowable copper temperature at the same time. A typical set of data is shown in table III for variously apportioning load on five three-conductor 800,000-circular-mil 13.6-kv cables and five three-conductor 500,000-circular-mil 27-kv cables installed in a two-wide by five-high duct bank. Similar data are shown in table IV for six single-conductor 600,000-circular-mil 132-kv cables and four three-conductor 800,000-circular-mil 13.6-kv cables. This illustration, which deals with cables of widely different voltage classification, shows the marked reduction in capacity which obtains due to occupancy of the same subway system. In order to study the limiting condition when two different voltage classes of cable are installed in a common duct bank we have found that the graphic solution as described in appendix I is very helpful.

Under a given set of conditions the maximum load which may be transmitted on both types of cable through the duct bank may not necessarily be the most economical manner in which to transmit the load. If we consider a system made up of a sufficiently large number of cables of two voltage classes and compare the relative annual charges of installing those cables as a mixed system in common duct banks as compared to installing each of the two voltage classes of cable in separate duct banks, it will be found that the relative annual charges are less when cables of the same voltage classification only are installed in common duct banks. The comparison between three-conductor 800,000-circular-mil 13.6-kv cables and three-conductor 500,000-circular-mil 27-kv cables is shown in figure 13 and a similar comparison between three-conductor 800,000-circular-mil 13.6-kv cables and single-conductor 600,000-circular-mil 132-kv cables is shown in figure 14. However, when the increment number of cables to be added in an existing system of other-voltage cable is small, the most economical arrangement will have to be studied as a specific case.

Table IV. Effect of Load Division Between Cables in a Two-by-Five Bank Containing Six Single-Conductor 600,000-Circular-Mil 132-Kv Cables and Four Three-Conductor 800,000-Circular-Mil 13.6-Kv Cables

Load (Kilovolt-Amperes)			Copper Temperature (Deg C)	
132-Kv Cables	13.6-Kv Cables	Total	132-Kv Cables	13.6-Kv Cables
279,000	0	279,000*	70.0	
222,000	43,000	265,000	70.0	72.0
217,000	45,000	262,000	70.0	77.6
211,000	48,000	259,000	70.0	82.0
192,000	48,300	240,300	66.0	82.0
168,000	49,000	217,000	62.0	82.0

* Maximum kilovolt-amperes is obtained when there is no load on the 13.6-kv cables.

Conclusion

1. Increased operating economies will obtain by properly co-ordinating the selection and loading of cables in specific duct banks.

2. The use of duct banks having more than two-wide arrangements in general are not economical.

3. The construction of new underground facilities frequently may result in a more economical system as a cable system is being expanded, than would an increase in congestion of existing facilities.

4. In general, it is not economical to install cables of widely different voltage classification in a common duct bank.

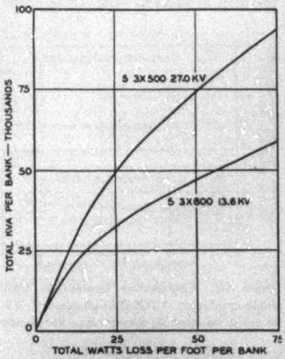

Figure 16. Heat loss in two-wide by five-high duct bank

Appendix I. Graphical Method of Determining the Effects of Load Division on Kilovolt-Amperes for a Duct Bank Containing Cables of Two Voltage Classes

The effects of load division on kilovolt-amperes for a duct bank containing cables of two voltage classes may be determined graphically. This method requires curves of idle duct temperature versus watts loss, the curves for different cables being plotted on separate sheets, and curves of kilovolt-amperes versus watts for each cable.

By superimposing one set of curves on another, the watts loss for each cable at rated copper temperature, or the watts loss on one cable if the loss on the other is known, may be determined. The watts may be converted to kilovolt-amperes by a kilovolt-amperes-versus-watts curve. It is possible also to determine the copper temperature of the cables that are operating below their rated copper temperature.

Two idle-duct temperature curves are needed for each cable, one calculated by subtracting the thermal drop from copper to idle duct from the rated copper temperature (equation 1) and one calculated by adding the thermal drop from idle duct to base earth to the earth ambient (equation 2).

$$T_d = T_{cu} - AF \times (H_1 + H_2 + H_3)W_t \quad (1)$$

T_d = idle duct temperature in degrees centigrade

T_{cu} = copper temperature in degrees centigrade

H_1 = thermal constant for insulation (Simmons)

$= \dfrac{0.00522\ RG_1}{N}$

H_2 = thermal constant, sheath to duct wall

$= \dfrac{4.9}{D(1 + 0.013W_t)}$

H_3 = thermal constant, duct wall to idle duct, 0.75 (Kirke). H_1, H_2, and H_3 are in degrees centigrade per watt per duct foot

AF = attainment factor for rise of copper above idle duct

R = thermal resistivity of insulation in degrees centigrade per watt per centimeter cube

G_1 = geometric factor (Simmons)

W_t = total watts loss per duct per duct foot

D = outside diameter of cable—inches

N = number of conductors

$$T_d = T_e + LF \times (H_4 + H_5)\ W_t \quad (2)$$

T_d = duct temperature in degrees centigrade

LF = loss factor for duct bank

$H_4 + H_5$ = thermal constant for duct bank in degrees centigrade per watt per duct foot (Kirke)

W_t = total watts loss per duct foot for the duct bank

T_e = earth temperature in degrees centigrade

In equation 1 values for the total watts loss per foot of cable are assumed, and the idle duct temperature is calculated. If there are five cables of one type in the bank, this idle-duct temperature is plotted against five times the assumed watts per cable. Values of total watts per foot per bank are assumed for equation 2.

The application of the graphical method is shown in the following example. Assuming a two-by-five bank containing five three-conductor 500,000-circular-mil 27-kv cables having a total loss of 30 watts per foot and five three-conductor 800,000-circular-mil 13.6-kv cables, the problem is to find the kilovolt-amperes which can be transmitted through the bank and the copper temperature of the 27-kv cables if the 13.6-kv cables are at rated copper temperature.

Idle-duct temperature curves are drawn for each cable and the 13.6-kv curves are placed on the 27-kv curves so that the zero-watt 13.6-kv ordinate coincides with the 30-watt 27-kv ordinate. This is shown in figure 15 where the curves for each cable are drawn on one sheet to simplify the explanation in preference to using separate sheets. The 13.6-kv cables will be at rated copper temperature where the cable and bank duct temperature curves *AA* and *BB* intersect. The watts loss for the five 13.6-kv cables is found by projecting this point to the 13.6-kv watt scale to be 71 watts. Using the kilovolt-amperes-watts curves (figure 16) for the five 27-kv and five 13.6-kv cables the kilovolt-amperes per each set of cables is found and added to get the bank kilovolt-amperes. By repeating this process for various assumed watts loss on each set of cables the correct load division for maximum kilovolt-amperes may be found.

The kilovolt-amperes-watts curves are calculated using resistance and dielectric loss values at rated copper temperature. This will introduce some error when one type of cable is operating below the rated copper temperature.

The copper temperature of the 27-kv cables in the example given is found in the following manner: The idle-duct temperature of the bank is indicated by the intersection of *AA* and *BB* to be 49.5 degrees centigrade. However, at 30 watts per foot on the five 27-kv cables the thermal drop from rated copper to idle duct indicates a required duct temperature of 58.5 degrees centigrade as shown by the intersection of *CC* and the zero-watt 13.6-kv ordinate. The difference between the actual duct temperature and this required duct temperature, 9.0 degrees centigrade, subtracted from the 27-kv rated copper temperature, 74.4 degrees centigrade, gives a copper temperature of 65.4 degrees centigrade for the 27-kv cables.

Bibliography

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2. Reduction of Sheath Losses in Single-Conductor Cables, **Searing** and **Kirke**. *Electrical World*, October 6, 1928.

3. Calculation of the Electrical Problems of Underground Cables, **Simmons**. *The Electric Journal*, May to November 1932.

Discussion

L. I. Komives (nonmember; The Detroit Edison Company, Detroit, Mich.): The data presented in table IV, concerning the effect of load division between cables of widely different voltage ranges, certainly merits considerable interest. It is, of course, understood that the cables described were compared on the basis of normal temperature limits. However, as oil-filled cables can be operated at higher temperatures than solid-type cables with the same factor of safety, this table should be used only when all other factors are taken into consideration.

J. M. Comly (Consolidated Edison Company of New York, Inc., New York): In Mr. Thomas' paper, figures 8, 9, 10, 11, and 12 show curves of relative annual cost per kilovolt-ampere for various combinations of cables and ducts. It may be of interest to mention an investigation of the effect on these curves of changes in copper temperature and loss factor.

The normal kilovolt-amperes used in calculating cost per kilovolt-ampere might be reduced in practice by the necessity of carrying contingency loads on some of the cables without exceeding their normal allowable copper temperature. Thus a reduction in copper temperature for normal operation would be necessary. Similarly, loads of different types would result in different daily loss factors. Obviously if changes in these constants materially affect the relative costs for different cable and duct combinations, the curves presented in the paper will not show the relative economy of these different combinations except for very special cases.

A study of the effect of changing copper temperature for the special case of three-conductor 800,000-circular-mil 13.6-kv cable indicates that changes in copper temperature from the normal allowable value of 82 degrees centigrade to 50 degrees centigrade and even to 30 degrees centigrade make no appreciable change either in the point of lowest cost nor in the relative costs in different-size duct banks.

Mr. Thomas' data are based on an assumed daily loss factor of 40 per cent. It seems unlikely that the load of large-capacity distribution feeders would vary its character more than would be indicated by a variation in loss factor from 30 per cent to 50 per cent. In this range, the changes in location of the point of minimum cost are negligible for three-conductor 800,000-circular-mil 13.6-kv cable operating in a two-by-five duct bank. If the range is from 20 per cent to 100 per cent, the point of minimum cost in the curves of cost per kilovolt-ampere versus number of ducts loaded may vary as much as two ducts.

In conclusion, it appears from a study of the effects of changes in these two essential constants that the relative costs shown by Mr. Thomas' curves would be reasonably accurate for all practical operating conditions within the range of subway and cable costs shown.

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York): In figure 1 of his paper, Mr. Thomas has used a figure of 1.3 watts per foot for the dielectric loss on 27-kv three-conductor cables. This is probably a representative value but it is by no means a maximum. In the course of some laboratory studies a few years ago, it was found that some cables, when subjected to overload-cycle aging tests, experienced a very large increase in dielectric loss. This suggested the desirability of examining samples removed from service and assumed to be typical of cable installed on the system. A disturbing number showed dielectric losses at rated voltage and 80 degrees centigrade of more than 5 watts per foot with isolated cases going to more than 10 watts per foot. Careful study of the operating records failed to disclose any evidence indicating previous high operating temperatures, nor was there any clue to the cause of the large increase. Some day, we hope to know the answer but until we do, it will be necessary to use some caution in calculating possible maximum temperatures or else be willing to accept the inevitable failure that will occur if a piece of this deteriorated cable happens to come at a point where the duct temperatures are somewhat higher than average.

F. H. Buller (General Electric Company, Schenectady, N. Y.): The writer wishes to endorse Mr. Thomas' conclusions most heartily.

The first conclusion is self-evident.

With regard to the second conclusion, the company with which the writer is associated has always contended that the most economical type of duct construction is a bank two ducts wide and deep enough to accommodate as many cables as may be required. Mr. Thomas has brought out in his paper that the use of duct banks with inside ducts is seldom economical, thus bearing out this contention.

With regard to the third conclusion, the company with which the writer is associated has always contended that the use of more than 12 cables in a duct bank is seldom economical. Mr. Thomas' curves, particularly figure 9, indicate that it is seldom economical to use more than 10 cables in a duct bank, even with a comparatively high-cost subway. While the inclusion of the cost of losses might increase this number of cables somewhat on the basis of annual charges, the writer is in full agreement with Mr. Thomas that a highly congested duct system is liable to be quite uneconomical in the matter of current-carrying capacity.

With regard to the fourth conclusion, this also has been the contention of the company with which the writer is associated, since the sacrifice of load on the higher-voltage cable may quite often exceed the total load carried by the low-voltage cable, and even if it does not, and the total load carried by the duct bank is increased by the installation of the low-voltage cable, nevertheless there may well be a definite sacrifice in economy by following this procedure.

With regard to appendix I, Mr. Thomas has followed the Kirke method of calculating cable ratings. This method is somewhat more complicated than the usual method adopted by the Edison Electric Institute and the Insulated Power Cable Engineers Association in preparing current loading tables, but as used by Mr. Thomas would not give widely differing results.

On the one hand, Mr. Thomas uses an ambient temperature for New York City of 15 degrees centigrade, whereas the usual method would be to base calculations on an ambient of not less than 20 degrees centigrade, on the basis of the curve of earth temperature shown in figure 14 of Mr. Kirke's paper (AIEE JOURNAL, October 1930, page 855). On the other hand, the usual method omits the heating constant H_1 altogether, or rather includes it in the duct constant D. In general, the usual duct-heating coefficient tends to be somewhat lower than Mr. Kirke's values, especially for small numbers of cables in the duct bank, and this offsets the difference in ambient. The resulting differences in current carrying capacity are shown in tables I and II of this discussion.

It will be seen that the usual method gives somewhat higher current-carrying capacity than the Kirke-Thomas method for small numbers of cables in the duct bank, but that the discrepancies involved are not very large.

With regard to the graphical method for evaluating current-carrying capacity when two different types of cables are installed in the same duct bank, the plan which Mr. Thomas outlines in his paper should work very well if the duct bank contains a group of cables which are not fully loaded, and it is desired to find how much load an additional group of cables can carry without exceeding permissible operating temperatures. It appears, however, to be limited to two types of cables, and involves a considerable amount of cut-and-try if it is desired to determine what load each group of cables should carry in order to operate at its maximum permissible copper temperature.

The writer has developed a graphical method which can be used quite readily for any number of different types of cable, and which will give the load which each cable can carry when operating at its maximum permissible copper temperature, or any other arbitrarily selected copper temperature, without resorting to cut-and-try. Since the method is very simple to apply, it might be well to outline it here.

Table I. Cables and Duct Arrangements Covered in Mr. Thomas' Paper

Number of Cables in Duct Bank	*Ratio
4	108
10	101
18	96

* Ratio of $\frac{\text{current rating by usual method}}{\text{current rating by Kirke-Thomas method}}$

Table II. Usual Standard Groupings

Number of Cables in Duct Bank	Ratio
1	1.12
3	1.09
6	1.05
9	1.02
12	0.99
*15	0.97
*18	0.96

* These groupings are not standard, but are obtained by extrapolation using the standard method

LIST OF SYMBOLS

T_c = copper temperature, degrees centigrade, equally loaded cables
T_{cn} = copper temperature of cable n (unequally loaded cables) in degrees centigrade
T_0 = earth ambient, degrees centigrade
T_d = duct temperature rise in degrees centigrade
W = loss per cable, watts per foot (for similar equally loaded cables)
W_n = loss in cable n in watts per foot (for unequally loaded cables)
L = loss factor, as a decimal (for equally loaded cables)
L_n = loss factor, as a decimal in cable n (for unequally loaded cables)
N = number of cables in duct bank
H = duct heating constant
D = "duct constant" for equally loaded cables = HLN
R_{in} = thermal resistance of insulation, for cable n
R_{sn} = thermal resistance of surface for cable n
R_i = thermal resistance of insulation (similar equally loaded cables)
R_s = thermal resistance of surface (similar equally loaded cables)

The usual equation for equally loaded cables is:

$$\begin{aligned} T_c - T_0 &= W(R_i + R_s + D) \\ &= W(R_i + R_s) + WLNH \end{aligned}$$

Or, since $WD = WLNH = T_d$

$$T_c - T_0 = W(R_i + R_s) + T_d \qquad (1)$$

If there are N equally loaded cables:

$$NWL = (WL + WL + WL \ldots) \text{ to } N \text{ terms}$$

which may be written

$$\sum_{n=1}^{n=N} nWL$$

Now, suppose that the N cables are unequally loaded but the average value of the product (watts loss times loss factor) is equal to WL. The duct temperature T_d will be the same as before.

Then

$$NWL = \sum_{n=1}^{n=N} nW_nL_n$$

for the unequally loaded case.

So that the duct temperature, T_d, which is the same as before, will be

$$T_d = HNWL = H \sum_{n=1}^{n=N} nW_nL_n \qquad (2)$$

Also, from equation 1

$$T_{cn} - T_0 = W_n(R_{in} + R_{sn}) + T_d \qquad (3)$$

or

$$W_n = \frac{T_{cn} - T_0 - T_d}{R_{in} + R_{sn}} \qquad (4)$$

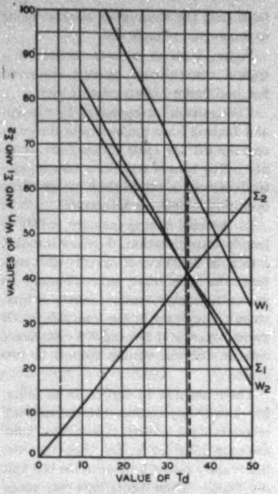

Figure 1. Illustrating graphical method of assigning current ratings to dissimilar unequally loaded cables in the same duct bank

Also, from equation 2

$$\sum_{n=1}^{n=N} nW_nL_n = \frac{T_d}{H} \qquad (5)$$

And, finally, from equations 3 and 2,

$$T_{cn} - T_0 = W_n(R_{in} + R_{sn}) + H\sum_{n=1}^{n=N} nW_nL_n \qquad (6)$$

Assuming arbitrarily chosen values of T_d, plot W_n against T_d from equation 4 for each cable. The quantity W_nL_n should be calculated for each value of W_n, and the summation

$$\sum_{n=1}^{n=N} nW_nL_n$$

should be computed and plotted for each value of T_d. Call this summation $\sum_1$.

Next, compute

$$\sum_{n=1}^{n=N} nW_nL_n$$

from equation 5. Call this quantity $\sum_2$ and plot it against T_d. Where the curves of $\sum_1$ and $\sum_2$ intersect, we have the value of T_d which satisfies both equations 3 and 5.

By dropping a perpendicular from this intersection and noting where it intersects the curves of W_n against T_d, we can find the values of W_n for each cable corresponding to the proper value of T_d, and we can determine the current loading to give these values of W in the usual way.

Since all the curves involved are straight lines, only two points need be calculated on each curve. This makes the arithmetical work very simple, even where several different types of cable are involved.

EXAMPLE

Two-by-five duct bank—five three-conductor 500,000-circular-mil 27-kv cables, five three-conductor 800,000-circular-mil 13.6-kv cables.

Here we will take $R_{in} + R_{sn}$ as equal to Mr. Thomas' values of $(H_1 + H_2 + H_3) \times 0.85$ and $H = H_4 + H_5$. The usual EEI-IPCEA values could, of course, just as well be used. The loss factor L_n will be taken as 40 per cent or 0.4.

	Cable		
	27 Kv, 500,000 Circular Mils	13.6 Kv, 800,000 Circular Mils	Notes
$R_{in} + R_{sn}$	2.902	2.53	Using lowest value of H_2
T_{cn}	74.4	82	
W_d	1.3	0.8	Dielectric loss
L_n	0.4	0.4	
T_0	0.15	15	
H	0.857	0.857	

Since there are two groups of five similar cables each, it will only be necessary to plot two curves of watts loss, since the cables in each group will presumably be loaded equally, though the two groups will carry different loads.

The values of W_1 and W_2 and also $\sum_1$ and $\sum_2$ are plotted in figure 1 of this discussion. $\sum_1$ and $\sum_2$ intersect at T_d = 35.4 degrees centigrade (duct temperature 50.4 degrees centigrade). W_1 = 62.5 or 12.5 watts per foot of cable. W_2 = 41.5, or 8.3 watts per foot of cable. $W_1 + W_2$ = 104.0. Mr. Thomas' method gives W_1 = 64.5 and W_2 = 44.0, or a total of 108.5 watts per foot of duct bank. The difference is probably due to slide-rule work or graphical discrepancies.

Both this method and Mr. Thomas' method are based on the assumption that the duct heating coefficient is the same for all the cables in the duct bank. This assumption may not hold very closely if some cables are installed in inside ducts. A conservative plan would be to assume a duct heating coefficient for all the cables corresponding to inside ducts; or alternatively, an average duct heating coefficient for the entire duct bank may be used, and the rating for the cables in the inside duct subsequently reduced somewhat, to take care of the higher duct-heating coefficient, which actually obtains in these ducts.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper was interesting and stimulating. However, some of the conclusions apparently do not apply for Chicago conditions.

As a result of detailed cost studies, we have found that the cost of duct banks that are two-wide is higher per outside duct than for a duct bank that is three-wide, prorating the cost of manholes in each case. This applies to a two-by-four duct bank as against a three-by-three duct bank, as well as to a two-by-five duct bank as against a three-by-four duct bank. Generally, the width of excavation in Chicago is about the same for a three-wide conduit as for a two-wide conduit, due to the minimum requirements for working room, while the need for going to extra depth with two-wide conduits results in more labor and interference with other substructures. Another advantage to us in using the three-wide conduits, where there are enough cables involved to justify such conduits, is that the center ducts may be used for signal or relay cables without interfering with the use of the other ducts for power cables.

Our studies of heating constants indicate that a 9-duct conduit has a very slight advantage over the 8-duct conduit, and this applies also for 12-duct conduit as compared to 10-duct conduit, assuming that only the outside ducts are occupied for power cables.

In general, it seems there must be some differences in conditions between New York and Chicago in this matter.

Regarding the author's conclusion 3, we have been endeavoring to limit the size of our conduits to 12 ducts, or to 16 ducts in special cases, for at least the past 12 years.

In general, we have not found it uneconomical to install cables of different voltage classifications in a common conduit. If one were to lay out a brand new system and install all the cables in a given city in a short time, then it certainly would be most economical to install cables of only one voltage classification in a given conduit as far as possible. Our policy is to avoid as much as possible the installation of conduits in a street for five years after it has been repaved. These and other factors affecting system planning plus the fact that the incremental cost of the larger conduits is relatively small mean that many ducts are unoccupied. It therefore becomes necessary to use these ducts as much as possible for new circuits, regardless of the voltage.

It so happens, however, that this practice works out fairly well because it is frequently necessary to limit maximum conduit temperatures to 50 degrees centigrade or less, in order to avoid drying of the soil which would cause conduit and cable temperatures to become excessive. By limiting the normal conduit temperatures to 45 or 50 degrees centigrade, the resulting copper temperatures are reasonable in almost all cases for all types of cable that may happen to be in a given conduit. In connection with emergency loading, only one cable, or three cables in the case of a single-conductor, three-phase line, is subjected to an unusual load and is generating an unusual amount of heat. The emergency lasts just one day, except that it might last two days for an oil-filled line, but during that short time the increase in the temperature of the conduit is only a few degrees. At the same time the cable temperature may safely go to 90 or 100 degrees centigrade or so, that is, to the limit set by the insulation for emergency operation.

E. R. Thomas: It is gratifying to the author that the presentation of this paper should have aroused the interest indicated by the pertinent comments and discussions which have been presented.

W. F. Davidson calls attention to the

Ignitrons for the Transportation Industry

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THE USE of the multianode metal-tank mercury-arc rectifier is well established in the transportation industry. At the present time there are approximately 500,000 kw of these rectifiers in operation on railway properties in America. The units vary in size from 500 to 3,000 kw and range from 500 to 3,000 volts direct current.

The mercury-arc rectifier has replaced rotating conversion equipment for transportation service largely because of its higher efficiency, particularly at low loads, lower installed cost, increased reliability, less maintenance, simpler control, no noise or vibration, ability to carry short-time heavy load swings, and its instant availability for service.

Although, for these reasons, the conventional rectifier amply justifies itself, it has been realized that it does not take full advantage of the possibilities inherent in the mercury-vapor arc. The voltage drop in a simple, high-current mercury arc is less than ten volts and the reverse voltage that such a structure will withstand is many times the value encountered in the transportation field. However, in some as yet not fully understood manner, a simple mercury-arc arrangement has been found to break down occasionally in the reverse direction or arc back, at voltages in the range required. In the conventional multianode rectifier, the anodes are removed from exposure to the cathode and are surrounded by shields and grids in order to provide the necessary reliability. This complication of the structure results in an arc drop varying from 20 to 30 volts, depending upon the tank size, the amount of increase being in proportion to tank size.

Figure 1 shows a cross section of a typical conventional mercury-arc rectifier, and figure 2 is an external view.

The ignitron, as conceived by Slepian and Ludwig,[1] is a major step in the progress toward the ideal mercury-arc rectifier. This type of rectifier is now a practical device as established by excellent operation in commercial service.

In the coal-mining industry, which is largely transportation, there are installed a total of 6,000 kw of mercury-arc rectifiers. Of this total, approximately 50 per cent, or eight units, consists of ignitrons. They vary in size from 300 to 400 kw and operate at 275 and 600 volts direct current. Service ranges upward to two years.

There are now in service or on order, for railway application, two mercury-arc rectifiers of the ignitron type. These units are each 3,000 kw in size, they have the heavy-duty rating, and are in the 600-volt d-c class. These units have an anode rating equal to that of the largest multianode rectifier now built for railway service. One of these units is for service on the subway system of the Board of Transportation of the City of New York, and the other is for service on the main-line electrification of the New York Central Railroad.

Principle of the Ignitron

A cathode spot is the essential element of an arc. With a cathode spot, in a low-pressure gas chamber, any anode will pick up current when a positive potential is applied. Since a cathode spot cannot be created reliably in a low-pressure gas by the application of high voltage, it is necessary to start a rectifier by some other means. In the conventional rectifier, this is done by drawing an arc by separating electrodes at the cathode surface. The cathode spot thus formed is maintained continuously by a small current to an auxiliary anode. This arc current results in ionized gas. It is obvious that economy of equipment and auxiliary power is effected by placing several power anodes in the same tank. This is the reason for the multianode rectifier. The continuous presence of ionized gas, which includes the time that the anodes are bearing reverse voltage, greatly facilitates the formation of a cathode spot on an anode which is the principal reason for the shields and grids as previously mentioned.

The ignitron principle provides a method of starting an arc reliably in a few microseconds. This method is ame-

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Paper number 39-39, recommended by the AIEE committee on transportation, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 23, 1938; made available for preprinting January 3, 1939.

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1. For all numbered references, see list at end of paper.

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fact that the dielectric loss of cables withdrawn from service has been found to exceed the average values used in calculating the ratings for the paper in many instances. This is a fact which should be considered in any investigation of a specific duct loading problem. Its effect on the results will be to indicate that the greatest economy can be obtained with smaller duct structures when and if such high-loss cables are used.

Mr. Halperin's comment on conditions in Chicago is very interesting. It would seem that where a requirement for duct space for supervisory or signal cables exists, a very real economy might be obtained by the use of the three-wide rather than the two-wide duct structure.

Mr. Buller's general agreement with the conclusions of the paper is appreciated. The variations he mentions between the ratings obtained by the EEI method and the Kirke method are recognized. However, it is felt that a somewhat greater complication of the Kirke method gives greater accuracy than the EEI method does.

Mr. Buller's comment on the graphical method given in the paper suggests that it involves the use of cut and try. By the superposition of several curves drawn on different sheets of transparent material, it is possible to determine accurately the load which each of several cables will carry when all are operating at their maximum permissible copper temperature. With some loss of accuracy, it is possible to use the same curves to determine the load each of several types of cable will carry when operating at temperatures other than the maximum permissible temperatures. The method outlined by Mr. Buller does not appear to be as flexible as the one shown in my paper since one set of curves is good only for one solution of one set of conditions. A simultaneous solution of two equations would accomplish the result with less labor. The W_1 curves shown in figure 1 of Mr. Buller's comments are straight because his R_3 is a constant. In my equation H_3 replaces the R_3 in Mr. Buller's equation and H_3 varies with the watts lost.

The supplemental data presented by Mr. Comly are interesting since they indicate the probability that the conclusions given in my paper would not be affected materially by normal changes in type of load or average loading.

Mr. Komives' comment on the permissible temperature limit for oil-filled cable seems irrelevant since the figures in table IV are based on the allowable temperature for each of the cables indicated. For the 132-kv cable, the values are those for oil-filled insulation.

In closing, let me re-emphasize the fact brought out by the paper that a considerable saving may be realized by careful analysis of the economies involved in duct-bank size and loading in advance of construction. I am indebted to the discussers for their interest and hope that more analyses of the subject may be made as a result of this discussion.

nable to synchronous application. With such a system of ignition, the arc may be permitted to extinguish completely at the end of each conducting period. This leaves the anode surrounded by deionized gas during the time that it is bearing reverse voltage, except for a few microseconds following the conducting period, which is the transition time required for deionization to take place. Of course, in order to take advantage of this method of operation, each anode with its own cathode is mounted in a separate chamber, thus removing it from the influence of other anodes when they are conducting current. This permits the reduction of the shields and grids to the minimum necessary to take care of the transition period and permits the location of the anode close to the cathode.

The way in which an arc is started by the ignitron principle is described in detail by Slepian.[3] Briefly, when a high-resistance rod is immersed in mercury and a current of sufficient magnitude is passed through the rod to the mercury, the potential gradient set up at the junction between the two materials is sufficient to initiate a cathode spot. The magnitude of current necessary is dependent upon the resistivity of the material used for the rod. It has been found that rods or ignitors of boron carbide or silicon carbide, which are the materials most in use today, have a resistivity such that a current of less than 20 amperes and approximately 100 volts are required. These are convenient values for practical operation. The excitation circuits will be described in a later section.

Construction of the Ignitron

In general, the type of construction used for large power ignitrons is the same as that used for conventional rectifiers. The anode assembly, consisting of a vacuum-tight insulating bushing, anode head, and the current-conducting parts is identical. The tanks are made from specially selected sheet steel with the seams welded vacuum tight. The same type of vacuum-tight gaskets are used for the cover-plate seal and for all separable connections in the vacuum pumping system. The vacuum pumping equipment is identical. The essential differences lie in the separate anode with associated cathode vacuum chamber, the vacuum-pumping manifolding, the simplified anode-shielding structure, and the ignitor with associated control thyratrons of the excitation system.

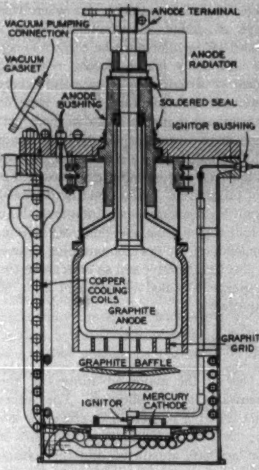
ANODE TERMINAL
VACUUM PUMPING CONNECTION
VACUUM GASKET
ANODE RADIATOR
ANODE BUSHING
SOLDERED SEAL
IGNITOR BUSHING
COPPER COOLING COILS
GRAPHITE ANODE
GRAPHITE GRID
GRAPHITE BAFFLE
IGNITOR
MERCURY CATHODE

Figure 3. Cross-section view of an Ignitron

Figure 3 shows a cross section of an ignitron.

There are a number of features associated with the small vacuum tank that contribute to reliability in service. The vacuum-tight gaskets are smaller. The need for a cathode insulator is eliminated. Copper coils for the water-cooling system are easily applied. The use of copper for external cooling coils and nickel for internal cooling coils eliminates all ferrous materials from the cooling system and reduces the corrosion problem to a minimum.

In order to secure a satisfactory wave form, power rectifiers are usually built with multiples of six anodes. This practice is followed in assembling the ignitrons into units. While, if required, more than six ignitrons can be assembled with a common vacuum-pumping system, greater operating flexibility is permitted, even for large station capacities, when the units are sectionalized.

Figure 4 shows a rectifier unit of six ignitrons.

A great deal of work is now being done on sealed-off rectifiers of both the conventional and ignitron types. So far, sealed-off construction has been confined to relatively small sizes, smaller than the usual transportation requirement. Not enough experience has been obtained to determine the average life of such rectifiers and it is obvious that this factor will

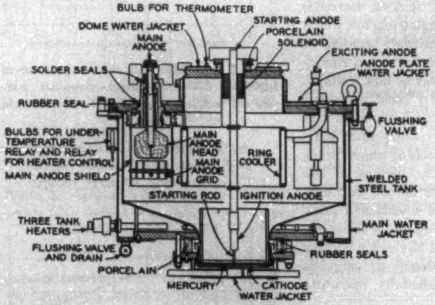
BULB FOR THERMOMETER
DOME WATER JACKET
MAIN ANODE
STARTING ANODE
PORCELAIN
SOLENOID
EXCITING ANODE
ANODE PLATE WATER JACKET
SOLDER SEALS
RUBBER SEAL
FLUSHING VALVE
BULBS FOR UNDER-TEMPERATURE RELAY AND RELAY FOR HEATER CONTROL
MAIN ANODE SHIELD
MAIN ANODE HEAD
MAIN ANODE GRID
RING COOLER
WELDED STEEL TANK
STARTING ROD
IGNITION ANODE
THREE TANK HEATERS
MAIN WATER JACKET
FLUSHING VALVE AND DRAIN
RUBBER SEALS
PORCELAIN
MERCURY
CATHODE WATER JACKET

Figure 1. Cross-section view of conventional multianode mercury-arc rectifier

Figure 2. A 750-kw 600-volt six-anode mercury-arc rectifier

determine the maximum size where this type of design is attractive. Since a sealed-off rectifier must be thrown away, or undergo a major factory rebuilding when it deteriorates to a point where service is unsatisfactory, the life must be many years to justify its use in the larger, more expensive sizes. On the other hand, in the small capacity sizes where the cost is low, the cost of periodic replacement is not prohibitive. This must be balanced against maintenance and relatively high initial cost of a vacuum-pumping system. Omission of the vacuum-pumping system is always attractive and an increase in the capacity of sealed-off rectifiers is to be expected.

Efficiency

One of the major advantages of the ignitron is its high efficiency. The factors that affect a rectifier unit efficiency are the losses of the transformer, the losses of the auxiliary apparatus, and the voltage drop in the power arc. The transformer is a highly developed piece of equipment and the losses are established by the economics of design. There is not much margin here on which to work to improve unit efficiency. The auxiliary losses are low and their total elimination would not represent much gain for units of the size used by the transportation industry.

The arc loss of a conventional rectifier constitutes not only two-thirds of the total unit loss but is known to be greatly in excess of that theoretically necessary. The present commercial ignitron, with its arc drop of 14 to 18 volts, is a major advance toward the theoretical minimum from the 20 to 30 volts obtained in the conventional design. In both ignitrons and multianode rectifiers the higher arc drop is associated with the higher ratings.

Figure 5 illustrates the efficiency advantage of the ignitron over conventional rectifiers at 600 volts.

For 275-volt applications, the ignitron has an even more marked advantage in efficiency. In any given type of rectifier the losses are almost proportional to the current and the arc drop is only slightly influenced by the system voltage. Therefore, the arc drop is a greater proportion of the output voltage in the lower voltage classes and an arc drop advantage becomes more important. This is illustrated in figure 6.

Factors Influencing Rating

The major factors which determine the rating of a given design are thermal limit, current instability in the arc, and arc-back frequency.

The thermal limitations of materials used are easily determined and offer no problems from the design standpoint.

Arc-current instability is the cause of voltage surges. In order to transport a given current in an arc, proper ion density must be maintained. The lower the vapor density, which is influenced by the temperature of the cooling surfaces, and the more obstructions in the arc path, the more difficult it is to maintain proper ion density. With an inadequate supply of ions, the arc resistance fluctuates with corresponding current fluctuations, or the arc tends to go out. Sudden decreases of direct current through a reactor, which in this case is the secondary winding of the transformer, cause the stored energy of the reactor to appear as high voltage, or voltage surges. To avoid surges in conventional rectifiers, it has been the practice to maintain cooling-water temperatures above the value at which surges occur. In the ignitron, because of the reduced obstructions in the arc path represented by minimum shields and grids and because of the proximity of the anode to the cathode, where the vapor density is greatest, the tendency to surge is greatly reduced. Incidentally, operating conditions under which surges occur also favor the occurrence of arc-backs. This cause of arc-back is, therefore, materially decreased in ignitron rectifiers.

The most important of the limitations in the design of a rectifier is arc-back. Although the causes of arc-back are not fully understood, they are known to be favored by such things as impurities in materials and foreign dirt particles, poor vacuum, too high mercury-vapor density, surge conditions, and exposure of an anode bearing back voltage to a cathode spot or ionized gas. The first three of these causes are minimized by careful selection of materials, careful shop practice, use of modern vacuum technique, and adequate cooling-medium control. Surge-producing conditions are avoided for normal conditions of operation.

As previously mentioned, the last cause is minimized by the use of shields and grids, and for the conventional rectifier, by the removal of the anodes from the cathode. These shields and grids increase the arc drop and the amount of the increase is proportional to the extent to which the arc-back rate is minimized. Experience with conventional rectifiers in commercial service has established the economic balance between the permissible arc-back rate and efficiency, as influenced by arc drop. Since the ignitron has per-

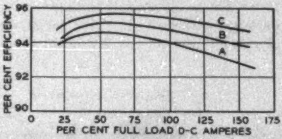

Figure 5. Rectifier unit efficiency curves of 3,000-kw 600-volt voltage-controlled mercury-arc rectifiers with 13,200-volt 60-cycle supply

A—Single-tank 12-anode rectifier
B—Sectional-type 24-anode rectifier
C—Ignitron-type 12-anode rectifier

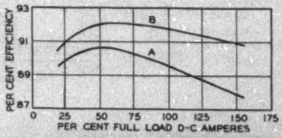

Figure 6. Rectifier unit efficiency curves of 600-kw 275-volt voltage-controlled mercury-arc rectifiers with 2,300-volt 60-cycle supply

A—Single-tank six-anode rectifier
B—Ignitron six-anode rectifier

Figure 4. A 1,500-kw 600-volt six-ignitron rectifier unit with vacuum and control auxiliaries

mitted a substantial reduction in shields and grids as well as anode to cathode spacing, by an entirely new principle of operation, there is a new proportionality between arc-back frequency and arc-drop voltage. At present, because there is this new proportionality, in order to provide greater freedom from arc-back, full advantage of the efficiency increase has not been taken.

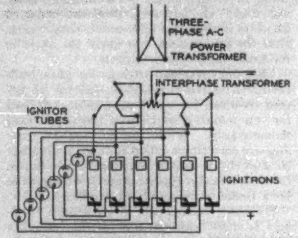

Figure 7. Ignitron rectifier diagram using the anode firing method of excitation without voltage control

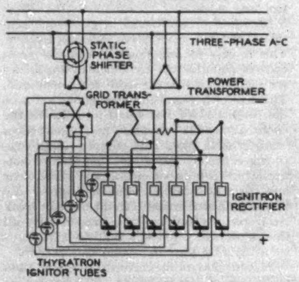

Figure 8. Ignitron rectifier diagram using the anode firing method of excitation with voltage control by phase shift

Special Circuits for Ignitrons

There are several ways by means of which the necessary accurately timed current impulses may be applied to ignitron ignitors. Possibly the simplest is shown in figure 7, which uses anode firing without direct-voltage control. The ignitor power is taken from the main transformer through thermionic-cathode gas-filled ignitor tubes. When the anode of the ignitron and associated ignitor tube become positive with respect to the cathode, current will flow through the ignitor tube because of its thermionic cathode. Upon the creation of the ignitron cathode spot, caused by this flow of current through the ignitor tube and ignitor, the ignitron will pick up, its arc short-circuiting and therefore extinguishing the ignitor current.

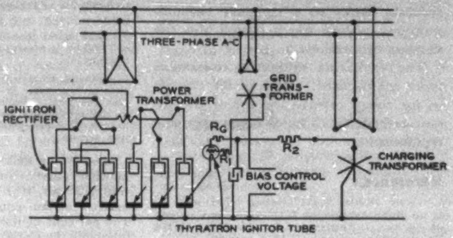

Figure 9. Diagram of a capacitor firing method of excitation

Control of the direct voltage of an ignitron is obtained in the same manner as it is for the conventional rectifier, that is by delaying the pickup of the anodes. However, in the ignitron, this delayed pickup is controlled by the provision of energized grids in the ignitor tubes rather than in the main power arc. This permits voltage control without any sacrifice in efficiency and by the use of relatively very little control energy. Figure 8 shows the devices and circuits involved. Thyratron ignitor tubes, which are thermionic cathode tubes with control grids, are used for the ignitor circuits. The anodes of the ignitron and thyratron tubes become positive with respect to the cathode, as before, but current will not flow through the thyratron tube until its grid is made positive with respect to the cathode by the grid transformer. The grid transformer is energized through a static phase shifter by means of which the phase angle between the potential of the anode and that of the grid may be accurately controlled. In this manner, the formation of the cathode spot in the ignitron is delayed to secure the desired direct-voltage reduction.

Another method of ignitron excitation is shown in figure 9, in which the ignitor current is obtained from a separate source including capacitors and a charging transformer. In the circuit shown, direct-voltage control is obtained by means of a bias voltage in the neutral of the grid transformer rather than by means of a phase shifter.

Other methods of ignitron excitation include transformer without capacitors, peaking transformer with Rectox, rotating impulse generator, and rotating commutator. The last three methods do not require ignitor tubes.

Since the ignitron depends on current flow through its ignitor before an anode can pick up, it is only necessary to block the excitation current to prevent pickup. This blocking can be accomplished either by opening the ignitor circuits, which can be done by relay contacts, or by applying a negative voltage to the grids of the thyratron tubes, if used. Since this involves only the control of low-energy circuits or the blocking of grids of low-energy tubes, "arc snuffing," that is, the interruption of d-c short circuits or of anodes feeding into an arc-back, can be accomplished with great speed and reliability.

Conclusions

The ignitron rectifier brings to the transportation industry a conversion unit having several advantages over the conventional multianode rectifier.

Although, in the interest of high reliability, full advantage is not taken of the possible reduction in arc drop, an efficiency advantage of from one to $1^{1}/_{2}$ per cent is realized for 600-volt units and of from 2 to 3 per cent for 275-volt units.

Because control of anode ignition is accomplished through small auxiliary tubes, voltage control is more flexible, has no detrimental influence on efficiency, and requires less control energy. For this same reason, "arc snuffing" can be accomplished with maximum speed and reliability.

Because the single-anode tanks of ignitrons are relatively small, the use of tubing of copper or other corrosion-resisting material for the cooling system is easy. This practically eliminates the corrosion problem.

In performing internal maintenance, only one ignitron need be opened at a time. The smallness and lightness of parts greatly facilitates this operation.

For a given rating, an ignitron unit is lighter and occupies less volume than a conventional rectifier. This results in economy of installation.

The ignitron also has some disadvantages.

There is the necessity for manifolding

separate tanks to a common vacuum-pumping system which complicates the vacuum connections.

The excitation system is somewhat more complicated. Most of the circuits in use today involve the use of thermionic-cathode tubes which require periodic replacement.

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Discussion

D. S. Smith (Northern Electric Company, Montreal, Que., Canada): In the paper on "Ignitrons for the Transportation Industry" the statement is made that as yet the sealed-off construction of rectifiers has been confined to small sizes below the range usually associated with traction substations. It is true that the individual sealed-off unit has not been built in large sizes, say above 1,000 amperes, but by the adoption of the unit principle large rectifier banks have been made possible. Glass is for many reasons an ideal material to use for the envelope of a sealed-off rectifier and the glass bulb rectifier has achieved considerable popularity in some parts of the world.

First of all let me give a rather broad picture by describing briefly some of the large glass-bulb traction installations made by one British company:

A. The Manchester-Bury line of the LMS Railway has two 3,600-kw rectifier substations operating at 1,200 volts direct curreut. The rectifier transformers are connected 12-phase and no d-c smoothing equipment is used. Each substation has three banks of six bulb units each, with high-speed d-c circuit breakers for each buak. These two substations are two of ten glass-bulb-equipped substations on the LMS totalling 13,200 kw.

B. The Bombay Baroda Railway in India has one 4,000-kw glass-bulb substation comprising two banks of six bulb units each, operating at 1,600 volts direct current. The rectifier, which is connected 12-phase, is provided with d-c smoothing equipment and has automatic grid control to give an overcompounded characteristic.

C. The New Zealand Government Railways have six substations with a total capacity of 6,000 kw operating at 1,600 volts direct current.

D. The British Columbia Electric Railway has two 330-kw substations and one 660-kw substation operating at 550 volts direct current. Their experience with glass bulbs has been the subject of an article in the technical press.

E. The London Passenger Transport Board has a total of 49 glass-bulb substations ranging in size up to 3,000 kw and having a total capacity of 35,500 kw.

F. The largest glass-bulb rectifier substation is one of 7,000 kw in Shoreditch London but this is not a traction job.

G. Finally, to give some idea of the place of the glass-bulb rectifier in Great Britain, I might say that the total installed capacity is over 500,000 kw against 350,000 kw of steel tank—these figures include all power rectifiers, not only those for traction service.

Now let us look at the glass bulb from another angle. In spite of its apparent frailty it can, if it is of approved type, be insured at an annual premium equal to $4^{1}/_{2}$ per cent of its value for the nine years following the initial year of service. During the first year it is covered by the manufacturer's guarantee. Mention has already been made of the Shoreditch station and it is interesting to note that no bulb replacements have been made to the three original 2,000-kw banks each with 16 bulbs and installed in 1929, 1930, and 1931, respectively. For a 2,000-kw installation made in 1925 the average annual maintenance cost per bulb unit including bulb replacements has been under $2.25 in spite of the fact that the bulbs are of an old type which could not be repaired. One engineer when asked about maintenance charges on a 3,600-kw traction substation under his charge placed them at about five shillings a week, $1.25, and this included the cost of periodical cleaning up of the substation.

Objections to the use of glass in the envelope of a rectifier are largely psychological and are gradually being overcome as a result of the comparative rarity of breakage in handling. One installation of glass-bulb rectifiers is in a mine in South Africa where the bulbs are subjected to frequent concussions from explosions. The British Admiralty actually carried out tests in which rectifier bulbs were set near large guns during firing and as a result have a glass-bulb rectifier at the Woolwich Arsenal.

Glass-bulb rectifiers are of course characterized by their extreme simplicity, the only auxiliary required being the cooling fan. The bulbs hold their vacuum indefinitely so that no vacuum pumps are required, and control gear is very simple in comparison with corresponding gear for water-cooled steel-tank installations. The strength of the glass bulb really lies in the seals which are simply formed by using a glass and metal combination with two nearly identical coefficients of expansion. The technique of glass manufacture, including of course, the seals, is the foundation for the success of the glass bulb, but it has taken long years to develop the technique and as a result the pioneer manufacture of bulbs in Great Britain still does a very large proportion of the glass-bulb business. There is one other point in connection with the use of glass which must be mentioned and it refers to its transparency. This is an invaluable asset both during processes of manufacture and afterward during operation. Trouble in a glass bulb when it does occur is very readily diagnosed.

Bulbs of the larger sizes are usually of the six-anode type with two or three excitation electrodes continuously energized and starting electrode. The old tilting bulb is a thing of the past and methods of starting are very simple, one method using a flexible electrode which is drawn down into the mercury pool by means of a small external electromagnet. Start-up is practically instantaneous and full load can be thrown on a cold bulb without fear of trouble.

The electrical characteristics of the glass-bulb rectifier are generally similar to those of the steel-tank type. Inherent regulation is usually about six to seven per cent from low load to full load. Efficiencies generally are higher with glass bulbs than for the large tank-type rectifiers as the use of large tanks involves a longer arc path with higher arc drops—the arc drop is of the order of 22 volts in the glass bulb and is thus higher than for the ignitron, but about the same as for small tank-type rectifiers.

Common overload ratings for glass-bulb rectifiers for traction service call for 25 per cent overload for two hours, twice full load for ten minutes, and three times full load momentarily. While higher overload ratings are sometimes called for and can readily be met by a small derating of the bulbs, these are fairly typical figures which have been found satisfactory in practice.

Six-phase connection of glass-bulb rectifiers is most common for traction supplies and smoothing equipment comprises air-cored choke and tuned shunt circuit. Twelve phase is sometimes used, two six-phase bulbs working together, and in such case smoothing equipment has usually been found unnecessary except where grid control was in use.

Backfires or arc backs in glass-bulb rectifiers are of extremely rare occurrence. This is easy to understand when the processes of manufacture by which all impurities are excluded from the bulb are considered. The use of a pure graphite anode reduces the likelihood of hot spots forming on the anode and as the vacuum is permanent there is no trouble from this source. High-rupturing-capacity fuses are normally connected in each anode circuit and these clear any internal faults but high-speed d-c breakers in the output circuit of each bank clear on external faults. Incidentally, all that is necessary to put a bulb back in service after a backfire is to replace the fuses.

Present maximum current ratings of glass bulbs are of the order of 400 amperes at 550 volts and 300 amperes at 1,500 volts, these ratings having been made possible by the efficient use of fan cooling. Experimental bulbs have been built to carry 1,000 amperes at the lower voltages and it would appear that a bulb of this rating would be useful for large installations if there is no sacrifice in efficiency.

Voltage control where required has in many cases been effected with induction regulators or on-load tap-changing transformers. Grid control has been very successfully applied but is not favored very generally on account of the harmonics introduced. Where grid control is used it is considered advisable to apply the debiasing voltage in the form of a steep-front wave, as the alternative method of steadily increasing the amplitude of the positive bias may lead to uncertain timing of the ignition of anodes working in parallel.

Multibulb banks of glass bulbs have the distinct advantage that the loss of one bulb results in only a small decrease in capacity. Further the large number of anodes results in a low current density per anode and this, together with the wide mechanical spacing of the electrode arms helps further to reduce the possibility of backfire.

While it would appear at first that the space required for glass-bulb rectifiers would be large this in fact is not the case. Generally speaking they require little if any more space than equivalent metal-tank types, this being partly due to the smaller amount of control gear necessary with glass.

Finally I would refer to discussions which have been going on with a view to eventually issuing an international specification on mercury-arc rectifiers. While it was originally proposed to have two separate specifications with different test conditions for steel-tank and glass-bulb rectifiers, as a result of the insistence of the glass-bulb manufacturers themselves the two are to be grouped together and the glass-bulb rectifier will thus have to meet the same standards as the steel-tank type.

J. J. Linebaugh (General Electric Company, Schenectady, N. Y.): The authors have given us a good outline of the development of the ignitron type of mercury-arc rectifier for transportation service with a general description of the ignitron principle, as applied to single-anode tanks.

The company with which the writer is connected has been working on the new problems incident to the development of this new type of rectifier for several years with very satisfactory results.

In 1937 the development had reached such a stage that an order was taken for a 3,000-kw 625-volt 12-tank unit for the New York Board of Transportation subway system. This unit has been in service since June 1938 and carries regular loads successfully. It is interesting to note that this unit must deliver 14,400 amperes for one minute. Similar equipment has been sold for service in coal mines.

One of the main problems to be solved in this development is the best method of starting the arc, as regards simplicity and reliability.

A number of different firing schemes have been proposed and several tried with varying degrees of success. Improvements are continually being made for the purpose of simplification and longer ignitor life.

It has been our experience that these 625-volt single-anode tanks can be opened for inspection and then restored to regular service without the necessity of bakeout if each tank is provided with a separate vacuum valve.

Our experience has been similar to that described in the papers and we find the advantages set forth in the conclusion of the paper are amply realized.

This type of rectifier will undoubtedly be a serious competitor of the multiple-anode tank.

S. R. Durand (nonmember; Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of this interesting paper have reviewed the principle of the ignitron tube and compared it with the multianode rectifier tank which today is well established in the transportation industry. When new developments are created which appear to have advantages in comparison to equipment already in use, there is always a period of time in which the value of these new features must be carefully weighed against the ruggedness and reliability of the well-tried and proved equipment. Very often in spite of outstanding new features incorporated in a device, some of these features in themselves may have inherent limitations which will impair the ultimate development of the equipment to the same degree of service reliability as attained in the older equipment. As the development of ignitrons and similar electronic devices proceeds, it is possible that in the near future equipment will be perfected which will most nearly meet in all respects the qualities desired in conversion apparatus for the transportation and other industries.

The authors have described the method of control utilizing the principle of timing the ignition. However, they also mention that use is made of shields and grids in ignitron tanks, and a grid and grid-inlet bushing connector are shown in the cross-sectional diagram of figure 3. It would be interesting to know if grid control is employed in some manner with ignitor control, or if the grid is simply energized to assist in the pick-up of the arc.

The use of external copper and internal nickel cooling coils is preferable to the use of steel water jackets on small tanks even though the problem of corrosion has been materially reduced in most large rectifier installations by means of recirculating cooling systems with heat-exchange units. In some localities the cost of cooling water is an important item in the operation of rectifiers in the transportation industry, so that it would undoubtedly be of interest to many engineers to know if the cooling-water consumption of a group of ignitrons can be reduced in comparison to multianode tanks under the same load conditions.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors present in a very interesting way the principle of the so-called "ignitrons" consisting of a tube or tank with a single anode having a cathode and an ignition device called the "ignitor," from which this kind of rectifier took its name. A very instructive comparison is made between the design as well as the characteristics of this rectifier with a conventional multianode rectifier which today is well established in the transportation field.

The main feature of operation of this rectifier is to establish a cathode spot only for a very brief interval so that no arc is maintained during the time a reverse voltage is applied to the anode. Therefore no ionized gases will be present during this period and it is claimed this principle will result in an operation free of backfires. At least the original papers by Mr. Slepian elaborated on this theory and pointed out that this new principle of inducing an anode to fire would reduce the backfire tendency even though the anode is located directly above the cathode and not shielded by grids, baffles, or the like.

Therefore I was greatly astonished to notice from the cross-section view of such a rectifier (see figure 3) that not only is the same anode arrangement with shields used as in the conventional multianode rectifiers (see figure 1), which the authors call "an elaborate one," but the same ring insulator to hold the anode shield and a very complicated grid and shield with two baffles and a cathode with an ignitor. By comparing figures 1 and 3 these facts become apparent. Considering that for a 12-tank arrangement there are 12 ignitors and 12 cathodes instead of one as in the conventional type of rectifier, it, therefore, seems to me that the authors overstress the simplicity of this new rectifier design. The same comparison could be made considering the number of auxiliary devices for the ignition apparatus of both types, however, reference is made to figures 7, 8, and 9, each showing as many ignition and auxiliary devices as there are tanks.

The brief presentation about surges is very interesting and our observation on an ignitor-type working together with a conventional-type rectifier, when both were cooled with water at three degrees centigrade, showed the former to operate without any surges originating in the main arc. However, what was most surprising was that a great many surges were found to be originating in the ignition arc. It is not our experience that surges due to an unstable arc lead to backfires; several 3,000-kw units on the Long Island Railroad which were installed from six to ten years ago were subjected to numerous surges during the winter season because the cooling water was often below ten degrees centigrade during starting. There are, however, other reasons which made our company introduce recoolers in order to avoid having water of very low temperature enter the rectifier, as is the case when direct cooling is used. The same reasons would in some cases also necessitate a recooling system for these single-anode tank ignitor rectifiers.

I understand that not only is the life of these ignitors very short, probably due to the fact that the main arc in each cycle is for an instant concentrated at the base of the ignitor, but that these ignitors fail quite often, during normal operation, to establish the main arc, and one or more anodes and their respective transformer windings refuse to carry current for several cycles. In other words, every time an anode of a 6- or 12-phase transformer rectifier circuit fails to pick up current, the 6- or 12-phase transformer is magnetically unbalanced. It was a surprise to me to see ignitor rectifier installations where no precautions were taken to avoid the destructive effect of such abnormal operations. These effects may not yet be apparent since these rectifier installations are operating below full load and have not been in service very long. Frequent arc failures may affect the life of the secondary windings of the transformer, especially should such ignitor failures occur during heavy load.

It would have been very valuable if the authors could have given some data on how these ignitor rectifiers behaved during overload and what overload characteristics they show compared to the conventional multiple-anode rectifiers. Due to the lack of volume, the gases freed during overloads and the excess vapor pressure produced may have a very decided effect on the overload capacity of such small tanks.

It would have been very interesting if the authors could have made some comparison between the conventional multiple-anode rectifier tank, the sectionalized multiple-anode rectifier, and the ignitor single-anode tank, not only as to arc drop or efficiency, but also in regard to auxiliary equipment, power consumption of auxiliary equipment, etc. This would have been very instructive because the same authors have fostered the introduction of sectionalized units—in other words, have repeatedly recommended the use of four multiple-anode tanks of 500- to 750-kw size instead of a 2,000-kw or 3,000-kw single-tank unit. All of you who have followed the rectifier development during the last few years will probably agree with me that the sectionalized rectifiers have not been used here nor abroad very extensively. Now this paper recommends the use of a further subdivision of units in tanks of single anodes. I do not want to infer that this may not be the final solution, but on the other hand I would like to call to your attention that the same optimism was expressed the last few years in regard to the sectionalized unit. Furthermore, we have to keep in mind that new features must be carefully weighed against the reliability of the well-tried and proved equipment.

J. H. Cox and **G. F. Jones:** Mr. Smith has presented some very interesting data on the capacities of glass-bulb rectifiers in service throughout the world and the bulb life being experienced with these units.

Bulb capacities up to 1,000 amperes are predicted, but it is interesting to note that the highest bulb capacity, for a commercial installation, listed in the discussion is 208 amperes. For large-capacity installations, this would indicate a large number of units with attendant complication and large space requirement. The overload rating standards listed in the paper are much lower than American standards for transportation service.

Glass is a poor heat conductor, which accounts for the very low capacity/volume ratio as compared with metal-tank rectifiers. Relatively large condensing surfaces are required in order to control the vapor pressure for given load conditions.

Except during the experimental stage, the ability to see what is going on inside the rectifier has little value. With modern relay applications, faulty conditions are easily detected and the cause exactly determined. With metal-tank rectifiers, any necessary repairs can be made on location by the regular maintenance personnel.

The glass-blowing art has not been developed in America to the point where large glass-bulb rectifiers can be made reliably. This makes any American user dependent on a European supplier. Contrary to Mr. Smith's statement, we are informed by a British user of glass-bulb rectifiers and by our European manufacturing associates that a regular setup is made for return of the glass bulb to the factory for re-evacuation, the user being supplied with special shipping cradles to minimize breakage during shipment.

For comparable service standards, the control functions for any type of rectifier are the same. Mr. Smith points out the simplicity of fuses in the anode circuits of glass-bulb rectifiers. This is simply an inexpensive device for localizing faults which could be used with any rectifier if service standards permit manual replacement of fuses.

Mr. Durand has asked regarding the energized grid in the ignitron illustrated and regarding comparative water consumption of the two types of rectifiers.

As used at present, the grid is connected to its associated anode through a resistor, in the large size ignitrons, to insure prompt pickup of the anode. It is not used for voltage control. This is so easily accomplished by control of the ignitor current that there is no need for controlled grids in the ignitron.

For given conditions of load and cooling-water temperature, the water consumption of the two types of rectifiers is comparable. The loss in the ignitron is lower, but for present designs, the discharge water temperature is also lower, the two factors practically counterbalancing each other so far as water requirement is concerned. Due to the use of copper cooling coils for the ignitron, except for very bad water conditions, a heat exchanger is not required to minimize corrosion. For direct water cooling, the water consumption is considerably less than for a unit requiring a heat exchanger.

Mr. Marti expresses surprise at seeing grids in an ignitron. As pointed out in our paper, a limited amount of shielding is used to take care of the transition period. As is natural with a new product, ample margin is taken in the use of these grids. It is expected that they will be reduced in the future. However, even in the present design, the amount of reduction of shielding can be realized by comparing the arc drop of 12-anode 3,000-kw 600-volt units. For the ignitron the figure is 18 volts and for the single tank design, approximately 30 volts. There is little likelihood of the shielding being reduced in the "well-established" conventional rectifier.

The relative simplicity of the anode structure of the ignitron cannot be realized by comparing figures 1 and 3. Figure 3 shows one anode of a 12-anode 3,000-kw 600-volt ignitron, whereas figure 1 shows one tank of a four-section 3000-kw 600-volt sectionalized rectifier which has a total of 24 anodes and which has by far the simplest anode structure of any rectifier at this rating. If a comparison is made with the anode structure of a 12-anode single-tank rectifier at this rating, for either grid control or non-grid control, the relative simplicity is quite pronounced.

The ignitron has a separate cathode for each anode. This cathode consists of a mercury pool in the bottom of the tank with a small quartz ring to confine the intermittent arc to the center of the pool. Note that there are no cathode insulators, no vacuum seals, no return mercury baffles, and no moving parts in the excitation system.

Mr. Marti's statement regarding ignitron circuit surges comes as a complete surprise to us. Surges in an arc originate when, for given conditions, the arc path is overloaded. If properly applied, the low-energy ignitron circuit of the ignitron will not surge nor will it be influenced by surges originating in an associated power arc. In the authors' experience, which comprises most of the past experience with ignitrons, there have been no surges in ignitron circuits.

The life of the ignitors is still to be determined. We now have over 100 ignitors in commercial rectifier service and have yet to experience a single failure of an ignitor in operation extending up to two years.

Pickup of an ignitron anode is just as reliable as for a multianode tank rectifier. For either type of rectifier, it is simply a matter of supplying the proper ionization to insure pickup. Ignitrons operate an inverter service with excellent reliability. Here, a single failure of an anode to pick up will result in a forward fire or short circuit. Extensive testing of both types of rectifiers in this service shows comparable reliability.

Failure of a thyratron excitation tube will result in failure of its associated anode to pick up. This results in magnetic unbalance of the transformer with tendency to saturate and with consequent increase in magnetizing current. The only result is increased heating of the transformer. This is a gradual temperature rise and is guarded against by transformer thermal protection. It could hardly be termed "destructive." An occasional failure of an anode to pick up is definitely of no consequence.

In actual railway service, a few failures of thyratron tubes have been experienced. The tubes have been replaced during normal inspection periods and in no case has any protective device been called upon to operate nor has service been impaired, even with tubes out of service for several days. Simple automatic means are available, if desired, to detect continuous misfiring of an anode.

The overload characteristics of the ignitron are equal to those of the conventional rectifier. Obviously they must be to meet the specified tests. They are somewhat superior in the higher, short-time overloads—above rating—because of the more open arc path and lesser tendency to surge.

The auxiliary power requirements are not an important item in determining rectifier efficiency. A vacuum pumping system requires less than one kilowatt and an excitation system for a tank rectifier or ignitron, three-quarters of a kilowatt or less. The efficiency curves shown in the paper include auxiliary power losses.

Contrary to Mr. Marti's statement, the sectional rectifier had an excellent reception following its advent, notwithstanding the fact that it was a new type in an established field. Since then, there have been as many sectional rectifier units installed in the transportation industry as single-tank rectifiers from any one supplier. The ignitron is receiving an even more gratifying reception.

Experience With Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines

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THE basic principles of ultrahigh-speed reclosing of high-voltage transmission lines have been presented previously before the Institute.[1] Although, as has been explained, the effects and phenomena behind these principles are generally known, there are still a number of unknown factors that have not been explored fully and the effects of which need be known to make a thorough and scientific application of these principles. Among these are further data on the effects of breaker time and elapsed time between clearing of the arc and the re-energization of the circuits on the deionization of the arc and the re-establishment of the insulation strength of the surrounding area; data on the probable extent of and total time elapsed, as well as time between intervals of multiple strokes; knowledge concerning the effects of the quantity and the type of load on the likelihood of restriking or the failure of lines to hold when re-energized by virtue of system drifting away from synchronism; and, finally, data on the dead time needed on phase to phase faults to prevent restriking.

All of these, however, are elements in a problem which can reasonably be expected to be further developed as time goes on. They are all aspects of a problem that on the whole is well understood and on which further work is going forward. But while waiting for results from this work to materialize, there is no reason why the admitted great advantages in power-system operation of utilizing the main principle need be or should be waived because the problem has not been worked out yet with complete precision. As a matter of fact the authors believe that there is no high-voltage line today, if it is of any importance, that should be designed and installed on any other basis except on the basis of ultrahigh-speed reclosing. Since the presentation of the first data on this subject, three additional line sections, that is six terminals, have been equipped with ultrahigh-speed reclosing and a number of others, as will be brought out later, are under way. It is the purpose of this paper to describe the development of the equipment and the installation of these new terminals and to cite operating experience with all of them in so far as that is available.

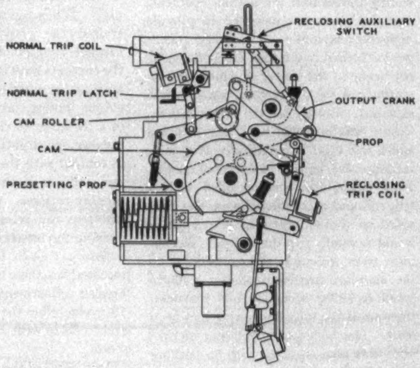

Figure 1. Schematic diagram of the new ultrahigh-speed reclosing mechanism for oil circuit breakers

The Elements of Ultrahigh-Speed Reclosing

The elements of an ultrahigh-speed reclosing setup are three. These are the high-speed breaker, the ultrahigh-speed mechanism and the high-speed relay system.

It is desirable that the circuit breaker be capable of interrupting a fault and de-energizing the line in the least possible time. So-called high-speed breakers available heretofore, in the United States at any rate, have had to utilize for this purpose a time of approximately eight cycles after the trip coil was energized. This does not refer to special designs. Within the last year more or less standard breakers having total time not to exceed five cycles have been developed and although faster times are expected, they are as yet not available today, in the American market at any rate. The work described herein has all been carried out so far on breakers having a time of approximately eight cycles.

The reclosing mechanism of the breaker has to be so designed that it is capable of reclosing the breaker with an elapsed time equal to no more than the minimum necessary to assure complete deionization of the arc and, therefore, assurance of no restriking on the one hand and minimum probability of loss of synchronism or loss of load on the other hand. Within the limits of eight-cycle operation of a breaker it has been felt heretofore that this time has to be of the order of a minimum of seven cycles but this has not been obtainable heretofore on mechanisms without running into stresses beyond what was desirable or practical in the standard breaker designs. As will be shown later on the actual performance so far this time has been from 10 to 14 cycles.

The relay system again must operate positively to clear the circuit on both ends in a minimum of time. Within the limits of breaker and reclosing speeds immediately in contemplation, it has been shown previously that this time should not exceed one cycle. No difficulty has been experienced so far in getting that with the utmost reliability.

In the earlier designs of ultrahigh-speed oil-circuit-breaker mechanisms two

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Paper number 39-61, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 25, 1938; made available for preprinting December 17, 1938.

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[1] For numbered reference, see end of paper.

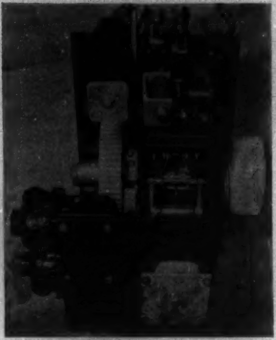


Figure 2. Front view of new ultrahigh-speed reclosing mechanism

mechanisms were provided for closing the breaker. One of these was a standard closing mechanism for normal operation and the other was designed to provide high-speed reclosing. These mechanisms were attached to opposite ends of a walking beam to the center of which the operating rod for the breaker contacts was attached. Energy for the high-speed reclosure was provided by a heavy spring which was reset automatically after each reclosure by means of a small motor. Each mechanism was provided with a trip coil and a transfer scheme was provided so that all tripping relays which would normally trip the reclosing mechanism were automatically transferred to the standard mechanism after a high-speed reclosing operation and remained there until the reclosing spring had been reset. The high-speed reclosing mechanism was also provided with an opening spring to speed up the opening of the breaker contacts. When the reclosing trip coil was energized this spring pulled the breaker contacts through a travel of about eight inches, at which point a latch released the closing spring, thus completing the reclosing operation. It was found that these mechanisms could be adjusted to give reliable operation with 18 cycles total elapsed time from trip coil energization to reclosure of breaker contacts.

Because there were two mechanisms required for each breaker, the total space needed for the complete equipment was considerably increased over that required by a standard electrically-operated breaker. Further, the space occupied by the high-speed reclosing part of the device was much greater than that used by the standard closing mechanism, so that the total space occupied by the complete equipment was more than doubled.

It was felt that this space requirement should be reduced if possible and at the same time the mechanism should be simplified in its operation. Keeping these objectives in mind, a new mechanism was designed and built which used the same motors both for normal closing and high-speed reclosing, thus eliminating the need for powerful reclosing springs, reducing the space required for the equipment, and considerably simplifying its operation.

A schematic diagram of the new ultrahigh-speed reclosing mechanism is shown in figure 1. As in the case of the earlier equipments these new mechanisms are provided with a standard trip coil and a high-speed reclosing trip coil. The closing motors drive a cam which operates the breaker output crank through a roller on its surface. At the end of the closing operation a prop falls into place and holds the breaker contacts closed. At the same time the cam is prevented from returning to the open position by the presetting prop. When the reclosing trip coil is energized the prop is removed and the breaker contacts begin to open. After the contacts have opened a predetermined amount, an auxiliary switch energizes the closing motors starting the cam revolving in a direction to close the breaker. The breaker opens until the cam roller comes in contact with the cam surface, the contact motion is then reversed, and the breaker recloses. Operation of the normal trip coil releases a trip-free toggle opening the breaker without a high-speed reclosure. As in the case of the earlier mechanisms, these new mechanisms permit breaker adjustment to reclose contacts in 18 cycles after the trip coil is energized and to do so with a considerably lessened strain.

Two views of this motor mechanism are shown in figures 2 and 3, and a view of the mechanism installed on a standard 138-kv oil circuit breaker in figure 4. The latter illustration is particularly striking when compared with a corresponding illustration of the original mechanism shown previously.

Ultrahigh-Speed Reclosing Installations on 132-Kv Lines of the Central System of the American Gas and Electric Company

The first 132-kv line on which ultrahigh-speed reclosing was installed, was the 59.2-mile line between the Fort Wayne (Ind.) station of the Indiana and

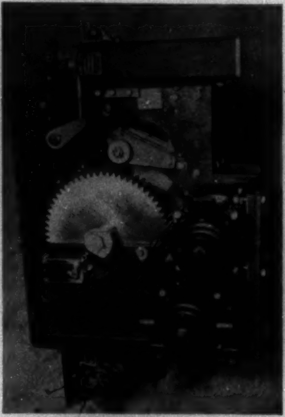


Figure 3. Side view of new ultrahigh-speed reclosing mechanism

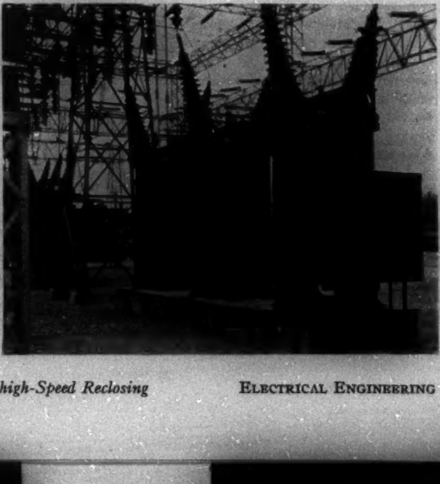


Figure 4. A 132-kv installation of the new ultrahigh-speed-reclosing breaker

Michigan Electric Company and the Deer Creek (Marion, Ind.) station of the Indiana General Service Company. This was placed in operation on May 17, 1936. Figure 5 shows a diagrammatic arrangement of the transmission lines in the area. It will be seen that these include a number of connecting lines comprising a loop circuit radiating from the Fort Wayne station and that they supply not only the Deer Creek station but also the Delaware station of the same company at Muncie, Ind. Although there is an interconnection at Kokomo, it is not of sufficient capacity to supply the entire load of the Indiana General Service Company when separated from the Fort Wayne station. Consequently the load here represents the general condition of a stub load fed from an interconnected system either by a single line or by a double-circuit line on a single-tower line. The solution for maintaining service continuity to this load was, as previously explained, felt to be the application of ultrahigh-speed reclosing, as a necessary part of which one-cycle carrier-current relaying was included. Behind all this was the idea that in case of simultaneous trouble occurring on the two circuits of the single tower line between Fort Wayne and Deer Creek, service restoration on the Fort Wayne-Deer Creek line would be made fast enough to avoid the normal consequences of a double circuit tripout.

The two ultrahigh-speed reclosing breakers used on this line were of the first design employing spring-operated mechanisms. The breakers were adjusted to give an over-all time of 20 cycles from the energization of the trip coil to the time the line was re-energized and that meant a dead time of approximately 12 cycles. The one-cycle carrier relay system gave an actual relay operating time of slightly less than one cycle.

The first operating results previously described and on which further data will be given in this paper were so successful that the same principle was applied on additional line sections. The sections chosen for equipping with ultrahigh-speed reclosing were three sections of a 350-mile double-circuit tie line between the Philo station of The Ohio Power Company, the Twin Branch station of the Indiana and Michigan Electric Company, and the Michigan City station of the Northern Indiana Public Service Company. Figure 6 shows diagrammatically these lines with various sectionalizing stations, generating capacities, and the synchronous condenser capacities installed at the various points. These three stations in turn tie in with very extensive 132-kv net-

Table I. Summary of Actual Operations of 132-Kv Ultrahigh-Speed-Reclosing Oil Circuit Breakers

				Fault						Source Terminal						Load Terminal				
Number	Date	Time	Line Section	Type	Single or Double Line	Cause	Fault Arc Restruck	System or Load Affected	Line De-energized Time (Cycles)	Primary Amperes	Relay Time (Cycles)	Oil-Circuit-Breaker Clearing Time (Cycles)	Oil-Circuit-Breaker Reclosing Time (Cycles)	Total Time (Cycles)	Oil-Circuit-Breaker Retripped	Primary Amperes	Relay Time (Cycles)	Oil-Circuit-Breaker Clearing Time (Cycles)	Oil-Circuit-Breaker Reclosing Time (Cycles)	Total Time (Cycles)	Oil-Circuit-Breaker Retripped
1	6/ 6/36	8:31 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	Yes	No	16.5	300	1.0	6.5	19	20	Yes	1,500	1.0	6.5	21	22	Yes
2	6/17/36	5:31 p.m.		Phase 3 to ground	Single	Lightning	No	No	13	281	1.0	6.5	18	19	No	1,552	1.0	6.0	22	23	No
3	7/23/36	6:02 p.m.		Phase 3 to ground	Single	Lightning	No	No	13	1,170	1.0	6.5	20	21	No	647	1.0	8.0	22	23	No
4	8/28/36	8:59 a.m.		Phase 3 to ground	Single	Lightning	No	No	12.5	563	1.0	6.5	20	21	No	1,165	1.0	6.5	22	23	No
5	5/26/37	3:41 p.m.		Phase 3 to ground	Single	Lightning	No	No	13	937	1.0	7.0	20	21	No	1,140	1.0	6.5	21	22	No
6	6/ 5/37	2:15 p.m.		Phase 1 to ground	Single	Lightning	No	No	12	300	1.0	6.5	20	21	No	1,725	1.0	6.5	21	22	No
7	6/24/37	7:17 a.m.		Phase 3 to ground	Single	Lightning	No	No	13	1,500	1.0	6.0	20	21	No	690	1.0	7.5	21	22	No
8	6/24/37	8:42 a.m.		Phase 3 to ground	Single	Lightning	Yes	No	14	1,125	1.0	6.5	20	21	Yes	863	1.0	7.0	21	22	Yes
9	7/ 3/37	2:30 a.m.		Phase 1 to ground	Single	Lightning	No	No	14	1,690	1.0	7.0	20	21	No	700	1.0	6.5	21	22	No
10	2/19/38	9:31 p.m.	South Bend-New Carlisle	Phase 3 to ground	Single	Sleet	No	No			1.0		18	19	No		1.0		18	19	No
11	5/20/38	3:17 p.m.	Fort Wayne-Deer Creek	Phase 3 to ground	Single	Lightning	No	No	12	1,000	1.0	6.5	20	21	No	1,050	1.0	6.0	20	21	No
12	5/20/38	7:15 p.m.		Phase 3 to ground	Single	Lightning	No	No	10	380	1.0	7.0	20	21	No	1,670	1.0	6.0	20	21	No
13	7/25/38	11:06 p.m.		Phase 3 to ground	Single	Lightning	No	No	15	2,000	1.0	7.0	23	24	No	645	1.0	8.5	23	24	No
14	8/ 5/38	4:51 p.m.	No. 2 Twin Branch-Fort Wayne	Phase 3 to ground	Single	Lightning	No	No			1.0		18	19	No		1.0		18	19	No
15	8/10/38	8:26 p.m.		Phase 3 to ground	Double	Lightning	No	No			1.0		18	19	No		1.0		18	19	No

works totaling some 4,000,000 kw of generating capacity. The flow of power over this tie line on certain sections reaches above 100,000 kw at some times and for this reason it is obviously very important that the continuity of the line should not be interrupted, if at all possible, even in case of simultaneous trouble occurring on parallel lines on the same tower line. Hence it was only natural that with the promising results obtained on the Fort Wayne-Deer Creek line, the solution of ultrahigh-speed reclosing as a means to insuring continuity be tried here.

The 17-mile line between the South Bend and New Carlisle substations was equipped with ultrahigh-speed reclosing equipment and placed in operation on January 29, 1938. The 65.4-mile double-circuit lines between the Twin Branch generating station and Fort Wayne station were equipped with ultrahigh-speed reclosing equipments a little later and the equipments placed in service on July 25, 1938, on one line and on August 31, 1938, on the second line.

The six ultrahigh-speed reclosing breakers used on these three line sections were of the latest design, employing motor-operated reclosing mechanisms of the type shown in figures 2 and 3. All of these circuit breakers were adjusted to give an over-all time of 18 cycles from the de-energization of the trip coil and that meant a dead time of approximately 10 cycles. The one-cycle carrier-current relay system again gave the usual relay operating time of somewhat less than one cycle.

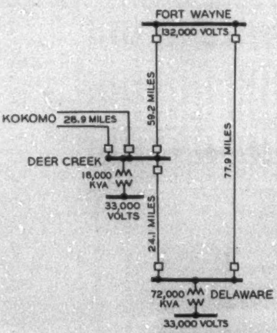


Figure 5. The 132-kv system of the Indiana and Michigan Electric Company radiating from Fort Wayne and supplying Deer Creek and Delaware stations of the Indiana General Service Company

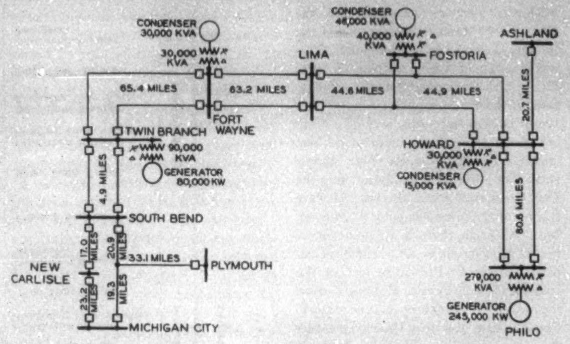


Figure 6. The 132-kv double-circuit tie line of The Ohio Power Company, Indiana and Michigan Electric Company, and Northern Indiana Public Service Company

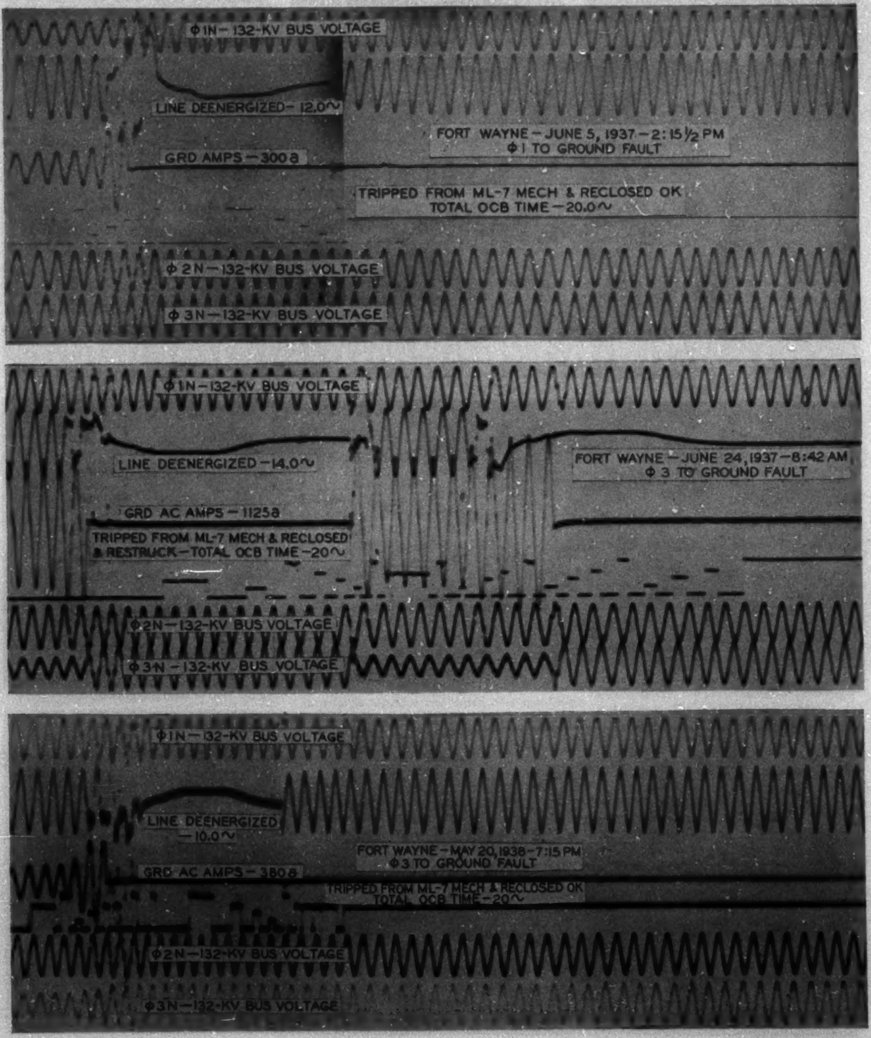
Operating Experience

With the installation of ultrarapid reclosing equipment on the three line sections described above, there were operating during a portion of 1938 a total of eight 132-kv breakers so equipped and serving four line sections. Since their installation there has been a total of 15 operations on the four line sections. A summary of these operations, all of which except one due to sleet have been traced to lightning, giving the time, type of fault, total time of de-energization, and details with regard to fault current and breaker operation, is given in table I. It will be seen that in all cases flashovers occurred between a phase wire and ground, 12 of the flashovers being between phase 3 which is the top conductor and ground, one between phase 3 which is the middle conductor and ground and two between phase one which is the bottom conductor and ground. In 13 of the 15 cases the breakers on both ends of the line tripped and reclosed without the arc restriking and without any of the normal deleterious effects of a feeder outage being felt in any case. This point, however, needs further elaboration and this will be done below.

In two of the 15 cases the breakers failed to stay in after the first initial ultrarapid reclosure. At least one of these cases analysis shows to be due to multiple lightning stroke and it is quite likely that the other failed to stay in for the same reason. Although in cases 2–7 inclusive and in cases 9, 10, 11, 12, and 13, no normal deleterious effects due to breaker outage and line de-energization were observed, it will be noted that in each of those cases only single line faults occurred. Hence no full test of the efficacy of the steps taken and the equipment installed was obtained in any of these situations. It is, however, interesting that in the case of the Fort Wayne-Deer Creek line where prior to the installation of a one-cycle relaying system, plus the ultrarapid reclosing setup, approximately 25 per cent of lightning flashovers involved both circuits, in more than two years that have elapsed since the installation of the ultrarapid equipment, not a single double-circuit flashover occurred although no other steps of any kind were taken on the line. Whether that is due to chance or whether the reason is to be found in the speeding up of the relaying and breaker action just the necessary amount to prevent involvement of the second circuit by the first to flashover, is something on which additional data will have to be obtained. But the two striking operations that put to at least partially complete test the principles attempted to be developed in this method of operation of high-voltage lines, were obtained in cases 14 and 15, and these deserve more full discussion.

In case 14, the system setup previous to this operation was a case of a single circuit tie between Twin Branch and Fort Wayne stations and a long weak tie line between the Fort Wayne and Plymouth stations. Referring to figure 6, the Twin Branch oil circuit breaker on one of the lines at Fort Wayne station was open due to oil-circuit-breaker revamping to ultrahigh-speed reclosing, and the breaker on the other end of the line at Twin Branch station was closed supplying a tap-off load. A 205-mile tie line between the Fort Wayne station and the Plymouth station by the way of Marion, Kokomo, Lafayette, Oakdale, and Monticello stations was closed. A lightning flashover between phase 3 conductor and ground occurred on the tie line between Twin Branch and Fort Wayne stations, causing the breakers at both ends of the line sections to trip from carrier-current relays and to reclose immediately without restriking without the loss of any load or system disturbance. The speed of operation was sufficiently fast to prevent the opening up of the weak tie line between Fort Wayne and Plymouth stations, which invariably occurred when Twin Branch station was separated from Fort Wayne station before the installation of high-speed-reclosing circuit breakers on these lines.

Figure 7. Oscillograms of actual operations on Fort Wayne-Deer Creek line



In case 15, the system setup was normal before this operation in that both lines between Twin Branch and Fort Wayne were in service as well as the weak tie line between Fort Wayne and Plymouth being closed. During a lightning storm simultaneous trouble occurred on both Twin Branch-Fort Wayne lines, causing one of the lines to trip at both terminals from carrier-current relays and to reclose immediately without the arc restriking. On the other line installation of high-speed-reclosing breakers was not as yet completed, and both terminals of this line tripped from instantaneous overcurrent relays since carrier-current relaying was not in service at the time. In this case no loss of load or system disturbance was experienced, and, in addition, the weak tie line between Fort Wayne and Plymouth stations did not open up.

The fact that in similar situations as those that occurred in cases 14 and 15 the system invariably pulled apart gives every reason to believe that the speeding up of the reclosing process can in similar situations be made to give the very desired result of having the systems opened and yet reclosed before either loss of voltage is felt on any part of the system or before the systems drift apart sufficiently to make necessary the introduction of considerable delay until they can be resynchronized. Obviously further expe-

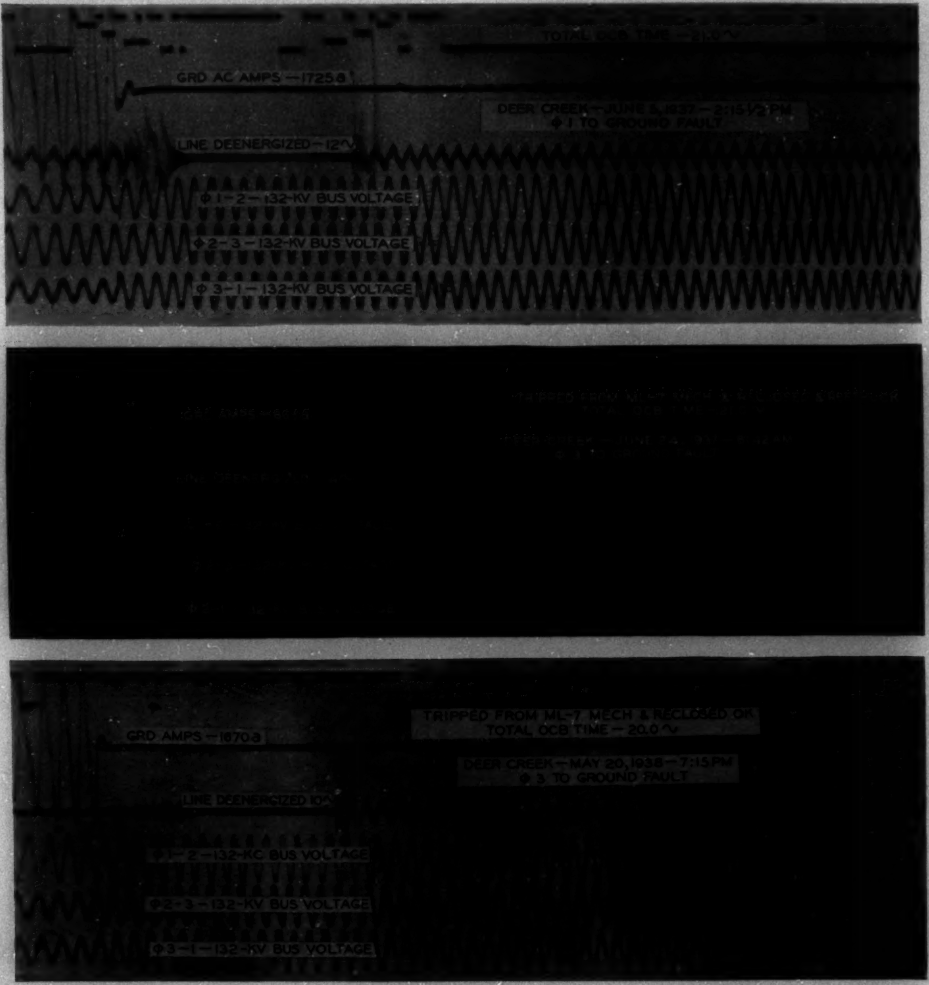


Figure 7 (continued). Oscillograms of actual operations on Fort Wayne-Deer Creek line

rience is necessary in similar situations while carrying loads more nearly within the stability limits of the line. But with the installation of additional equipments it is hoped to be able to present information on the performance under such conditions.

The oscillographic records of the performance of the systems under these conditions are extremely illuminating. In figure 7 are shown three sets of oscillographic records showing system performance in cases 6, 8, and 12. Referring to the record of case 6 it will be noticed that a short circuit occurred close to Marion on phase 1 and that the line remained deenergized for a period of approximately 12 cycles and reclosed without any further disturbance.

The oscillogram of case 8 shows the line voltage on the third trace from the top of the record taken at Deer Creek. This is obtained from a bushing potential device and it will be noticed that at least three cycles before the line was reenergized, potential of possibly three times normal appeared on the line, eventually dying out. The inference is very strong that a lightning discharge took place to the line and that the effect of that was to cause the line to flashover dynamically when it was re-energized some two cycles after the discharge itself had apparently disappeared. The appearance of the same voltage is shown clearly also on the record taken at Fort Wayne, except to a lesser extent. A possible explanation of that is that the voltage at Fort Wayne is measured by a regular instrument-type potential transformer.

The record of case 12 was obtained on May 20, 1938, and shows a successful reclosure of the Fort Wayne-Marion line. It will be noticed in this case that the fault was very close to Deer Creek and after a line de-energization of ten cycles, reclosure was entirely successful.

New Installations

The 80.6-mile double-circuit 132-kv lines from the Philo Generating station to the Howard substation (see figure 6) are both at present being equipped with ultrahigh-speed-reclosing circuit breakers in conjunction with one-cycle carrier relaying and it is expected that these will be placed in operation the early part of next year. This is another step in making this 350-mile tie line immune to interruption in continuity for simultaneous trouble occurring on two lines on the same tower line.

Plans are under way to install ultrahigh-speed-reclosing circuit breakers on the 77.9-mile 132-kv line between the Fort Wayne station of the Indiana and Michigan Electric Company and the Delaware substation of the Indiana General Service Company (see figure 5). It is expected that these installations will be put in operation before the next lightning season. The aim in this case is to install ultrahigh-speed-reclosing circuit breakers on both of the 132-kv supply circuits radiating out from the Fort Wayne station to insure uninterrupted service to this important load in case of simultaneous trouble occurring on both of these lines, one of which it is hoped will remain closed after the first reclosure.

A number of other points on the American Gas and Electric system still remain where principal source of supply comes in either over a single-circuit line or over a double-circuit line running on the same right of way without some loop backup. In all of these cases it is expected over the next year to install ultrarapid reclosing as fast as the breaker and relaying problems in connection therewith can be properly worked out and taken care of.

Conclusions

The results obtained so far, it appears to the authors, definitely warrant the following conclusions:

1. On high-voltage overhead lines outages caused by lightning can be materially reduced by ultrarapid reclosing. The originally[1] expected figure of 75 per cent seems conservative in the light of the additional experience obtained since that time.

2. Apparently two-circuit flashover, on double-circuit transmission lines properly equipped with ground wire, when the two ends are equipped with ultrarapid-reclosing breakers is materially reduced. It does not appear that the ultrarapid reclosure element can be a major contribution in this regard and it would appear, therefore, that this is in a large measure the result of a speeding up of the relaying and circuit interrupting process. More information on that is needed and it is hoped to gather it over the ensuing years.

3. It appears definitely that not only can service continuity materially be improved by the installation of ultrarapid reclosing on lines feeding isolated areas, but the use of ultrarapid reclosing on tie lines between two major generating systems will result in line flashover having a minimum effect on the continuity of power flow. Apparently even when handling power close to the stability of the line ultrarapid reclosure when properly functioning can be made to bring two systems together without the necessity of a wait for synchronization and without the danger of system drift to a point where reclosure without synchronization produces any deleterious effect or fails to keep the two systems together.

4. The experience with ultrarapid reclosure seems to indicate the need for a complete re-examination of the concept of the switching process on any important high-voltage circuit, consisting of relaying, de-energization, and re-energization of the circuit. If ultrarapid reclosure will accomplish, in a vast majority of cases, the results here indicated, it would appear logical to consider that under all cases the proper cycle of operation is one in which ultrarapid reclosure to the extent of one reclosure ought to become standard practice and that only in exceptional cases ought a period of waiting longer than the absolute minimum required for deionization, be permitted between de-energization and reclosure.

5. The work carried out on high-voltage systems seems to indicate that very much similar results can be obtained on intermediate and low-voltage lines by utilizing the same principle. The exploration of that field is a problem for the future. The authors themselves hope to be able to contribute something to that.

Reference

1. ULTRAHIGH-SPEED RECLOSING OF HIGH-VOLTAGE TRANSMISSION LINES, Philip Sporn and D. C. Prince. AIEE TRANSACTIONS, volume 56, 1937, pages 81–90.

Discussion

S. B. Crary (General Electric Company, Schenectady, N. Y.): The benefits which have been obtained from quick switching and relaying have been fully demonstrated in practice and by system studies and stability analyses. More recently immediate reclosure of standard circuit breakers has been applied with pronounced success to feeder circuits and to a limited extent to tie lines. Now Messrs. Sporn and Muller present operating data showing the improvements which they have obtained by combining quick switching with rapid reclosure by means of special, fast, breaker mechanisms and relaying. Those of us who are engaged in making system studies are impressed with the benefits which may be derived from quick switching and quick reclosing and the possibilities it opens up for improving the reliability of service. Undoubtedly, in almost every major system, there are circuit-breaker locations and system conditions for which quick switching and rapid reclosure would be or are of material benefit in improving service. Some circuit-breaker locations and system conditions make quick clearing and reclosing more desirable and favorable than others. These locations and conditions are generally recognized and can be determined quite accurately by a system analysis.

A system analysis based on operating records of the types of faults to be expected along with information similar to that present in the paper, can be used to predict the expected improvement in system performance. There are now generally available a-c network analyzers and developed procedures for making such studies. In recent years a better understanding has been obtained of the transient performance of machines and systems undergoing system disturbances and out of step conditions, both from an analytical and an operating point of view. No small part of this new knowledge has been contributed by the automatic oscillographs. As an aid in determining the electromechanical oscillations to which rotating equipment is subjected during out of step and pulling into step conditions, the differential analyzer has also been put into use. All of these methods of analysis have contributed to a better understanding of system and machine performance and have made it possible to predict more accurately their behavior. This knowledge can be used to advantage in the rational application of quick reclosure to power systems.

With simultaneous circuit-breaker tripping by means of carrier-current relaying and an over-all time of 18 cycles from energization of the trip coil to breaker reclosure an accomplished fact, it becomes evident that such equipment may be more generally used to prevent loss of synchronism between two systems for even more severe conditions than that reported in the paper by Messrs. Sporn and Muller. It should be recognized, however, that there do exist limitations as to how much power may be carried through a disturbance with a successful reclosure for a given set of conditions. The more important factors are the synchronizing strength of the electrical tie, the effective system inertias, the fault location, number of phases involved, fault duration, and reclosure time. However, the effect of these factors may be evaluated and the power limitations predetermined with fair accuracy by analysis.

A factor of considerable importance favoring the reclosing principle which is becoming more fully recognized is the ability of systems to pull together even after being closed-in out-of-phase or definitely out of step. The conditions and factors which allow for this resynchronization are determined to an appreciable extent by the automatic control devices, voltage regulators, and governors, which are attempting to hold the voltage and speed of the individual parts of the system. There have been several cases where this ability of systems to regain synchronism has been fully demonstrated. An important factor in such resynchronization is that the relaying should not be sensitive or operate during the out of step or pulling into step process. Such a consideration may tend to influence the relaying philosophy of a system which intends at some future time to take full advantage of quick reclosure. This constitutes an important reason why it is advisable to give immediate careful consideration to the reclosure problem. The factors which allow systems or parts of systems to resynchronize are also amenable to analysis. Since system performance can be studied evaluating the effect of the factors influencing system operation following reclosure, it is believed that in the future more consideration and study will be given to this problem in a similar manner as was done in the past, in studying the effect of quick clearing of faults as a factor in system and machine stability.

It has been suggested in the case of two parallel circuits which are equipped with quick switching and reclosing that a control scheme be used which will allow for quick clearing and reclosing of the faulted circuit, if only one is involved, when the load to be carried through the disturbance is above the stability limit of one circuit. However, in the case in which the load to be transmitted is below the stability limit with one healthy circuit left in service, reclosure on the faulted circuit is delayed in order to reduce the possibility of reclosing on an uncleared fault at an inopportune time of the system oscillation. Accordingly, in this case it is conceivable that the probability of proper system operation is actually increased by delaying the reclosure. However, if operating near or above the stability limit of one circuit it may be advisable to reclose as quickly as possible. The justification of using this type of control is determined among other factors, by information of the nature which Messrs. Sporn and Muller are accumulating, such as the percentage of faults which clear during the breaker reclosure time, the number of phases and circuits involved in the fault, etc. Their results are of considerable value and should encourage the consideration and study of a more general use of rapid reclosure.

E. E. George (Tennessee Electric Power Company, Chattanooga): In the first part of their paper Messrs. Sporn and Muller use one argument which might bear considerable emphasis. They state that the electrical industry ought not to wait for a complete and precise evaluation of all factors involved before utilizing high-speed reclosing. Most of the transmission companies in the Southeast would agree that ultrahigh-speed reclosing is desirable on all high-voltage lines; nevertheless they have not waited for high-speed-closing breakers. So-called immediate reclosing was put in use in the Southeast about five years ago and has since been considered standard practice on all transmission and distribution lines since that time. It has now been applied to practically all existing lines.

High-speed reclosing is of much more value today than it was a few years ago. Lines are being built better and there is increased probability of coming back in service "OK" after an interruption. Many years ago when a large percentage of line failures resulted in burn-downs and permanent trouble it was "OK" to do switching on the basis "where there's no hope there's no hurry." With breakers of the closing speed standard until recently, the reclosing time has varied from 75 cycles at 154 kv to 15 cycles at 2,300 volts with the majority of operations between 45 and 25 cycles.

It is now standard practice of The Tennessee Electric Power Company to reclose immediately on *both* ends of tie lines if pilot wire or power carrier protection is available. This is applied regardless of lower-voltage loops, transmission loops through the rest of the system or through interconnecting companies, etc., although it might not be adequate if a generating plant were tied to the system with only one line.

This reclosing is being used at both ends of 110-kv, 44-kv, and 11-kv ties, most of which are short and protected with pilot-wire relays. Wherever the relaying is too slow to permit instantaneous reclosing on both ends it is used on one end with synchronism check or dead bus reclosing on the other, thus restoring the line in a few seconds.

Many of us have talked about faster closing of oil circuit breakers for several years but outside of Mr. Sporn and his associates we have done very little about it. In answering a questionnaire on the subject of reclosing about five years ago our company stated that we would reclose breakers in ten cycles if the equipment were available. We still stand by this opinion and feel that reclosing even faster than Mr. Sporn is using would work out satisfactorily in most cases. Operating experiences have shown that little weight should be given the fear of multiple lightning strokes or reignition of an arc across transmission insulator strings. Multiple lightning strokes are largely confined to certain rare types of lightning storms, but do not seem to occur frequently, at least when we are talking about multiple strokes a few cycles apart rather than a few microseconds apart. In this area our worst lightning occurs in windshift storms during the passage of a cold front, and the lightning is generally accompanied by high winds. This tends to prevent reignition of an arc.

Experience in the southeastern area would indicate that all new transmission lines should be designed to utilize the development discussed by Messrs. Sporn and Muller. However, on existing transmission lines great advantage can be obtained with existing relays and breakers by applying immediate reclosing.

A. J. A. Peterson (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors are to be commended for the confidence with which they have proceeded to equip their important 138-kv transmission lines with high-speed reclosing equipment as described in this paper, and the results described in their paper well justify their work in this direction.

As pointed out in the paper, the practical limit on high-speed reclosure is determined by the time of complete outage required for deionization of the original fault path, which time, of course, increases with the service voltage. This confirms the findings of Griscom and Torok ("Keeping the Line in Service by Rapid Reclosing," *Electric Journal*, May 1933).

Even though the limit on reclosing speed for the circuit breaker is to a large measure determined by this deionization time, faster

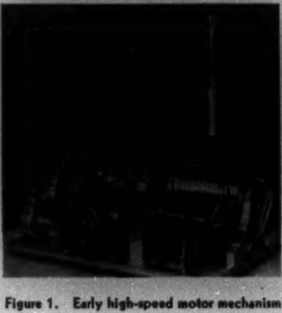


Figure 1. Early high-speed motor mechanism

arc interruption will obviously permit faster over-all reclosing time, and therefore, better maintain system stability.

With the fault current interrupted, the next step in restoring service and maintaining stability is to reclose the breaker quickly. The mechanics of a quick reclosing cycle are: first, fast opening of the breaker; second, stopping the opening stroke; and third, closing the breaker. Trip-free action must be provided so that the opening action will not be unnecessarily delayed by the mechanical or magnetic drag of the closing means. We have been working on such a mechanism for several years. Figure 1 of this discussion shows a high-speed circuit-breaker operating mechanism. It consists essentially of two sets of springs, one for closing and the other for tripping. These springs are charged by motor. Drive from the springs to the breaker mechanism is through a "capstan" clutch which gives very quick engagement at any part of the stroke, thus permitting fast closing at any desired point on the opening stroke, as well as trip-free operation at any point on the closing stroke. In reclosing, the closing means is controlled to initiate closing of the breaker before the full open position is reached. The mechanism thus quickly stops and reverses the breaker, and carries it back to its closed position.

A further development of the mechanism is shown in figure 2. This mechanism is a direct motor drive, using the same type of "capstan" or band friction clutch. The high-torque high-speed motor drives a helical band friction clutch through suitable worm gearing. This clutch freely rotates around a drum attached by suitable shaft, crank, and levers to the breaker operating rod. In order to close the breaker, the motor is energized and quickly comes up to speed. At the same time, the friction band is snubbed into action around the breaker drum by a low-energy magnetic face plate. This couples the breaker directly to the motor drive. Trip-free action is obtained by simple de-energizing the snubber, thus releasing the breaker drum from the motor friction band, and permitting the breaker to open freely through its normal accelerating springs. The same design of mechanism for 138-kv breakers is shown in figure 3. Because of its inherent ability to get into action quickly, this mechanism is well adapted to provide high-speed reclosing on a fast-opening breaker.

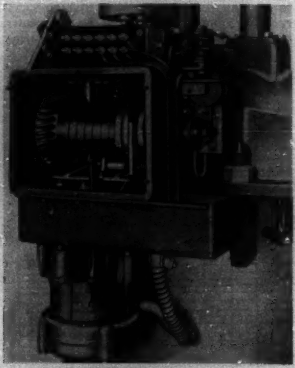


Figure 2. High-speed motor mechanism with "capstan" clutch, front view

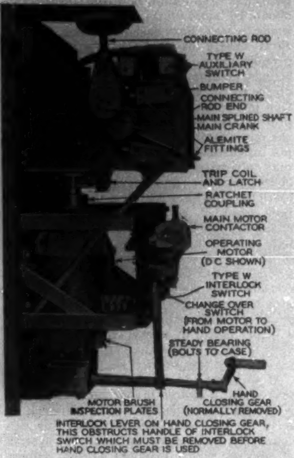

CONNECTING ROD
TYPE W AUXILIARY SWITCH
BUMPER
CONNECTING ROD END
MAIN SPLINED SHAFT
MAIN CRANK
ALEMITE FITTINGS
TRIP COIL AND LATCH
RATCHET COUPLING
MAIN MOTOR CONTACTOR
OPERATING MOTOR (D C SHOWN)
TYPE W INTERLOCK SWITCH
CHANGE OVER SWITCH (FROM MOTOR TO HAND OPERATION)
STEADY BEARING (BOLTS TO CASE)
HAND CLOSING GEAR (NORMALLY REMOVED)
MOTOR BRUSH INSPECTION PLATES
INTERLOCK LEVER ON HAND CLOSING GEAR, THIS OBSTRUCTS HANDLE OF INTERLOCK SWITCH WHICH MUST BE REMOVED BEFORE HAND CLOSING GEAR IS USED

Figure 3. High-speed motor mechanism with "capstan" clutch, for 138-kv oil circuit breaker, side view

D. C. Prince (General Electric Company, Philadelphia, Pa.): Engineers are constantly under pressure to find ways of providing better service at reduced cost. Any improvement in line availability and freedom from interruption is a step in this direction.

Overhead ground wires, improved grounding, additional insulation, Petersen coils, expulsion gaps, duplicate lines, and ultrahigh-speed tripping and reclosing are all means of improving continuity of service. Each of these steps affects continuity of service in a different manner; each has its cost, each must be judged in the last analysis by field experience.

Ultrahigh-speed reclosing is a means toward service improvement which presented originally a good many problems. First, ultrahigh-speed-reclosing means had to be devised; then laboratory and field tests had to be made to determine how fast reclosure would be useful. And then, finally, it was necessary to find how much of the possible theoretical gain might be realized in practice. The authors of this paper are to be congratulated and thanked for furnishing an answer to this last and crucial question.

The improvement in service appears to compare favorably with that obtained by other means. High-speed reclosure is applicable to any type of temporary fault whether phase to ground or phase to phase, and whether from lightning, sleet, or birds. It does not eliminate tripouts, but judging from the paper, does reduce loss of load which is the important element. Since breakers are required in any case, the cost of securing this improved service by high-speed reclosure is very moderate considering the improvement obtained.

It will be very interesting to see to what extent high-speed reclosing can be used as a substitute for other more expensive means of assuring service continuity.

Percy H. Thomas (Federal Power Commission, Atlanta, Ga.): Mr. Sporn and his associates have added another to the long series of fundamental and classic papers, on the problem of the adequate control of the flow of electric energy in heavy power circuits, and in the usual style—brief, to the point, and adequate.

A look ahead as to the ultimate critical factors in this most important aspect of the control of the flow of electric energy may be of interest.

What can be done to increase the length of the time of interruption that may be permitted without loss of synchronism in the system? With the complexity and infinite variety of conditions met in utility power circuits, no general particular method of extending this time seems probable. On the other hand, if and when the use of reclosing breakers is extended and the holding of synchronism becomes critical, no doubt a study of individual situations will show effective means to secure a material improvement. With the present available methods of recording transient phenomena in power circuits and the means for mathematical analysis, such a study offers little difficulty.

What can be done to shorten the time required for the extinguishment of the arc when the operation is to interrupt a flashover?

As stated in the paper, the prompt interruption of the current after the initial breakdown is of the greatest importance. The amount of matter heated and the accumulated energy generated in the arc might increase as the cube, or higher, with a proportionally rising current. Since the opening oil breaker introduces an important resistance in the line long before the current is interrupted, the saving in the accumulation of heat in the flashover arc will be very greatly reduced by quicker operations. That portion of the duration of the arc in which the current is a maximum would be materially shortened. But the saving is double: first, the saving of time in the opening operation; second, the lessening of the interval of waiting, with the current interrupted, for the arc to die out.

That this rapidity of action is important and very potent, is shown first by the well-known experiments of Nicholson on quick-acting arc switches and fuses, and, second, by the effectiveness of the low-voltage high-speed d-c breaker.

Such a phenomenon as the multiple lightning flash should presumably require a different treatment to avoid handicapping all the opening operations by a lengthy waiting period to meet the occasional special case.

Can anything be gained in opening the circuit by basing the action of the opening relay on the rate of rise of the flashover current? This "rate of rise" will be greatest at the start and will of course be at a maximum only when the total impedance of the arc circuit is small; that is, when the arc current is potentially very large. If the secondary of a line current transformer is connected to a relay of high resistance, preventing a saturating flow of current in the transformer, the relay would be subjected to a voltage proportional to the rate of rise of the arc current. This should represent some gain in relay action.

What advantages may be hoped for from changes in the mechanical construction of the breaker? No doubt with the excellent performance so far secured, evolution for the immediate future and for the immediate expansion of utility use should continue with substantially the same model. However, in seeking the ultimate best layout, other radically different forms should be considered, as in the end somebody will get a better type if the possibility exists. It is well recognized that at times less advantageous types of apparatus have been long used in practice in the electric art where better ones were potentially available.

In these breakers, the interests of size and cost and upkeep put a very great premium on light moving parts. Designs with possibly five to ten per cent of the mass of those here used would have a great advantage in these matters of cost and size, as well as speed. A radial blade carried by a rotating shaft, as already used in some designs, offers a tempting field for the designer. The very speed of action attainable might eliminate the necessity of some of the present arc-suppressing features. It may well be that a sufficiently rapid extension of the rupturing arc in the breaker through oil, under an initial moderate pressure, might have a miraculously increased effectiveness.

Is the growing aggregate capacity of generating stations and the necessity of increasing rupturing capacity likely to develop into a limitation in the use of reclosing breakers? It would be a natural inference from the reports of tests and construction difficulties in these "Sporn" papers that the economic rupturing capacity limit was very likely being rapidly approached in this type of construction.

There are at least two avenues of escape to be considered: first, the beneficial effects of more rapidly moving parts in breakers; second, the adoption in the layout of large electric power systems to cut the concentration of too great short-circuit power on any one bus or connection point. This can be accomplished, as is well known, oftentimes by paralleling large generators only through the distribution systems. Where there are several large generating stations on a network, the loose coupling of units can be accomplished, at least in a new system, with very little added cost. In such a case a great limitation of the present demands for flexibility in a major power station would be a minor handicap in operation, though requiring some sacrifice of habits of thought and a different point of view on the part of the station operator.

The writer has long advocated this approach to the solution of the threat made by the concentration of generating capacity.

In closing this discussion, the writer desires to point out what seems to him the natural conclusion from the availability of the reclosing breaker, that is, that, assuming that a satisfactory method of handing flashovers has been found, new line construction should be based on this fact and no effort be made to make lines flashover-proof. Reductions should be made in the heights of ground wires, in the length of insulator strings, clearances, etc. When the vast increase in the expense of overhead construction in the last few years, and the present pressure for lowered cost are remembered, the opportunity should be welcomed. It is not unreasonable to hope that ground wires may ultimately be largely omitted. Of course engineers and operators will be very loath to build lines with less insulation than now provided, and it will be agreed that changes should come step by step in the light of resulting experience, but, in the view of the writer it is the duty of our leading system managers, in appropriate cases, to consider carefully and initiate this policy. It may be pointed out that a low-insulated line protected with reclosing breakers may have a higher factor of safety, from the point of view of operation of the system, than a superinsulated line without such protection.

If we may add to the reclosing breaker protection, a system layout such that there is always the capacity in the network to serve the load with any particular link disconnected for a while, the result is a system of reserve, which provides against almost any form of interruption, whether lightning, mechanical failure, or operative error, and a system which does not require any supertypes of construction. One interruption a month on any unit in such a system would presumably not disturb operation and would be good practice.

While comments and suggestions of this sort may be sound and of a great potential value, the credit for any benefits resulting must go to those who have the foresight, the courage, the stamina, and the facilities for making the initial application.

V. M. Marquis (American Gas and Electric Service Corporation, New York, N. Y.): One reclosure which has not been discussed by the authors and which gave an excellent check on the ability to reclose between two large systems without losing synchronism when transferring heavy load occurred on November 16, 1938, on the South Bend-New Carlisle 132-kv line. The location of this line and its relation to the other parts on the system is shown in figure 6 of the authors' paper.

A fault on the Plymouth tap caused a trip-out of the South Bend-Plymouth-Michigan City line when about 80,000 kw was being delivered east from Michigan City, 50,000 kw of which was delivered to South Bend and 30,000 kw into the Plymouth substation. The tripping out of this line threw the entire 80,000 kilowatts on the Michigan City-New Carlisle-South Bend circuit and, immediately following, the South Bend-New Carlisle line tripped and reclosed successfully while carrying the load of 80,000 kw. Figure 4 of this discussion shows a graphic record of power flow over the line at New Carlisle substation; the load change due to the opening of the Michigan City-Plymouth-South Bend circuit and the reclosure on the South Bend New Carlisle circuit is shown at *A*. In figure 5 is shown a graphic record of the 132-kv bus voltage at South Bend substation. This meter is equipped with a high-speed trip attachment, but the surge was not sufficient to operate it; however, the record indicates that the fault on the Michigan City-Plymouth-South Bend line caused a voltage dip of some six volts and the reclosure a dip of only about two volts.

In this particular instance, the operators at Michigan City did not know that the South Bend-New Carlisle line had tripped out until some time later when the operators at South Bend and Michigan City were interchanging data on relay targets. This is no reflection on the alertness of the operators but rather, as I see it, a compliment to the ultrahigh-speed reclosing equipment.

Previous calculations had incidated that there would be no difficulty in reclosing this circuit when carrying a load of 80,000 kw over it and this operation bore this out. This operation, it is believed, was an excellent demonstration of the possibilities of ultrahigh-speed reclosing on important high-voltage ties.

With reference to the high-speed reclosing on the Twin Branch-Fort Wayne circuits, it is of interest to note that a load is tapped off one of these circuits midway between Twin Branch and Fort Wayne. During the reclosures that have occurred on this line, the loads fed by this tap have not been disturbed.

The authors in conclusion 3, in discussing improvement to service due to high-speed reclosing, point out that it is apparently possible to reclose even when operating near the stability limit of the line. It is perhaps pertinent to point out that the time available for reclosing of a tie line between

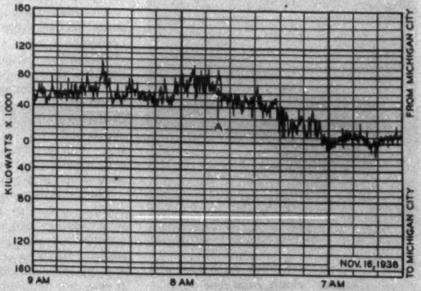


Figure 4. Power interchange on South Bend-Michigan City tie recorded at New Carlisle substation

Reclosure at *A*

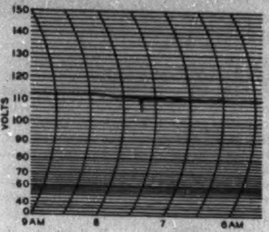


Figure 5. The 132-kv bus voltage at South Bend substation

Voltage dip not sufficient to operate high-speed trip on voltmeter

two systems without losing synchronism is dependent upon several factors, one of which is the relative inertias of the two systems. Whereas as much as 30 cycles might be available when reclosing between two large systems, calculations indicate that something under ten cycles would be available when reclosing, for example, between a hydro plant and a large steam system, if reclosure is to be made without losing synchronism. However, there has been some experience in reclosing and holding the systems together even when they are out of phase. Further experience may indicate that there will be no particular difficulty in such cases even with the present speeds available for reclosing.

K. B. McEachron (General Electric Company, Pittsfield, Mass.): It is interesting to compare the data on multiple strokes with the performance figures obtained for successful automatic reclosure operation reported by Messrs. Sporn and Muller. Figure 6 of this discussion is taken from a paper entitled "Multiple Lightning Strokes—II" (K. B. McEachron, AIEE TRANSACTIONS, volume 57, 1938, page 510) and shows the operations of expulsion protector tubes as measured oscillographically in those cases where two or more successive operations were involved. The record shows tube operations on the 132-kv Roanoke-Glenlyn and Roanoke-Danville lines of the American Gas and Electric Company. Table I of the paper referred to shows a total of 184 discharges, which operated protective tubes during the four-year period of study beginning with 1934.

Of these 184 discharges, 52 were multiple, and from figure 6 of this discussion 10 are seen to involve a longer time than 20 cycles, or a little over five per cent of the total strokes which were of sufficient magnitude to cause tube operation, which no doubt also means that sufficient potential was encountered in most cases to have caused an insulator flashover.

If the total time is reduced from 20 cycles to 15 cycles, as an illustration, the number of times in which a single reclosure would not be satisfactory is increased to six per cent. This indicates that the shape of the curve is such as to show that the deionization time becomes controlling rather than the multiple discharges, from the point of view of decreasing reclosing time.

However, I do not believe that the above gives a clear picture of the true situation, because the data which we have available with Boys camera photographs, taken both in Pittsfield and in New York in the Empire State investigation ("Lightning to the Empire State Building," K. B. McEachron, *Journal* of the Franklin Institute, February 1939) since 1934, indicate that an appreciable percentage of the successive discharges of a multiple stroke will be continuing. That is to say, the current will persist for a considerable period of time after the initiating discharge has decreased to a relatively small value. It has been shown, for instance, that currents as great as 250 amperes may persist for as long as 0.4 second.

In the case of data shown in figure 6 of this discussion, the transformer neutrals were grounded, with the result that the direct current of the continuing discharge finds a ready path to ground when interrupted by the expulsion action of the protector tube at the point of inception. With the operation of the circuit breakers, the line is left isolated from ground, if high-voltage switching is used, with the result that the continuing current will hold on as a d-c arc over the faulted insulator string. The occurrence of this type of discharge tends to decrease the number of successful operations to be expected using one reclosure below that which might otherwise have been expected.

Perhaps some indication of the most pessimistic view might be obtained from a consideration of the results of strokes to the Empire State Building, in which, out of a total of 67 photographed, 27 had a duration of more than 0.33 second, or 20 cycles. On this basis, 40 per cent of the breaker operations would not have been successful. However, these results are undoubtedly influenced by the fact that many of the strokes began at the building rather than at the cloud, which is the usual case for transmission lines.

I am inclined to the view that the second oscillogram of the authors' figure 7 is a case of this sort, in which a continuing discharge caused the line to be in a faulted condition at the moment the breaker closed.

It seems to me that, for the present, until more accurate data are available, with a dead time as long as 14 cycles and a total time of 20 cycles, if a fault exists at the time of reclosing, it will be as a rule the result of a continuing discharge. If it occurs a cycle or more after reclosing, then it would appear to be either a multiple discharge or another stroke.

In lieu of any more accurate information, I would suggest a figure of 20 per cent as representing about the number of unsuccessful operations to be expected on the line with which Messrs. Sporn and Muller have experimented, as a result of multiple or continuing strokes.

Although the protector tube has its limitations, it does perform successfully on multiple strokes with long times included between the first and last discharge, and it ought to be free from the effects of continuing strokes on grounded-neutral systems.

It will be of interest to engineers to learn of the further experience which Messrs. Sporn and Muller will secure with automatic reclosing, and I hope that they will continue to secure oscillographic data which will help materially in determining the cause of unsuccessful reclosure.

S. B. Griscom (nonmember, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): The authors have presented most interesting data on a power system operating procedure which is probably destined to find an extensive future use. While calculations can be made to determine whether two parts of a system will be held together after a high-speed reclosure, they do not take the place of the assurance obtained by actual operation and the proof that some obscure factor has not been omitted from consideration.

In reading this paper, I looked over the illustrations before proceeding to the text, and after examining the Fort Wayne oscillograms of the case 8, June 24, 1937, fault, I concluded that this was an example of a restrike due to insufficient deionization time. This was based merely on the fact that in both the initial fault and the subsequent reclosure, the fault was from phase 3 to ground, as evidenced by the lowest voltage being on phase 3.

Upon reading the text, it was found that the authors had concluded that the evidence pointed a repetitive lightning stroke. This conclusion was inferred from the simultaneous oscillogram at Deer Creek where a disturbance on the line-voltage trace was noted three cycles before the actual reclosure. The restrike was interpreted as being due to the residual ionization from the lightning stroke persisting during the three intervening cycles before the circuit breaker reclosed.

It may not be possible to establish definitely whether in this particular instance the

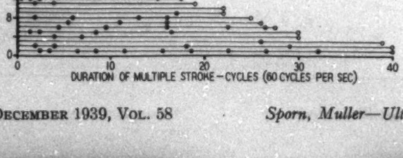


Figure 6

restrike was caused by residual 60 cycle ionization or by residual ionization from a repetitive lightning stroke during the de-energized period. However, the principle is important because if the former, it will have a bearing on the allowance to be made for de-ionization time, while if the latter, since repetitive strokes are a random phenomena, nothing can be done about it.

Mr. Torok and I collaborated on an article "Keeping the Line in Service by Rapid Reclosure" in the May 1933 *Electric Journal*, and a large number of high-speed movies of 60-cycle arcs were taken in the preparation of data. I have just re-examined some of these to get an idea as to the length of time evidence of the 60-cycle arc remained. It was not uncommon to find that incandescent gases remained as long as 30 cycles after the current had been interrupted. However, as the oscillographic results showed, it was rare for the arc to restrike after 12 cycles, although it was a random occurrence. The evidence is that the flashover path gained dielectric strength because the ionized gas had drifted from the proximity of the electrodes by convection currents or action of wind, rather than because the gases had become sufficiently deionized again to withstand the applied voltage.

Such evidence that I have been able to locate indicates a different action in the case of arc paths due to lightning. Messrs. Brookes, Southgate, and Whitehead in AIEE paper 33-44 made tests indicating that power follow from lightning was affected by the magnitude of the 60-cycle voltage at the instant of the surge current. In tests on "De-ion" protectors, it is found that surges taking place near voltage zero are much less likely to result in power follow. This is a somewhat different circumstance, of course, since the discharge is confined. However, both examples seem to indicate that the arc space from lightning strokes recovers dielectric strength much more rapidly than that from 60-cycle arcs being measured possibly in hundreds of microseconds rather than in thousands.

Although the Deer Creek oscillogram for case 8 was not very distinct, there appears to be another surge on the "line de-energized" trace, about four cycles later than the cessation of the reclosure ground current. There also seems to be slight evidence of a counterpart on the Fort Wayne record, but this is only about two cycles later than cessation of the ground current. The duration of the ground currents was not the same at both stations so this may account for the difference. These disturbances are similar to those attributed to the repetitive stroke, which if that theory is correct, may indicate still a third stroke.

Philip Sporn and Charles A. Muller: Mr. Crary's discussion gives some idea of the general groundwork which must be performed to predict results to be expected from ultrahigh-speed reclosure, a very necessary step in justifying it economically. It is interesting to note that in the case of parallel lines equipped with ultrarapid reclosure it may be desirable to delay reclosure for a fault on one line if the total load is below the stability limit of a single line. This condition was considered in the case of the Twin Branch-Fort Wayne lines, but because of the additional complications in control which are involved it was decided to wait until operating experience should more clearly indicate its desirability.

Mr. Thomas has made some stimulating comments as to the possible future lines of investigation in improving the design and effectiveness of ultrahigh-speed-reclosing breaker applications. It is hoped that breakers will soon be available which will considerably reduce the amount of time required to extinguish the arc, and that this will, in turn, allow a reduction in the length of time the line must be de-energized, as Mr. Thomas suggests. This possibility was referred to in Messrs. Sporn and Prince's AIEE first paper on "Ultrahigh-Speed Reclosing of High-Voltage Transmission Lines," in which tests were cited that indicated that by reducing the arcing time from eight cycles to two cycles the time required for the line to be de-energized was reduced by about 25 per cent. In regard to Mr. Thomas' suggestion of employing a "rate-of-current-rise" relay as a possible means of decreasing somewhat the relay time required the authors believe that the present relay time of one cycle could not be materially reduced, and, since this is only a small portion of the total arcing time, any small gains in this time would be of minor importance in the over-all reclosing cycle.

Mr. Marquis has described an additional reclosure to those listed in the paper, which probably represents the most severe operating condition so far encountered. The fact that this reclosure was successful and it had been indicated from previous calculations that it would be, gives added confidence in the accuracy of the preliminary analysis as well as concrete evidence of the value of the equipment.

Mr. George brings out the point that the Tennessee Electric Power Company has been employing immediate reclosing as standard practice on all transmission distribution lines for the past five years, and that his company now recloses immediately both ends of tie lines equipped with carrier current or pilot wire relay systems. This is excellent operating practice but it needs to be emphasized that immediate reclosure at best is from two or three times longer in time than ultrarapid reclosure on high-voltage transmission lines, and that with this longer time of interruption, synchronism would probably invariably be lost between systems not having an extremely large amount of inertia if the line is carrying any appreciable load. However, it can still be of value if the systems have the ability to pull in step even though closed out of phase, as pointed out in Messrs. Crary's and Marquis' discussions.

From Mr. Griscom's comments on figure 8 of the paper, it appears that he has concluded as a result of laboratory tests that the cases where the 60-cycle power arc will restrike after 12 cycles would probably be extremely rare. It is especially interesting that he apparently believes the arc will not restrike after 12 cycles because of the probability that the ionized gases will have been swept out of the path along which the arc would probably be established rather than due to the absence of deionization. As Mr. Griscom points out, it is important to know whether the reestablishment of the arc is caused by a continuation of the 60-cycle ionization or by a multiple lightning stroke, or, as Mr. McEachron also suggests, by the possibility of a continuous lightning stroke. Incidentally, Mr. McEachron's discussion brings up a most important point in connection with the entire problem of lightning protective apparatus and the effect on it of the continuous lightning stroke. It is obvious that a great deal of our thinking as regards the requirements of lightning protective equipment will have to be considerably modified if such equipment is actually being subjected to continuous flow of current from lightning strokes of from 0.3 to 0.4 second.

It will be interesting to observe the actual operating data as it is gathered over the years and determine how the percentage of unsuccessful operations actually obtained compares with the 20 per cent which Mr. McEachron suggests as being probable. As has already been indicated heretofore, that has been about the percentage of unsuccessful performance, but there seems to be reason for belief that the percentage might be reduced with the speeding up of the breaker process. Operating performance, however, over the next few years ought to throw further light on this.

The authors were very glad to learn through Mr. Peterson of the work his company is doing toward developing another type of ultrahigh-speed reclosing breaker. While he has not mentioned the operating time for this mechanism, it is to be presumed that it will be as fast or faster than those described in the paper. It will be of considerable interest to learn more about this development and whether the design will permit modernization of existing breakers or will only be applicable to new ones. Actual operating results will also be awaited with interest.

Mr. Prince has reviewed the factors that have to be kept in mind in a system designer's attempts to continue to provide better service while reducing the cost of rendering it. The ultrarapid reclosing breaker seems to be one of the most valuable tools developed in many years that can be utilized in accomplishing this. But, of course, more experience will be required before one will be able to evaluate accurately all its advantages and disadvantages.

Out-of-Step Blocking and Selective Tripping With Impedance Relays

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WHEN two interconnected power systems pull out of step the relative values of voltages and currents are such as to indicate a three-phase fault somewhere in the interconnection, which if in the zone of high-speed impedance or reactance relays will cause them to trip their associated oil circuit breakers. Although this phenomenon has been recognized for some time, and various blocking schemes suggested,[3,4] it has been generally accepted that when high-speed relays are involved, carrier current or pilot wires giving simultaneous control of the relays at both ends of a protected section are required to discriminate between a fault and an out-of-step condition.[2,7] This paper describes a scheme developed for an important tie circuit of the Gulf States Utilities Company, whereby out-of-step blocking and selective tripping are obtained with existing impedance relays and without the use of carrier current or pilot wires.

The Problem

The intervening load between the Neches and Baton Rouge generating stations of the Gulf States Utilities Company is supplied duplicate power by means of a 66-kv sectionalized tie circuit between the two stations as shown geographically in figure 1, and schematically in figure 2. Normally, power is fed into both ends of the line and tapped off to loads at each sectionalizing point. The division of load between the two generating stations varies with the season, depending on the total system load, steam requirements, etc. In case of a fault on this line, the faulted section is segregated and the two systems continue to supply uninterrupted power to the intervening load by operating independently until the fault is cleared and the interconnection re-established.

The high-speed impedance line relays and directional ground relays located at each sectionalizing point clear faults on the tie line with very little disturbance to the loads. However, opening the interconnection may cause an excessive load shift on the two plants, depending on which section is segregated. It is therefore important that the interconnection be opened only when absolutely necessary, and then, if possible, at a selected point.

Transmission-line faults, other than those which can be prevented by reasonable measures, will occur occasionally and must be tolerated. However, in addition to faults, instability occasionally develops due to disturbance elsewhere on the system, and the Neches and Baton Rouge plants pull out of step, causing the relays to open one or more sectionalizing breakers. Opening the interconnection at one point because of an

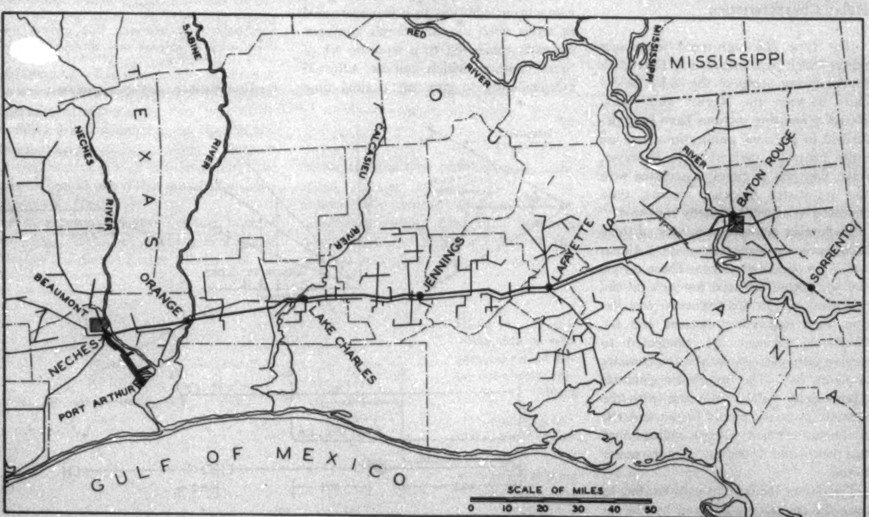

Figure 1. Interconnecting 66-kv transmission circuit between Neches and Baton Rouge steam generating stations

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Paper number 39-81, recommended by the AIEE committee on protective devices, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted January 31, 1939; made available for preprinting March 13, 1939.

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The authors wish to acknowledge the valuable suggestions of W. A. Lewis, formerly of the central-station engineering department, H. A. Travers of the switchgear engineering department, and members of the relay engineering department of the Westinghouse Electric and Manufacturing Company. They also express their appreciation to R. O. Hopkins of the Gulf States Utilities Company for his assistance in making the field tests and preparing the data for the paper.

1. For all numbered references, see list at end of paper.

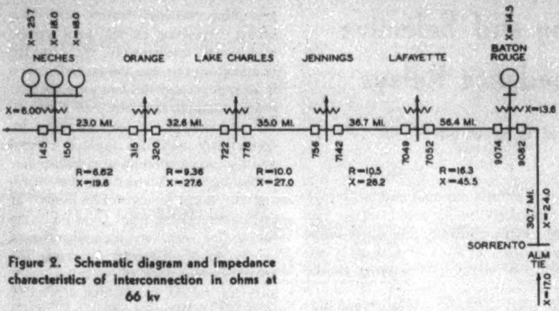


Figure 2. Schematic diagram and impedance characteristics of interconnection in ohms at 66 kv

out-of-step condition does not cause a loss of power to any load but is undesirable because of the usual change in load division between the two systems. Opening more than one breaker in other than one section would cause a loss of all load between the two breakers. The problem was therefore to prevent more than one breaker from opening during an out-of-step condition and to make it possible to select the breaker to be opened under the particular operating condition. Furthermore, it was desired to accomplish the out-of-step blocking and selective tripping with a minimum change to the existing relay equipment and without the addition of carrier current or pilot wires.

Relay Characteristics

The type *HZ* high-speed impedance relays which were installed in 1931 at all sectionalizing points of the 66-kv tie circuits between the Neches and Baton Rouge generating stations have been described in previous publications and are quite generally understood.[1] However, since they are so closely associated with the out-of-step blocking scheme, their characteristics will be briefly reviewed.

Each relay consists essentially of three high-speed impedance elements, a timer, and a directional element. Three relays, one per phase, are used for each oil circuit breaker. In this particular installation, each impedance element and the directional element are connected to receive delta current as shown vectorially in figure 3*A*. The impedance elements receive delta voltage in phase with the currents at unity power factor and the directional element receives voltage that lags the current 60 degrees at unity power factor.

The pull of the current coils tending to close the contacts is opposed by the restraining pull of the voltage coils so that the net force on the impedance elements is proportional to the ratio of voltage to current as shown in figure 3*B*. The curves indicate the relative balance points of the impedance elements, that is, the ratio of voltage to current at which the pull of the current coil just overcomes the voltage restraint. At any ratio less than the balancing ratio the contacts will close and at any ratio greater, the contacts will be held open. Any one of the three impedance elements will close its contacts in one cycle or less for a voltage-current ratio within its balance point. The first impedance-element contacts are connected in series with the directional contacts to close the trip circuit instantly. However, the second and third element contacts, in addition to being connected in series with the directional contacts, are each connected to a separate set of timer contacts which can be adjusted independently to give any desired time delay up to 180 cycles. The timer motor is started by the operation of the third impedance element.

The ratio of the voltage to the current at the relay location during a line fault is equal to the impedance to the fault so that the impedance elements can be set to balance at any desired impedance within their range. As shown in figure 3*D*, the first element Z_1 is set to balance at 80 per cent of the first section impedance, the second element Z_2, at 50 per cent of the second section impedance and the third element, Z_3, at 25 per cent, of the third section impedance. The first element of all relays trips in one cycle or less. The time settings of the relays at the different locations vary from 20 to 35 cycles for the second elements and from 100 to 120 cycles for the third elements.

It is evident from figures 3*B* and *D* that any voltage-current ratio within the balance ratio of Z_1 will close the contacts of Z_1, Z_2, and Z_3. Any ratio greater than the balance point of Z_1, and less than that of Z_2 will close Z_2 and Z_3 contacts. Any

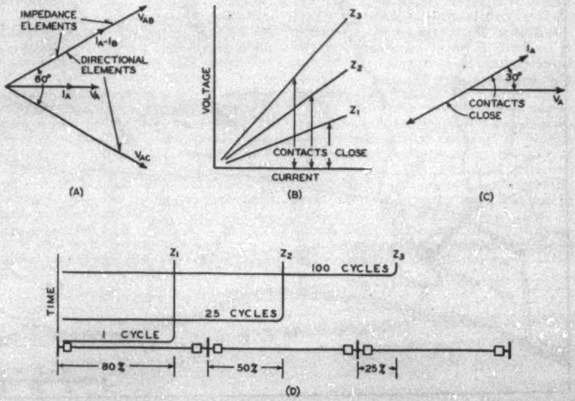
Figure 3. Operating characteristics of HZ impedance relays installed at all sectionalizing points

A—Vector relationship of voltages and currents applied to relay elements at 100 per cent power factor

B—Relative characteristics of impedance elements

C—Angle zone between line current and line-to-neutral voltage that directional contacts close

D—Time-distance setting of relays



ratio greater than the balance point of Z_2 and less than that of Z_3 will close Z_3 only.

The directional element has a true wattmeter characteristic, that is, there will be a closing torque on the contacts when the current applied to the element either leads or lags the applied voltage by less than 90 degrees. Since the directional element is connected to receive delta voltage which lags the applied delta current by 60 degrees at unity power factor, the phase relation of the phase current with respect to phase voltage for a closing torque is 30 degrees lead to 150 degrees lag as shown in figure 3*C*.

From the above, it can be seen that the manner in which the impedance relays operate during an out-of-step condition depends on the ratio of voltage to current, the relative phase angles of the voltage and current, and the length of time that the contacts of the impedance elements remain closed.

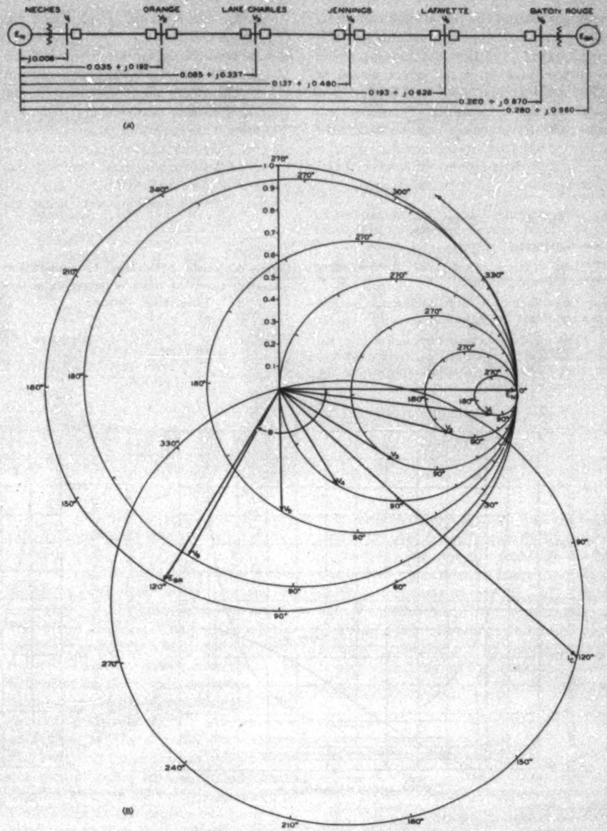


Figure 5. Circle diagrams of current and voltages at relay locations

A—Unit impedances to sectionalizing stations

B—Circulating current I_c, and line-to-neutral voltages at sectionalizing stations for complete slip cycle between Neches and Baton Rouge. Vectors are shown for 120-degree displacement angle

Analytical Investigation

In order to determine the effect of an out-of-step condition on the relays at the various sectionalizing points, an analysis was made of system voltages and currents during a complete slip cycle between systems. This information is also supplemented with oscillograms obtained with an automatic oscillograph located at the Neches generating station.

The part of the system considered consists of the Neches generating station at Beaumont, the 66-kv sectionalized tie circuit, and the generating station at Baton Rouge which is normally tied in with the Arkansas-Louisiana-Mississippi system at Sorrento. The impedances in ohms at 66 kv are shown in figure 2. A typical operating condition is two generators, number 1 and number 2, at Neches and two generators at Baton Rouge. Because of the relatively high line impedance the results would not differ greatly for other generating conditions.

The internal voltages (voltage back of transient reactance) of the generators at Neches and Baton Rouge, all of which are of the turbine type provided with high-speed voltage regulators, will remain substantially constant during a fault or out-of-step swing. Also, since power is normally fed into the line at both ends, the internal voltages at both stations will be about equal. The load current or line-charging current during the fault or out-of-step period will be relatively small. When the out-of-step condition is caused by an external fault located near one of the generating stations, the 66-kv bus voltage will drop so that, if the fault is of long duration, it will have considerable effect on the circulating current and line

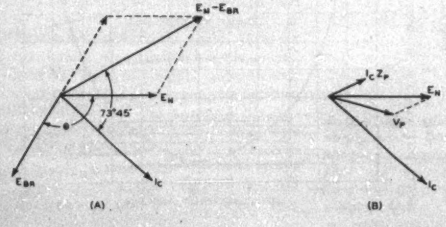


Figure 4. Vector relationship of voltages and current during out-of-step conditions

A—Circulating current I_c for displacement angle *θ*

B—Voltage at point *P* for circulating current I_c

voltages during the out-of-step condition. However, since the fault would normally be cleared by the time the actual pull-out occurred, and since the effect would be different for each fault location, it was neglected in the analysis. The effect for any special condition can be estimated fairly closely from the curves shown.

The following assumptions were therefore made:

1. The internal voltages of the generators at both stations remain constant and equal in magnitude.
2. The load current and line-charging current are negligible.
3. The effect of a fault during the out-of-step condition is neglected.
4. The unit impedances from the internal voltage, E_N, at Neches to the equivalent internal voltage E_{BR}, at Baton Rouge are as shown in figure 5*A*.

As the two systems swing apart, the difference voltage, $E_N - E_{BR}$, between the internal voltages at Neches and Baton Rouge will cause a circulating current $E_N - E_{BR}/Z$ to flow, where Z is the total impedance between E_N and E_{BR} including the generator transient reactances. The voltage and current will be balanced between phases so that only one phase need be considered.

The relation existing between the internal voltages, the difference voltage, $E_N - E_{BR}$, and the circulating current, I_c, is shown vectorially in figure 4*A*. The circulating current I_c, which is shown vectorially equal to $E_N - E_{BR}/Z$, may also be expressed mathematically as,

$$I_C = \frac{E(1 - \cos\theta - j\sin\theta)}{Z} \qquad (1)$$

where E_N and E_{BR} have the same magnitude E, and θ is the angle that E_{BR} lags E_N. The angle of 73 degrees 45 minutes that I_c lags the difference voltage is the natural impedance angle of the system.

The current I_c will be common throughout the tie circuit and is the current at each relay location for any given displacement angle, θ. However, the voltage will vary with location and at any one point, P, the line-to-neutral voltage is

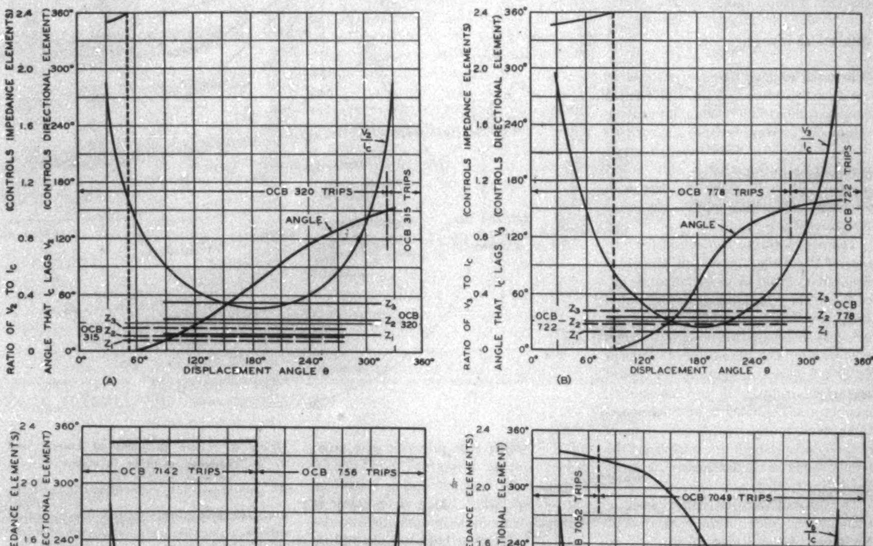


Figure 6. Ratio of voltage to current and operating zones of impedance relays at sectionalizing stations

A—Orange
B—Lake Charles
C—Jennings
D—Lafayette

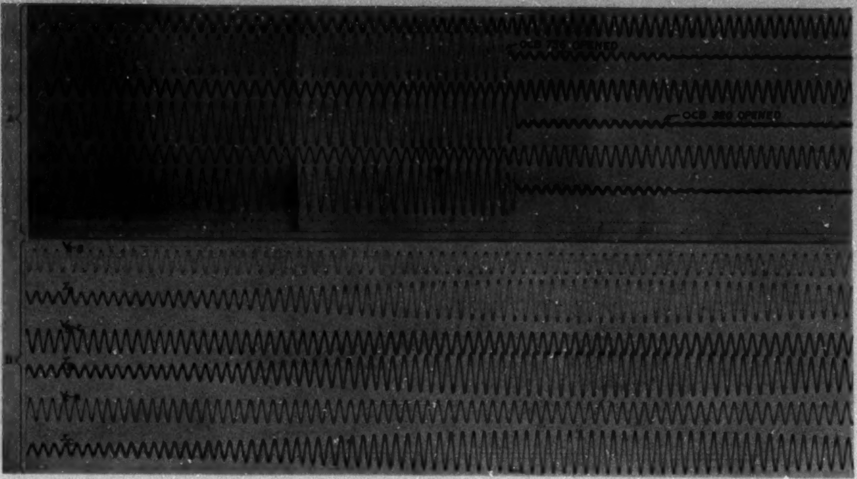


Figure 7. Typical oscillograms of voltage and current at Neches 66-kv bus during system disturbance

A—Out-of-step condition caused by disturbance on Baton Rouge end of system external to interconnections. Breakers 756, 778, 320 opened

B—Severe system oscillations caused by disturbance on Baton Rouge end of system external to interconnection. No breakers opened

equal to $\boldsymbol{E}_N - \boldsymbol{Z}_P$ where $\boldsymbol{Z}_P$ is the impedance from E_N to P. This relation is shown vectorially in figure 4*B*. It may also be expressed mathematically as,

$$\boldsymbol{V}_P = E\left[1 - \frac{\boldsymbol{Z}_P}{\boldsymbol{Z}}(1 - \cos\theta - j\sin\theta)\right] \quad (2)$$

The voltage and current at any point on the line for any displacement angle can be obtained either graphically as shown in figure 4, or analytically by means of equations 1 and 2. The loci of $\boldsymbol{I}_c$ and the line-to-neutral voltage vectors for a complete slip cycle are shown plotted in figure 5*B*. The vectors, shown for 120-degree displacement angle, represent unit values, where unit voltage is normal internal phase-to-neutral voltage, unit impedance is the total impedance between E_N and E_{BR}, and unit current is the current that would flow if $E_N - E_{BR}$ were equal to unit voltage.

It will be noted that the locus of the current vector, $\boldsymbol{I}_c$, is a circle with its center displaced one unit from the origin, that is, at $r - jX$, where r and X are the unit values of resistance and reactance corresponding to Z.

The loci of the voltage vectors will also be circles with their centers located at $1 - Z_P(r - jX)$ units from the origin. The phase angle and magnitude of the current or voltage at a sectionalizing point is equal to a vector drawn from the origin to a point on the respective circle corresponding to the displacement angle. The circulating current, $\boldsymbol{I}_c$, will increase to a maximum at 180 degrees displacement and back to zero as the two systems slip one pair of poles, that is, 360 electrical degrees, whereas the voltage decreases to a minimum at 180 degrees and back to a maximum at zero displacement.

It is evident from figure 5 that the voltages at the generating station 66-kv busses, V_1 and V_6, will not drop enough during out-of-step conditions to cause the relays at those locations to operate. The voltages will, however, drop appreciably at Orange, Lake Charles, Jennings, and Lafayette, particularly at Jennings which is almost exactly at the electrical center of the interconnection. The ratio of the

Switch Position	Switch *A*	Switch *B*	Operation
1	Open	Closed	Nonblocking
2	Open	Open	Out-of-step blocking
3	Open	Closed	Preferred out-of-step tripping

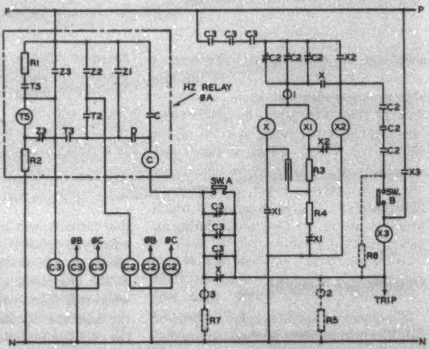


Figure 8. Schematic diagram of relay control circuit. Dotted connections show test connections of oscillograph galvanometers

voltage to current and the phase relation between them for these four points are therefore plotted against displacement angle, in figures 6*A*, *B*, *C*, and *D*. The actual settings of the impedance elements expressed as a ratio of voltage to current, and representing the balance points of the elements with the current lagging the voltage 60 degrees are also shown on the same curves. The shift in balance point with large changes in angle between the voltage and current is not shown, but if desired can be taken into account from available calibration curves on the *HZ* relay. From the curves of figure 6, it can thus be determined which breakers are subject to trip-out during an out-of-step condition.

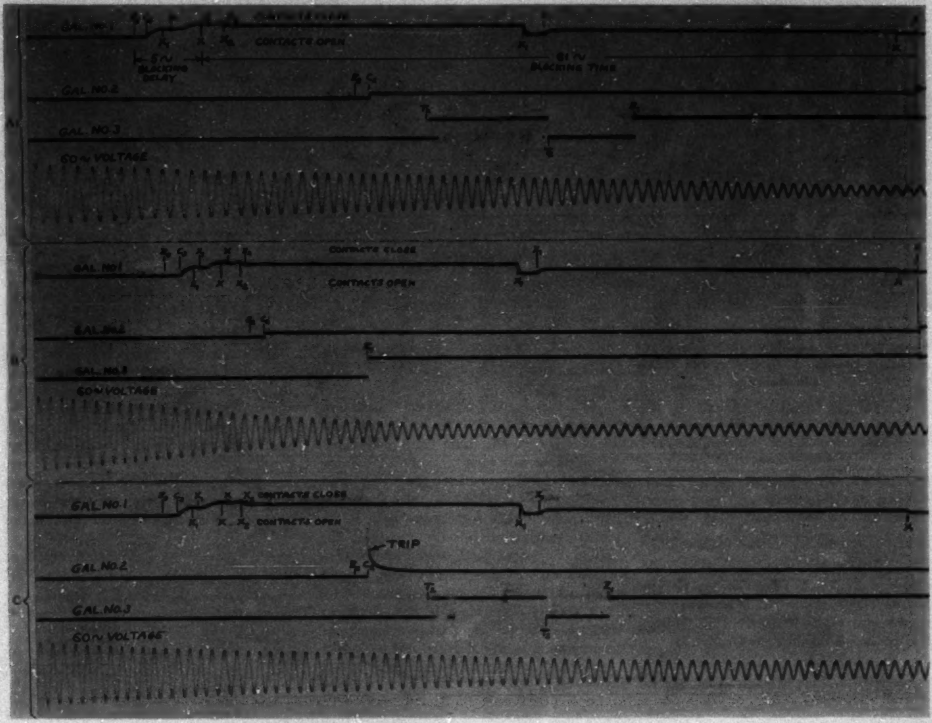


Figure 9. Oscillograms showing sequential operation of relays during simulated out-of-step conditions

A—Out-of-step blocking for slow pull-out

B—Out-of-step blocking for fast pull-out

C—Selective tripping during pull-out

Oscillograph Records

A number of oscillographic records of out-of-step conditions and system oscillations, occuring over several months' operation previous to the installation of the blocking relays, have been obtained by means of an automatic oscillograph located at Neches. The oscillograph, which was connected to record the 66-kv bus line-to-line voltage and the 66-kv tie-line currents, was initiated by an impedance-type relay. Although the oscillograms do not show the actual voltages at the intermediate sectionalizing points where the relays are most affected, they do show the circulating current I_c, and the voltage at Neches. These values can be interpreted from figure 5 and figure 6, to indicate the voltage and phase angles at the sectionalizing points.

Two typical examples are shown in figure 7. Oscillogram *A* shows the results of a pull-out due to a disturbance on the Baton Rouge end of the system, external to the interconnection which resulted in breakers 320, 778, and 756 opening. As shown by the oscillogram, at the time the oscillograph was initiated, the line voltage V_{B-A} was slightly lagging I_A, that is, I_A was lagging V_A by about 120 degrees, which from figure 5, indicates a displacement angle of 260 degrees. It will be noted from figure 6*C*, that at 260 degrees displacement, the Z_3 element of breaker 756 is nearly at its balance point. At the time breaker 756 opened, the current I_A was lagging V_A by about 90 degrees which indicates a displacement angle of 210 degrees, sufficient to cause tripping of the Z_2 element, or possibly Z_1 element.

This also shows that E_{BR} was advancing in phase position with respect to E_N, which might be expected with a disturbance near Baton Rouge. The Z_2 element of breaker 778, and the Z_3 element of breaker 320, both balance at about 220 degrees, and would therefore have closed their contacts shortly before 756 tripped. The total time shown on the oscillogram to the opening of breaker 756 is 39 cycles, which, allowing 15 cycles for breaker operation, would leave 24 cycles or just about the timing of Z_2 for breaker 756. Also, the Z_1 contacts for breaker 756 would have operated about that time, so that either the Z_2 contacts or the Z_1 contacts might have actually tripped the breaker. The timers for breaker 778 would have reached their second-zone contacts before breaker 756 opened and would therefore cause tripping on breaker 778. Breaker 320, which opened 15 cycles after breaker 756, was evidently tripped by its Z_3 contacts, just before 756 opened. Inspection of the relay that tripped breaker 320 showed that the Z_3 timer contacts had undoubtedly closed prematurely due to break contact on the Z_3 element being out of adjustment, which on the early design of *HZ* relay would energize the timer motor.

Oscillogram *B* shows oscillations between Neches and Baton Rouge, also due to a disturbance on the Baton Rouge end of the system. The disturbance in this case was not severe enough to cause the two systems to pull out of step. This oscillogram is of interest because it shows the natural period of oscillation.

Blocking-Relay Scheme

It is evident from the foregoing that the voltage and currents applied to the impedance relays during some part of a slip cycle may have the same relative values as for line faults located within the operating zones of the relays. It is also evident, however, that the ratio of voltage to current gradually decreases so that it comes within the zone of Z_3 first, then Z_2, and finally Z_1 if the relay is located near the electrical center. Relays located farther from the electrical center will close only their Z_3 and Z_2 contacts in succession. Also, since the out-of-step condition produces balanced voltage drops, the impedance elements of all three phases will operate in the order, $Z_3 - Z_2 - Z_1$ and then reset in the reverse order, that is, $Z_1 - Z_2 - Z_3$, to complete the slip cycle.

When a line fault occurs, the ratio of voltage to current at the relay reaches its final value almost instantly and remains substantially constant during the operating time of the relay. The basis of the blocking scheme is therefore the sequential closing of the impedance elements of all three phases during a slip cycle. The operation of the Z_3 elements of all three phases without the immediate operation of a Z_1 element causes the tripping circuit of the Z_1 and Z_2 elements to be blocked out long enough for the displacement angle to pass through the operating zones of Z_1 and Z_2.

The blocking equipment, as shown schematically in figure 8, consists essentially of the three existing *HZ* impedance relays, two special type *SM* auxiliary relays, two type *SG* auxiliary relays, a capacitor, a resistor, and a control switch. The a-c connections to the *HZ* relays have been omitted from the diagram since they have no bearing on the blocking scheme which functions within the 125-volt d-c control circuit.

One of the *SM* relays consists of three instantaneous voltage elements, C_3, each with make and break contacts, and an instantaneous current element, X_3 with make contacts. The other *SM* relay consists of four instantaneous voltage elements, C_2 and X_2, each with make and break contacts. The two *SG* relays, *X* and X_1 are standard voltage relays used with the capacitor and resistor to provide time delay. The control switch is a three-position switch which can be set to obtain nonblocking, out-of-step blocking, or preferred out-of-step tripping.

When out-of-step blocking is desired, the switch is set on position 2, so that contacts *A* and *B* are both open. As the systems pull out of step and the voltage-current ratio comes within the zone of the impedance relays, the three Z_3 contacts will close, thus energizing the three C_3 contactors which will open their break contacts connected in parallel in the trip circuit and close their make contacts which are all connected in series. The closing of the three C_3 make contacts will energize the *SG* relay, X_1, from positive through the C_3 contacts, the C_2 break contacts and the X_2 break contacts to negative. The capacitor will be partially charged through the *X* coil, but not enough to pick up *X*. The closing of the X_1 contacts will energize the other *SG* relay, *X*, which will open its break contact in the trip circuit. It will also close its make contact to energize contactor X_2. When the break contact of X_2 opens, the coil of *SG* relay, X_1, will be disconnected from negative. However, the charging current to the capacitor will delay its opening several cycles. When X_1 make contact opens, the *X* coil will be disconnected from negative but its drop-out will also be delayed several cycles due to the capacitor charging in the reverse direction. The capacitor-resistor arrangement shown in the diagram will provide approximately 5 cycles delay in the opening of the *X* break contact after Z_3 contacts close and will keep it open approximately 60 cycles, giving an over-all blocking time of approximately 65 cycles.

When the displacement angle reaches the zone of the Z_2 elements they will operate and energize the C_2 contactors which will open their break contacts and close their make contacts. However, with switch *B* open, the C_2 make contacts will have no effect and by that time

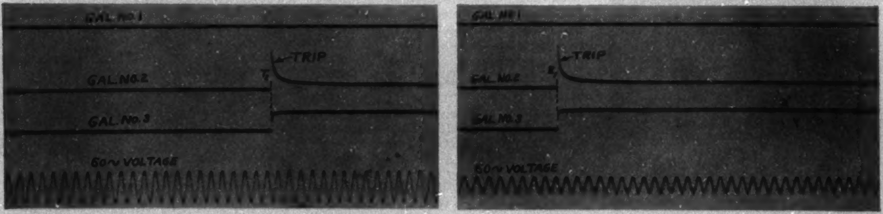


Figure 10. Oscillograms showing operation of relays during simulated fault conditions

A (left)—Three-phase fault in zone of Z_2 and beyond zone of Z_1

B (right)—Three-phase fault in zone of Z_1

the X_2 make contact will be closed so that the opening of the C_3 break contacts will have no effect. Also, during the time the X break contact is open, the trip circuit will be open to Z_1 and the timing circuits of Z_2 and Z_3.

When a single line-to-line fault occurs in any one of the impedance zones, neither all three C_3 elements nor all three C_2 elements will pick up and the *HZ* relays will function in their normal manner. In case of a three-phase fault in the first or second impedance zone, the Z_3 elements will operate and pick up all three C_3 elements. However, the C_2 break contacts will open before relay X picks up and prevent any blocking action. If a three phase fault occurs in the zone of Z_3, the blocking period will be over before the timing period of T_3 elapses and normal tripping will be obtained. If a fault occurs in a blocked section during the out-of-step period, tripping will be delayed until the end of the blocking period.

When preferred out-of-step tripping is desired, the control switch is set on position 3, which opens switch contacts A and closes switch contacts B. The operation of relays will be the same except that when the C_3 make contacts close the trip circuit will be completed through control switch contact B, and the breaker will be tripped without waiting for the directional contacts and either T_2 or Z_1 contacts to close. This is important as the interconnection is opened up at the desired point as soon as the out-of-step zone reaches the Z_3 elements without any respect to the directional element or timing of Z_2, so that the system disturbance is minimized. Fault tripping is obtained in the normal way.

When the switch is in the nonblocking position 1, the blocking contacts are shunted out by contacts A, and contacts B are open, so that the blocking relays have no effect on the operation of the *HZ* relays.

The dotted connections in figure 8 show the scheme that was used to test the timing and sequential operation of the combined relay equipment with an oscillograph. The number 1 galvanometer of the oscillograph recorded the closing of the Z_3 contacts and the sequential operation of the X_1 and X relays. The number 2 galvanometer recorded the closing of the Z_2 contacts by measuring the current flowing through the resistor, R_6, in series with the resistor R_4. It also recorded when the trip circuit closed by the higher deflection caused by the current through R_6 only. The number 3 galvanometer recorded the operation of the Z_1 contacts.

In making the tests, actual out-of-step conditions were simulated as closely as possible. The current coils of the *HZ* relays were energized with a constant 60-cycle current of 8.5 amperes. By means of a variable-voltage test set, the voltage coils were energized with a 60-cycle voltage that could be varied at a rate corresponding to the change in voltage-current ratio during an out-of-step condition. This voltage which was recorded by the number 4 galvanometer was also used as a timing indication on the oscillograms.

Typical results of the tests are shown in figure 9 and figure 10, which show respectively the action of the relays during out-of-step conditions and during three-phase faults within the operating zones of the impedance elements.

The oscillogram in figure 9*A*, which was taken with the control switch on position 2, for blocking, shows the sequential operation of the relays for a relatively slow slip cycle. It will be noted that a delay of five cycles is obtained between the closing of the Z_3 contacts and the

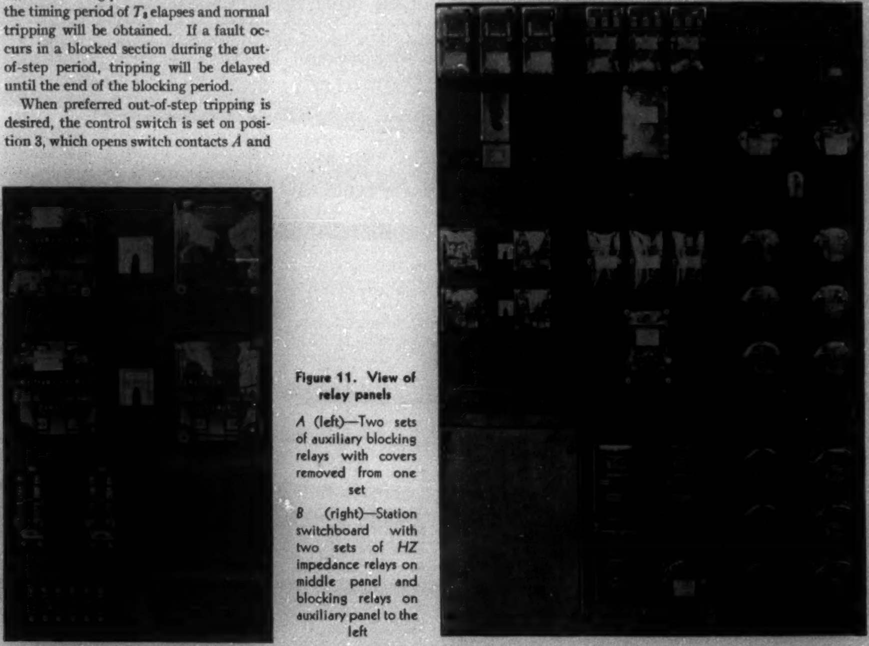


Figure 11. View of relay panels

A (left)—Two sets of auxiliary blocking relays with covers removed from one set

B (right)—Station switchboard with two sets of *HZ* impedance relays on middle panel and blocking relays on auxiliary panel to the left opening of the X break contact in the trip circuit. This is to allow time for the Z_1 or Z_2 contacts to close in case of a three-phase fault within the section and thus not delay normal tripping. The blocking period from the opening of the X break contacts until it closes again is recorded as slightly less than 61 cycles. It will be noted that the number 3 galvanometer recorded the closing of the T_2 timer contacts which occurred before the Z_1 contacts closed.

The oscillogram in figure 9B was taken under the same conditions as figure 9A, except that the 60-cycle voltage was decreased faster to represent a rapid pull-out. In this case the Z_1 contacts closed before the T_2 contacts.

The oscillogram in figure 9C was taken with the control switch in position 3, for preferred out-of-step tripping. As shown by the number 2 galvanometer tracing, the trip circuit closed immediately after the Z_2 contacts picked up and before the T_2 or Z_1 contacts closed.

The oscillograms in figure 10 were taken with the control switch in position 2, for blocking. In A, a voltage was applied to the impedance elements corresponding to a three-phase fault in the second zone. In B, the voltage applied was equivalent to a three-phase fault in the zone of Z_1. Both oscillograms show that the blocking relays did not pick up, thus permitting the impedance elements to trip in their normal manner.

The complete installation consists of eight sets of blocking equipment, that is, two each at Orange, Lake Charles, Jennings, and Lafayette. The only change necessary to the *HZ* relays was to modernize them by providing two additional studs and a switch-controlled timer as shown in figure 8. A view of two sets of the auxiliary blocking relays mounted on an auxiliary panel section is shown in figure 11A. The covers have been removed from the set on the left to show more clearly the different elements of the relays. The selector switches are located between the two sets of relays, as shown. The capacitors and resistors are mounted on the back of the panel. Figure 11B shows a view of a complete installation at one station consisting of two sets of *HZ* relays on the two upper sections of the middle panel, and the two sets of auxiliary relays on the middle section of the left-hand panel.

Conclusion

The method used for calculating line voltages and currents provides a means of determining the performance of distance relays during out-of-step conditions. The automatic oscillograph is also invaluable in studying voltage and current characteristics during the slip cycle.

Out-of-step blocking of high-speed impedance and reactance relays has been available where carrier current or pilot wires are used as a medium of control. The scheme described in this paper can be readily applied to existing installations of impedance relays or used with new installations where carrier current or pilot wires are not available. In addition to blocking, the scheme provides a means of rapid tripping during out-of-step conditions at any desired location which can be selected by means of a selector switch. At the time of writing this paper the equipment was just being installed and had not been put in operation. However, the rigid field tests that were made with simulated out-of-step conditions indicate that the scheme will function as intended.

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Discussion

E. H. Bancker (General Electric Company, Schenectady, N. Y.): For some five or six years prior to 1934 there were many studies of stability in which the effect of relays upon system stability was an important factor. In reference 3 Mr. Hunter and the writer undertook to study the reverse of this situation and to find out what effect instability had upon relays. This was further explored by Mr. Mason in his paper "Relay Operation During System Oscillations," which appeared in volume 56, July 1937, Electrical Engineering, page 823. Reference 2 outlines a study of a specific system from both points of view.

During the course of the investigation of possible means for preventing incorrect relay operation during instability, a scheme was devised which is mentioned briefly on page 194 of reference 2. This scheme is covered in more detail in United States Patent No. 2,030,665. This arrangement is intended particularly for application to reactance-type distance relays and consists in changing the characteristics of the starting unit after the started unit has operated. It prevents relay operation during a system swing when conditions are similar to those existing during a fault, and yet permits relay operation under actual fault conditions.

This paper presents an interesting application to distance relays alone of the principles now in use as a result of these earlier studies for blocking tripping of carrier pilot relaying during system oscillations.

In June 1936 there was published a French patent No. 799,279 which disclosed an arrangement for preventing unwanted operation of impedance relays by utilizing the progressive drop in indicated impedance during a slip cycle in contrast with the immediate drop that occurs when a fault starts.

This paper is an interesting sequel to these others in giving the details of another actual installation and its characteristics during loss of synchronism and in giving in detail the application of the principles now in use, and those outlined in the French patent. Of special interest is the oscillographic record of actual cases of loss of synchronism and bad system oscillations. Such records, in conjunction with the mathematical analysis, make possible still more refinement in designing and selecting protective apparatus in such a way as to increase service continuity.

In view of the very complete research which has already been made and described regarding relay operation during system oscillations not accompanied by a fault on the system, it is regrettable that the authors have neglected to analyze the possible effects of the presence of such a fault during the oscillations. It is fairly obvious that with such a fault, all three phases will not be similarly affected. It is very likely that the Z_3 impedance element of one phase will operate in a much different fashion from the Z_3 elements in the other two phases. How this will affect the functioning of the scheme described by the authors, wherein the simultaneous operation of all Z_3 elements has been assumed, should be analyzed carefully to prove its dependability under all practical operating conditions. It is to be hoped that a sequel to this paper will appear with the oscillographic evidence to indicate the success attained.

H. R. Vaughan: Mr. Bancker's discussion emphasizes the statements made in the opening paragraph of the paper regarding the previous recognition of out-of-step phenomena and the generally accepted conclusion that carrier current was required to prevent out-of-step tripping where high-speed relays were involved.

Even before 1934, the effects of instability on distance-type relays were quite well recognized. In 1932, I had an opportunity to assist in investigating on the network analyzer, the instability influences on distance-type relays in operation on a specific system. The results of the investigation showed conclusively that certain conditions of instability would cause the relays to operate and accounted for questionable relay operations that had been experienced. This condition was overcome by speeding up other relays to prevent instability. The paper that Mr. Bancker and Mr. Hunter presented in 1934 as well as the later papers referred to are valuable contributions toward a better understanding of the effects of out-of-step conditions on distance-type relays.

Mr. Bancker refers to a scheme intended particularly for reactance relays that was mentioned briefly on page 194 of reference 2 and covered in more detail in United States Patent No. 2,030,665. It is stated on page 194 of reference 2 that the scheme appeared doubtful and that carrier-current control was adopted instead, thus bearing out the statement in our paper that carrier current was considered necessary to provide out-of-step blocking.

Regarding the effects of the presence of a fault during system oscillations, it is recognized that the majority of pull-outs are caused by faults. However, as stated in the paper, the fault would normally be cleared by the time actual pull-out to the point of relay tripping occurred, so that the operation of the blocking relays would not be appreciably affected. This is borne out by the automatic oscillograms, two of which are shown in the paper. The voltages and currents shown by these oscillograms which were taken during pull-outs caused by system faults external to the interconnection, check closely the values calculated by neglecting the effects of the fault.

The effects of long-duration faults external to the interconnection could be readily determined by means of the a-c network analyzer, or more laboriously by calculations. In general, the electrical center of the interconnection would be shifted toward the fault. If the fault were phase to phase, the fault current fed through the interconnection might cause the Z_3 element of the relay associated with the faulted phases to pick up. However, the generators nearest the fault would normally advance in phase angle so that the circulating current due to the out-of-step condition would counteract the current supplied through the interconnection to the fault. This would tend to equalize the impedances indicated to the three relays so that all three Z_3 elements should pick up before any one relay could cause tripping.

The blocking scheme is based on all three Z_3 elements being in the operated position at one time, but it is not necessary for them to pick up simultaneously. The pick up of one or two Z_3 elements before all three Z_3 elements picked up would not prevent blocking. It is only necessary that all three Z_3 elements be in the operated positions before one of the Z_1 contacts and its associated directional contact close simultaneously, or before one of the Z_2 contacts and its associated timer and directional contacts close at the same time. It is believed, therefore, that the blocking relays will function properly even though an external fault exists at the time of an out-of-step condition. As operating experience and additional oscillographic data are collected it may be necessary to alter some of the Z_2 and Z_3 settings or to change the timing periods of the blocking relays.

Improvements in the Construction of Condenser Bushings

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Synopsis: Condenser bushings use paper to provide high dielectric and physical strength. The value of paper as an insulation is recognized by its accepted use as the insulating medium in cables, transformers, and other apparatus. It is also recognized that unlimited life is attained when proper protection is provided against external influences. Such protection is obtained by improved methods of winding the capacitors, treatment with oil impregnation, and surface varnish. Further protection is provided for outdoor bushings by sealing the condenser in a weather casing of porcelain with improved flexible caps and gaskets. Insulating oil or a plastic heavier than water is available for encasing the capacitor.

MANY thousands of transformers and circuit breakers installed during the past 30 years have been equipped with condenser-type bushings. This type of construction has been used here and abroad because first, the distribution of the voltage stresses through the bushing is such as to lower the concentration of such stresses and thereby increase the resistance of the bushing to dielectric breakdown, and second, it has great mechanical strength and resiliency. The condenser bushing is composed of paper and an organic bond. Here, as in transformers, capacitors, cables, and other high-voltage apparatus, paper provides the high mechanical strength and high electrical, especially impulse strength. In winding the bushing the paper is divided into a number of condensers by the insertion of foil layers at intervals selected to provide the proper distribution of the voltage stresses. These well-known principles have been in use since the first condenser bushings were manufactured.

It is recognized that the life of paper as an insulating medium is without limit as long as it is retained in its original state. The greatest enemy to the life and dielectric strength of such insulation is moisture. The manufacturer and the user have both aimed for such improvements as would definitely retain the original characteristics of the paper. Recent improvements in the construction of condenser bushings have markedly increased their resistance to the effects of moisture.

In a condenser bushing, the condenser—or core, as it is sometimes called—is the insulation member. The first step in improving the moisture-resistant characteristics of the bushing is to make the condenser tight and homogeneous. This was found to be a matter of processing, requiring a definite proportionment of bonding material to the weight of the paper, as well as a more rigid control of the relative speed, pressure, temperature, and tension during the winding process. That such tight bushings can be made is indicated by the fact that in

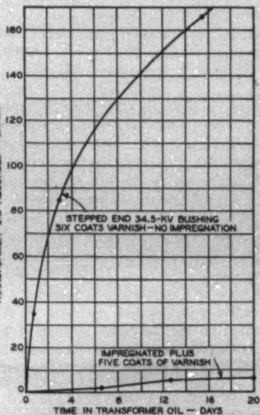

Figure 1. Effect of oil impregnation in preventing oil travel in condenser bushings

Condensers immersed in transformer oil at 80 pounds per square inch and 80 degrees centigrade

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Paper number 39-88, recommended by the AIEE committee on protective devices, and presented at the AIEE South West District meeting, Houston, Tex., April 17-19, 1939. Manuscript submitted March 31, 1938; made available for preprinting March 15, 1939.

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The writer gratefully acknowledges the assistance of H. J. Lingal in preparing this paper and of C. F. Hill, J. G. Ford, and E. E. Arnold for the tests and data responsible for these developments.

commercial production, bushings must withstand a pressure test of 60 pounds per square inch without leaking.

A polymerizing organic oil is now used to impregnate the condenser to considerable depth in order to increase its resistance to moisture. The condenser is then cured by an appropriate time and temperature process, which converts the oil from a liquid into a permanent solid by a dual process involving both oxidation and polymerization. This treatment also increases the effectiveness of the varnish by providing for it an improved base.

The oil used for impregnating the condenser was selected after an exhaustive investigation of many materials. The choice was made on the basis of its power of penetration, ease of curing, electrical characteristics, and effectiveness as a seal against both oil and water. Comparative data were obtained by testing sample 34.5-kv bushings with various impregnating materials, times of curing, and other processing details. These data would be interesting from the viewpoint of chemistry or physics, but for the present purpose it should be sufficient to say that the oil now in use was found to be superior in the essential features.

The effectiveness of the impregnating oil treatment on sealing the pores of the condenser is shown by figure 1. The data for these curves were obtained by immersing duplicate bushings in transformer oil at 80 degrees centigrade, with an applied pressure of 80 pounds per square inch, for 19 days. The weights of the bushings were measured at intervals in order to determine the progressive absorption of the hot oil. The oil-absorption capacity of the untreated bushing was determined by test and calculation to be slightly greater than 400 grams, so the absorption of approximately 6 grams by the treated bushing after 19 days must be regarded as negligible. This test is very exaggerated and is no measure of the absorption of the bushing under ordinary operating conditions, but serves under accelerated conditions to demonstrate the relative improvement obtained by the new oil-impregnating treatment.

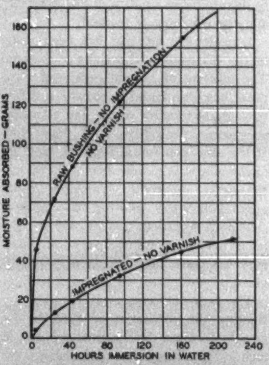

Figure 2. Effect of oil impregnation on rate of moisture absorption; 34.5-kv condensers stepped on one end, immersed in water

Similar tests were performed to determine the effect of the oil impregnation on the absorption of free moisture. Sample bushings were immersed in water, and weighed at intervals to observe the progressive absorption. Of the various tests performed, data showing the relative performance of the impregnated, unvarnished condenser and that of the raw condenser, untreated, were selected for figure 2. In the untreated condenser the moisture is absorbed rather rapidly at first, and then at a decreasing rate. The treated condenser shows a marked reduction in the initial rate of absorption and further slowing up of the absorption process after a few days. This curve indicates that after 19 days total immersion in water the absorption by the condenser was only about one-eighth of its theoretical capacity of approximately 400 grams.

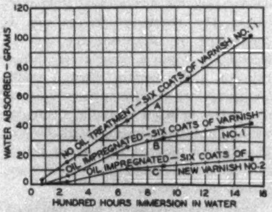

Figure 3. Effect of improved varnish blend and effect of oil impregnation as measured by moisture absorption; 34.5-kv bushings stepped on one end, immersed in water

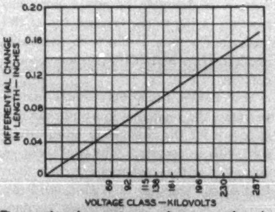

Figure 4. Approximate change in length between condenser core and porcelain casing of bushings of various voltages for 100 degrees centigrade change in temperature

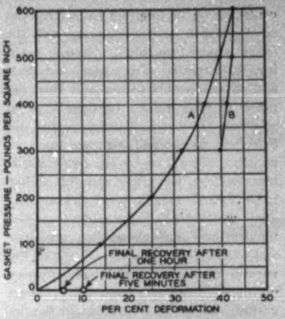

Figure 5. Deformation test on cork-Neoprene gasket material showing compression and return toward original dimension

Another interesting accelerated test was performed by testing bushings under oil at pressures of 800 pounds per square inch. After two hours of such treatment the bushing with only the varnish treatment absorbed ten grams of oil, whereas the oil absorbed by the impregnated bushing was too small to measure.

Obviously, such severe tests serve only to determine and illustrate the efficiency of the impregnating oil treatment in resisting moisture penetration of the condenser, and reflect actual operating conditions only in a greatly exaggerated way.

After the impregnating oil had been selected, research was directed toward obtaining a superior varnish treatment for the surface. The following are some of the more important characteristics which were sought:

(*a*). High resistance to electrical leakage over the surface.

(*b*). Resist the tendency to "tree" or "track" under high potential stress.

(*c*). Seal the bushing against the penetration of moisture, oil, ordinary acid fumes, etc.

(*d*). Resist abrasion such as is met with in normal handling.

(*e*). Provide a hard, smooth surface to avoid holding of dust, carbon and other foreign substances and to facilitate cleaning.

(*f*). It must not crack or craze from age or temperature changes.

Many varnishes show good oil-resistance characteristics, so work was concentrated on finding one having the desired moisture resistance along with the other required qualities. Sample bushings were treated with various varnishes and varnish combinations and then exposed to tests similar to those used in selecting the type of impregnating oil. Selection was based on resistance to moisture absorption by the condenser as measured by weighing, and the ability to withstand various tests and accelerated weathering tests under varying temperature and humidity conditions and ultraviolet exposure. Of course, the varnish must be suitable for ordinary processing. As no commonly known varnish was found to provide all the desirable features, a blend was compounded of materials having individual superiorities, so that the new varnish shows superiority in all requirements. The results of the tests on water absorption are shown in figure 3. Curve *B* shows by comparison with curve *A*, the improvement obtained by adding the oil impregnating treatment to the old varnish treatment. Curve *C* shows the further superiority of the new varnish treatment over the old. The moisture absorption is reduced to about 40 per cent of that in older bushings, by the impregnating oil treatment, and further reduced to less than 20 per cent by using the new varnish blend. An interesting feature of these tests is that even after 63 days total immersion in water, the power factor of the bushing with the improved impregnating and varnish treatment was only 3.36 per cent—still an operative bushing.

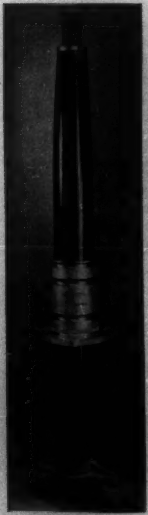

Figure 6. A 23-kv condenser bushing—indoor service

Figure 7. A 69-kv condenser bushing—outdoor service

Treeing, tracking, and crow-footing are terms which have been applied to the phenomenon of creepage occurring on insulating surfaces when the latter are subjected to abnormal electrical stresses. It appears to be initiated by a leakage current along the surface with local concentrations of heat, resulting in gradual thermal breakdown or carbonization of the surface and possibly resulting in the formation of a conducting path. The American Society for Testing Materials test has been found most reliable for determining the arc-resistant characteristics. The resistance to tracking is determined by measuring the time in seconds to form a conducting carbon path on a dry surface when a small, low-energy arc plays continuously across the surface between two point electrodes one-half inch apart, resting on the surface. The varnish selected has an arc resistance equal to the best of the materials applicable as a varnish.

The three processes just described namely, tightness, oil impregnation, and varnish treatment, applied to indoor bushings, provide the moisture-resistance characteristics necessary for the most humid atmospheres. For outdoor bushings, it is desirable to add a porcelain weather casing to the exposed end of the bushing, clamping it down on the bushing flange by a suitable expansion cap and gaskets.

It would be a relatively simple matter to encase the condenser in a porcelain if it were not for the changes in temperature met with in service. The coefficient of expansion of the condenser is greater than that of the porcelain. With a rigid cap coupling between the condenser conductor and the porcelain this difference would be enough to release the pressure on the gaskets at extremely high temperatures.

Figure 4 indicates the differential expansion for bushings of various service voltages. This difference in expansion is taken care of by providing a flexible coupling between the condenser conductor and the porcelain. In the simplest form this resiliency is provided in the cap by shaping it so as to supply the necessary spring action over the range of temperature. In other assemblies the spring element is provided by a separate spring or group of springs enclosed within the cap. With any type of cap it is necessary to design for sufficient length of spring action so that at the lowest temperatures the pressure on the assembly is not excessive, while at the highest temperature, when the differential expansion is the greatest, there remains sufficient pressure to maintain the rigidity of the assembly and the efficiency of the seal.

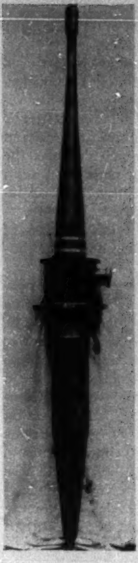

Figure 8. A 138-kv condenser after oil impregnating treatment

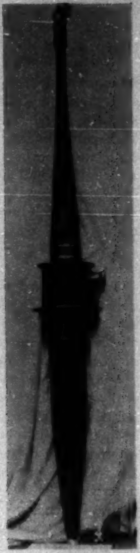

Figure 9. Same as figure 8 except cleaning and applying the varnish

Considerable advance has been made in the materials and designs of gaskets. The mixture of ground cork and Neoprene has been found to provide satisfactory characteristics of oil and moisture resistance, flexibility, and long life. It has also been found that the best life of the gasket is obtained by compressing the gasket to about two-thirds of its original thickness, limiting this compression by a so-called "gasket stop." The latter may take the form of an auxiliary gasket of harder material, or may be provided by a ridge on the gasketed surface.

The best performance is obtained by limiting the maximum pressure on the gaskets to about 300 pounds per square inch. This has been determined by applying various loads and measuring the ability of the gasket to return to the original dimension.

Figure 11 (above). Outdoor condenser bushing assembly being removed from dry-ice bath during temperature-change test

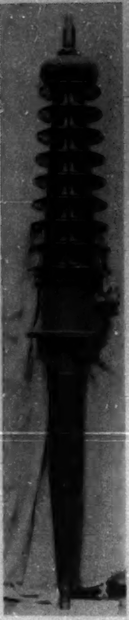

Figure 10. Same as figure 8, completely assembled with weather casing and cap

Figure 5 shows the deformation characteristics of gaskets made from ground cork and Neoprene. The unlimited compression of sample gaskets was measured under various loadings up to 600 pounds per square inch. The return curve *B* was obtained by releasing the pressures, taking readings down to 300 pounds per square inch. Five minutes after pressure was completely released the gasket had returned to within ten per cent of its original thickness and one hour after pressure was released the gasket had returned to within five per cent. It is well known, of course, that with lower pressures, the return toward the original dimensions becomes greater. The pressure of 300 pounds corresponds to about one-third compression, which determines the relative size of gasket stop. The area of the gasket is, therefore, designed to obtain a maximum pressure under lowest temperature and full compression of the spring of approximately 300 pounds per square inch. The data shown in figure 5 illustrate the ability of the gasket to withstand abuse. However, the use of a gasket stop and spring pressure maintained from the cap insures that the gasket is kept at a constant dimension and under a constant pressure. This eliminates any movement of the gasket under temperature change and so contributes to its long life and high efficiency as a seal.

Insulating oil and plastic materials are used for filling the space between the condenser and the porcelain weather casing. A new plastic material heavier than water and highly moisture resistant has been developed for this purpose. In addition to the high specific gravity, other requirements must be met by the material in order to make it suitable for use in bushings. Among the more important are:

1. Specific gravity greater than water throughout the temperature range
2. High dielectric strength
3. Low moisture absorption
4. Not be too fluid at operating temperatures
5. Be plastic at low temperatures
6. Good adhesion to both the condenser and the porcelain
7. Sufficient fluidity to permit easy filling of the bushing

In addition, the material must be stable and neutral in its effect on condenser finishes, gasket materials, and metal parts.

The new plastic material, which has these characteristics, has been tested in the laboratory, and in outdoor service in bushings and with moisture present. In the laboratory tests, 34.5-kv bushings have been operated for over a year with water on top of the filling material, with 70 degrees centigrade flange temperature to soften the compound and at 30 kv. No change was detected in the power factor of the bushing, showing no moisture absorption. In addition, the material itself does not absorb any measurable quantity of moisture.

This material is quite plastic at temperatures as low as −40 degrees Fahrenheit, and will not pull away from the condenser or the porcelain until the temperature has gone down to below −60 degrees Fahrenheit. The material remains sufficiently viscous at 70 degrees centigrade to prevent convection currents, but still can be poured at a temperature of 100 to 125 degrees centigrade.

In addition to the laboratory tests, a large number of bushings using this new filling material have been exposed to the weather for almost a year, operating at a voltage corresponding to the line-to-line voltage instead of line-to-ground voltage. In some of these the caps have been opened up to permit free entrance of moisture to the top of the bushing and some had water introduced on top of the compound at the beginning of the test. Others were sealed up in the conventional manner. This continued operation under higher-than-normal voltage confirms all the laboratory tests, insuring that moisture entering accidentally will be retained in a harmless location at the top

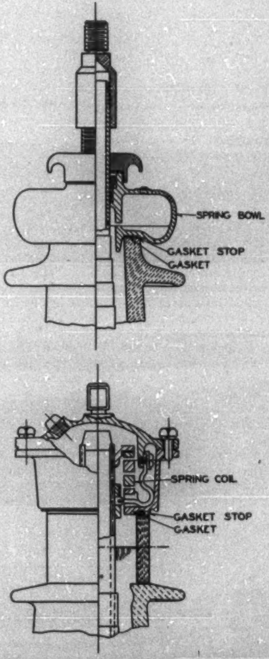

Figure 12. Types of caps showing spring action to maintain rigidity of the assembly and effectiveness of gasket seal

of the bushing and will not get in contact with the condenser itself.

A conventional 115-kv oil-encased bushing with a weather casing containing ten quarts of oil and two quarts of water was operated continuously with 132 kv, twice normal voltage to ground, applied between the bushing terminal and the grounded flange. The bushing stood this test with no measurable change in power factor, so the test was concluded after 60 days.

The primary purpose of these developments was to produce a condenser core which would resist the penetration of moisture. This has been obtained by means of materials, processes, and surface finish. The second step in the development, applicable to outdoor bushings, was to design an assembly which would be effectually sealed against the entrance of moisture to the condenser. This is a matter of proper porcelain, gasket, and cap design. With these conditions fulfilled, it matters little whether the space surrounding the condenser is filled with oil or compounds as this is not required for insulation. As a matter of interest, we have been told of a situation in which a whole set of outdoor bushings is operating with nothing but air filling the space between the condenser and the porcelain, using a heater at the flange to keep the air dry.

Discussion

L. Wetherill (General Electric Company, Pittsfield, Mass.): Mr. Peterson makes the statement that moisture is the greatest enemy to the life of paper insulation. The facts involved might be more accurately represented by stating that moisture has an adverse effect only in cases where the insulation is not adequately protected by suitable long-lived gaskets. It is never desirable to operate a bushing with defective or inadequate gaskets, or with a defective porcelain.

While occasional cases of fractured porcelain can probably never be eliminated, the new gasket materials which have become available in the last ten years offer an effective means of providing effective and permanent protection from moisture. It has been possible to eliminate troubles on the older bushings in service by regasketing. Experience over the last ten years, involving hundreds of thousands of gaskets, strongly indicates that gasketing is no longer a major problem on bushings of modern design.

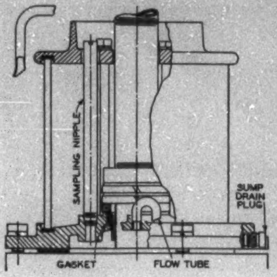

Figure 1. Thermal seal

The experience of the writer agrees with that of Mr. Peterson in that proper control over the materials and conditions during the winding of bushing cores will give a solid and impervious structure. It is the practice of the General Electric Company to require that solid bushing cores must withstand a gas-pressure test of 80 pounds per square inch applied for ten minutes. Experience has shown that a test of this severity assures high quality and long life in service.

There is one additional phase of the differential thermal expansion problem not mentioned by Mr. Peterson. In bushings using solid cores, it is the practice of the General Electric Company to use insulation with coefficient of thermal expansion matched with that of the copper conductor, in order to eliminate internal strains in the core. Such procedure means that a core which is initially tight will remain tight in service.

Mr. Peterson has found that the use of 300 pounds per square inch maximum gasket pressure and about 33 per cent gasket compression give best results. These conclusions are influenced doubtless by the composition and configuration of the gaskets involved. For example, tests on a somewhat harder mixture of cork and Neoprene show that the pressure required for 17 per cent compression varies from 1,100 to 1,900 pounds per square inch, depending upon the dimensions of the gasket.

The trend toward the use of fluid filling compounds in higher voltages is apparently continuing. Fluid compounds do not form shrink voids or show cumulative deterioration as a result of occasional local overstress. They also avoid the danger formerly resulting from the use of solid filling compounds which permitted defective gaskets to remain in service unrecognized.

On the larger oil-filled bushings the General Electric Company, for nearly three years, has been using an open ventilated construction but with the oil protected from the atmosphere by a thermal trap similar in its effect to the conservator used on many power transformers. This construction is shown in figure 1 of this discussion.

The inverted U tube, which connects the bushing with the expansion chamber, serves to prevent circulation of oil by convection currents; and the interchange of oil between the bushing and the expansion chamber is limited to the small amount necessitated by volumetric thermal expansion of the oil. Extensive accelerated tests have shown that bushings utilizing this construction retain their original dielectric condition permanently.

A. J. A. Peterson: Mr. Wetherill's discussion emphasizes, in an interesting manner, the approach of another manufacturer to the problem of protecting the bushing so as to give it longer life. Testing pressures, operating stresses on gaskets, and so forth are matters of individual and detail design. It is interesting to note, however, that the sufficiency of pressure testing to determine sound cores is influenced by the time during which such pressures are applied. Long experience in manufacturing and testing condenser bushings has led to the adoption of combinations of pressure and time to insure the most satisfactory results.

As pointed out in the paper, the numerous improvements in condenser bushings have made the same condenser core suitable for either fluid or plastic encasing material. Both oil and plastic as used in condenser bushings eliminate shrink voids referred to by Mr. Wetherill, and the condenser principle inherently avoids the local overstresses which might be present in other designs.

Gaskets serve not only to keep moisture out of the bushing but also to keep the filling material in the bushing. This is of major importance in bushings where the oil contributes or forms the main part of the insulation, and is obviously of less importance in the capacitor bushing.

There is likewise considerable difference in the relative importance of the filling material of various types of bushings. The condenser unit of a Westinghouse bushing is the real insulating medium and the oil or plastic serves only to protect the condenser, and does not contribute materially to the dielectric strength of the bushing.

Dielectric Strength of Porcelain

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FUNDAMENTALLY, the voltage-time characteristic of insulation suggests the extending of studies for fullerboard and transformer oil[1,2] to other insulation materials. Porcelain is considered in this investigation since it is used extensively for line and substation insulation, in apparatus design as bushings,[3] and for other insulation.

The determination of the impulse characteristics of porcelain for limited and for repeated voltage applications is another object of the paper. Tests were made with full and chopped waves and with steep impulses to simulate traveling waves and direct strokes. Line apparatus in particular frequently is subjected to these two types of impulses.

Test Method

For the impulse tests the equipment and general procedure conform to the usual practice. Voltage and time were recorded by the cathode-ray oscillograph connected across the test object through a resistance divider or a capacitance divider calibrated against the resistance divider. A rod gap chopped the impulse to the desired wave for the steep-front and chopped-wave tests applied to the specimens.

The voltage supply for the 60-cycle tests was a 150-kv 75-kva testing transformer excited through an induction regulator for voltage control. The voltage measured at the voltmeter coil of the testing transformer was calibrated against a standard sphere gap connected across the test load.

Porcelain Shells—Test Results

Forty-seven porcelain shells of the suspension-insulator type were tested in transformer oil at 24 degrees centigrade. The shells were nine inches in diameter and one-half-inch thick between the line electrode and ground plate, figure 1.

Tests were made to determine:

(*a*). The impulse strength for limited applications

(*b*). The impulse strength for repeated applications

(*c*). Sixty-cycle strength

Impulse strength for limited applications means single applications of voltage each setting raised in five-per-cent steps to breakdown. Table I and figure 2 give the results of these tests for positive and negative full, chopped, and steep-front waves for a total of 28 shells.

Impulse strength for repeated applications means as many as 100 applications of voltage each setting raised in approximately ten-per-cent steps to breakdown. Table II and figure 3 give the results for positive, full, and steep-front waves, and for negative full, chopped, and steep-front waves for a total of 13 shells.

For the 60-cycle tests a voltage of about 80 per cent of the expected failure was applied for one minute. A rest period of one minute intervened. The voltage was raised in five-per-cent increments to breakdown. This procedure was repeated for five shells. On one shell the voltage was rapidly applied to breakdown. The results are summarized in table III.

The breakdown of a single test specimen varies as much as ten-per-cent and more from the average, as shown in tables I, II, and III. For limited applications the variation on the average is five to ten per cent. The test data for repeated applications are more consistent, the average variation not exceeding five per cent. Some of the 60-cycle breakdown values depart substantially from the average.

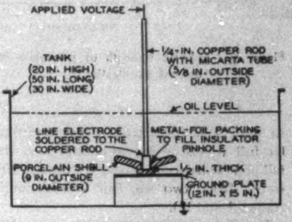
APPLIED VOLTAGE
TANK (20 IN. HIGH) (50 IN. LONG) (30 IN. WIDE)
¼-IN. COPPER ROD WITH MICARTA TUBE (⅝ IN. OUTSIDE DIAMETER)
OIL LEVEL
LINE ELECTRODE SOLDERED TO THE COPPER ROD
METAL-FOIL PACKING TO FILL INSULATOR PINHOLE
PORCELAIN SHELL (9 IN. OUTSIDE DIAMETER)
½ IN. THICK
GROUND PLATE (12 IN. x 15 IN.)

Figure 1. Arrangements of porcelain shells for test

Porcelain has a characteristic similar

Table I. Impulse Strength of Porcelain Shells for Limited Applications

Porcelain Number	Test*	Number of Impulses	Kilovolts Held	Kilovolts Breakdown	Time to Breakdown (Microseconds)	Kilovolts Per Microsecond	Kilovolts Average Breakdown	Average Time to Breakdown (Microseconds)
1	Positive full wave $1\frac{1}{2}$x48 microseconds	6	160	168	3.0		154	2.9
3		1		137	4.0			
12		6	153	158	3.3			
5	Positive wave chopped at 2–3 microseconds	4	154	163	2.2		156	2.64
6		4	159	158	2.3			
7		3	148	159	2.5			
8		1		140	3.3			
9		6	171	179	2.5			
10		1		136	2.7			
11		4	141	153	3.0			
13	Positive wave-steep fronts	2	198	213	1.4	152		0.74
14		3	256	265	0.62	427		
15		1		279	0.59	473		
16		1		277	0.57	487		
17		1		279	0.29	967		
18		1		339	0.3	957		
24	Negative full wave $1\frac{1}{2}$x48 microseconds	11	199	205	2.3		219	2.9
25		5	185	194	2.3			
26		11	232	241	3.0			
27		10	225	235	3.0			
28	Negative wave chopped at 2 microseconds	7	201	209	2.0		209	2.3
29		8	210	209	2.5			
30		8	205					
19	Negative wave—steep fronts	1		339	0.2	1,690		0.37
20		1		330	0.19	1,735		
21		1		282	0.5	563		
22		1		298	0.53	563		
23		1		307				

* In these tests the full wave was $1\frac{1}{2}$x48 microseconds which for these tests is equivalent in effect to either the $1\frac{1}{2}$x40-microsecond (AIEE) or the 1x50-microsecond (IEC) waves.

Paper number 39-99, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3–5, 1939. Manuscript submitted August 8, 1938; made available for preprinting March 28, 1939.

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The authors acknowledge the helpful assistance rendered by G. Y. Hager, G. L. Candler, and W. D. Albright in the test work.

1. For all numbered references, see list at end of paper.

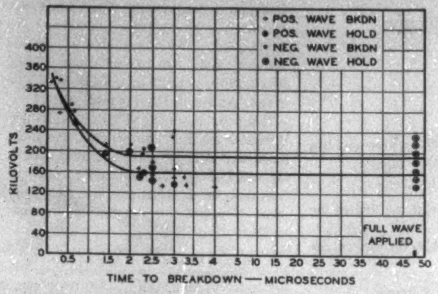

Figure 2. Impulse strength of porcelain for limited applications

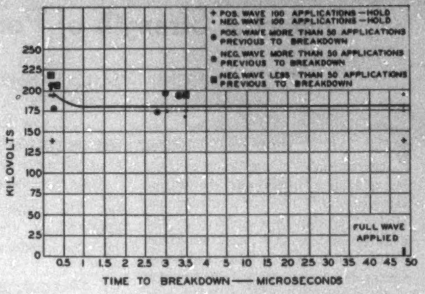

Figure 3. Impulse strength of porcelain for repeated applications

to other solid insulation materials in that for full waves or waves chopped on the tail breakdown occurs near the crest of the wave, from two to four microseconds (tables I and II).

For the majority of impulse tests the puncture of the porcelain (figure 5) was from the edge of the line electrode where the greater stress appears. In relatively fewer cases failure occurred near the center. The failure is in the nature of a fused path the size of an ordinary pin or greater through which the current discharged. When testing on the front of the wave, due to the higher setting of the impulse generator, holes were blown in the porcelain by the explosive action of the heavier current. Breakdown on 60 cycles was at the corner of the electrode.

From figures 2 and 3, a polarity effect is apparent for the specimen and electrode arrangement employed. For impulses, two microseconds and longer the negative voltage is about 20 per cent greater than the positive. At the very short time, for single applications, the polarity effect practically disappears.

The voltage-time characteristic for limited applications (figure 2) is a constant voltage for waves two microseconds and longer. For shorter durations, an upturn of the curve takes place with an increase in the voltage of nearly 100 per cent at 0.3 to 0.2 microsecond. These very short impulses are chopped on a front which rises at approximately 1,000 kv per microsecond (tables I and II).

For repeated applications (figure 3) the voltage-time characteristic is a constant voltage with the indication of an upturn on approaching durations less than one microsecond. The voltage strength for repeated impulses is about 90 per cent of that for limited applications for two microseconds and greater. These tests show that at 0.2 microsecond the single application breakdown is 70 per cent greater than for repeated applications.

The impulse ratio (impulse voltage divided by 60-cycle one-minute hold) of the flat part of the voltage-time characteristic for limited applications (figure 2) is respectively 1.47 and 1.75 for positive and negative waves. The average for the two polarities is 1.61. For repeated applications (figure 3) the impulse ratio is 1.28 for the positive and 1.65 for the negative waves. The average is 1.47.

Porcelain Tubes—Test Results

Tests were made on one-inch-thick porcelain. The specimens consisted of porcelain tubes, one-inch inside diameter through which a metal tube fitted snugly. Midway on the outside of the tube, a tinfoil band and flange similar to a bushing arrangement comprised the ground. Voltage was applied to the metal tube. The tests were made in transformer oil at 26 degrees centigrade. Tests at higher temperature did not show appreciable change in breakdown. The strength of several samples to a $1^1/_2$x40-microsecond positive wave for limited applications averaged 237 kv and the one-minute 60-cycle hold, 146 kv (crest). Thus the impulse ratio is 1.62. This value compares closely with the corresponding

Table II. Impulse Strength of Porcelain Shells for Repeated Applications

Porcelain Number	Test*	Kilovolts	Number of Impulses	Kilovolts	Number of Impulses	Kilovolts	Number of Impulses	Kilovolts	Number of Impulses	Total Impulses	Kilovolts per Microsecond	Kilovolts Breakdown	Time to Breakdown (Microseconds)
37	Positive full wave $1^1/_2$x48 microseconds	118	50	126	50	140	100	154	52	253		154	
41	Positive wave—steep fronts			132	100	139	100	176	54	255	765	176	0.23
31	Negative full wave $1^1/_2$x48 microseconds			178	100	196	54			155		196	
32				175	50					51		175	
35				175	100	195	12			113		195	
33	Negative wave chopped at 2–3 microseconds			175	100	195	2			103		195	3.5
34				175	100	195	68			169		195	
36				175	51					52		175	2.8
44				171	100	195	88			180		195	3.5
38	Negative wave—steep fronts	150	100	165	100	193	99			300	918	193	0.23
39		150	100	165	100	193	200	218	22	423	800	218	0.18
42				176	100	202	90			191	1,040	202	0.2
43				176	100	200	109			210	1,000	200	0.2

* In these tests the full wave was $1^1/_2$x48 microseconds which for these tests is equivalent in effect to either the $1^1/_2$x40-microsecond (AIEE) or the 1x50-micro second (IEC) waves.

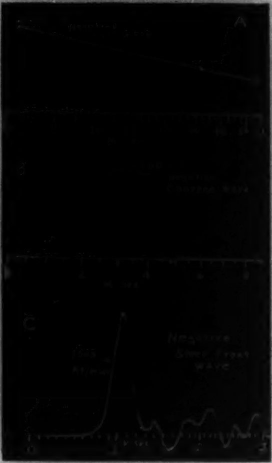

Figure 4. Typical oscillograms of test applied

A—Full wave
B—Wave chopped on tail
C—Steep-front wave

average impulse ratio for the tests on porcelain shells. From these and similar tests the relation between voltage strength and porcelain thickness is a straight line on log-log co-ordinates with a slope of 0.5.

Steep-Front Tests on Suspension-Insulator Strings

Strings of 3, 5, and 14 standard suspension insulators were flashed over with negative waves on a 3,000 to 4,000 kv per microsecond front. Thirty impulses in succession were applied to each string. The 14-unit string was last to be tested. Four insulators in this string already had been subjected to 30 impulses, so that these four units received a total of 60 impulses. The test data are summarized in table IV.

Table III. Sixty-Cycle Strength of Porcelain Shells

Porcelain Number	Test	Maximum One-Minute Hold Voltage: Kilovolts (Crest)	Maximum One-Minute Hold Voltage: Average	Breakdown Voltage: Kilovolts (Crest)	Breakdown Voltage: Average	Time Hold (Seconds)
45	60-cycle voltage applied one minute—one minute rest between steps	93.5	111	99	116	10
46		110.0		116		40
47		140.0		147		30
48		99.0		104		50
49		111.0		116		40
50	60-cycle voltage rapidly applied (approximately 10–15 seconds)			159		

The five-insulator assembly was in addition subjected to combined steep-front and high-current flashovers (figure 6). This test simulates a lightning stroke discharge.[4] Table V summarizes the test data.

No sign of damage to the insulators was apparent from either the steep front or combined tests. These results are particularly significant as these tests approach or simulate in severity the stresses to which insulators may be subjected on lines as the result of direct strokes. On the basis of uniform distribution of the voltage, the insulators of the three-unit assembly were stressed the highest (table IV). Each insulator withstood repeated impulses of 330 kv with approximately 0.25-microsecond duration. The porcelain thickness of the insulators, between cap and pin, is approximately $^{13}/_{16}$ of an inch; the cap and pin are assembled to the shell with cement.

Suspension Insulators Tested to Breakdown in Oil

Impulse tests to breakdown were applied to the insulators previously tested on steep fronts. Each insulator was immersed in transformer oil similar to the arrangement of figure 1, voltage being applied to the pin. The voltage was raised by steps to breakdown. Table VI summarizes the data.

Tests were made on 60 cycles. The results are given in table VII. In these as in the other tests reported in the paper good transformer oil (30 to 35 kv, standard cup test) was used, except for the three 60-cycle tests (table VII) where due to contamination the strength of the oil was 17 kv. Even then no apparent difference is noted in the puncture strength of the insulators.

The impulse tests indicate that the negative full-wave strength is somewhat higher than the positive full-wave but the data are insufficient to establish definitely the amount. The average breakdown for the positive and negative full waves is 265 kv. The average 60-cycle one-minute

Figure 5. Typical breakdown of porcelain shells

Repeated applications:
A—Negative full wave
B—Negative wave chopped on tail
C—Negative steep-front wave
D—Positive full wave
E—Positive steep front

Limited applications:
F—Positive steep front
G—Negative steep front
H—Negative wave chopped on tail
I—Positive wave chopped on tail

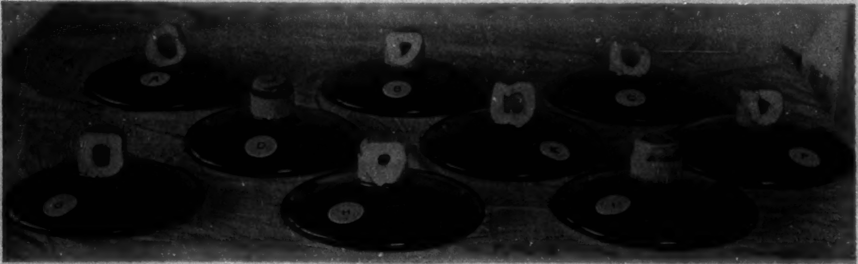

Table IV. Steep-Front Impulse Tests on Suspension Insulator Strings, Negative Polarity

Insulator Units* in String	Number of Impulses	Voltage Applied		Comment
		Kilovolts	Kilovolts per Microsecond	
3	30	1,000	4,000	No apparent damage
5	30	1,300	3,000 to 4,000	No apparent damage
14	30	2,100	3,500**	No apparent damage

* Standard insulators, 10-inch diameter, 5¾-inch spacing.

** Rise of front. Flashover in 1.5 to 1.6 microseconds.

Table V. Combined High-Voltage and Current Impulse Tests on Five-Unit Insulator String*

Number of Tests	Voltage Applied		Current		Comment
	Kilovolts	Kilovolts per Microsecond	Amperes	Total Duration (Microseconds)	
4	1,300	3,000 to 4,000	40,000	70	No apparent damage

* These tests made on same five-insulator string in table IV. Polarity of voltage and current negative.

hold strength is 163 kv (crest). Therefore the impulse ratio is 1.62, a figure close in agreement with the results for the porcelain shells and tubes.

From these tests the voltage-time characteristic (table VI) rises at 0.2 microsecond approximately 50 per cent above the 2- to 40-microsecond value. This amount in the upturn is not so great as the tests of the porcelain shells (figure 2) show due possibly to the difference in the electrode arrangements and in the specimens, and in particular to the relatively larger number of impulses that were applied to the suspension insulators previous to (table IV) and during the breakdown tests (table VI).

Figure 6. Combined high-voltage and current impulse test on five-unit insulator string

Comparison of Porcelain Data

As a basis of comparison the data for the one-half-inch shells have been plotted in figure 7 as impulse-ratio/time curves. The corresponding impulse ratios for the one-inch tubes and the suspension insulators from 2 to 40 microseconds are essentially the same as in curve *A* (limited applications).

The 30 flashovers in air of the suspension insulator assemblies indicate that a stress was sustained by the insulator units which corresponds to an impulse ratio of 2.0 at 0.25 microsecond. This value compares to 1.7 of curve *B* where, however, the number of tests applied to the one-half-inch shells in oil is greater. Following the flashover tests, the limited tests on the suspension insulators in oil (table VI) give an impulse ratio of 2.0 at 0.28 microsecond and of 2.4 at 0.17 microsecond. Although the test data on the suspension insulators are not on the same basis as the curves of figure 7 and therefore cannot be directly compared, the data well substantiate the impulse-ratio characteristic of porcelain as given by these curves.

In a recent investigation for the comparison of impulse tests sponsored by the International Electrotechnical Commission, tests[5] are reported by Allibone on thin (0.15-inch) porcelain cups with an electrode arrangement simulating a rather uniform field. The tests were made in oil at room temperature. Repeated impulses were applied. Essentially the same voltage was obtained for positive and negative waves. From this investigation the impulse ratio for 1-, 5-, and 50-microsecond waves are respectively 1.55, 1.48, and 1.46. These values are in close agreement with the corresponding data in curve *B* of figure 7.

Sixty-cycle tests in oil of suspension insulators have been a subject of considerable investigation, for the condition of the oil affects the puncture voltage. Insulators tested in oil of abnormally low resistivity show some 30-per-cent increase in puncture voltage over the tests made in normal transformer oil, due apparently to the grading effect that low-resistivity oil has on the concentrated field at the metal parts. Rebora[6] has investigated the effect of the oil on the puncture voltage of standard suspension insulators. His data for the tests in normal transformer oil are in substantially good agreement with ours (table VII) both in regard to the relative values and in the nature of the breakdown. In these tests (table VII) where normal transformer oil was used no substantial grading at the cap edge or at the pin could have been present since streamer formation could be observed as the test voltage was increased to the puncture value. The concentrated stress is indicated also from the frequent occurrence of failure through the porcelain shell from the edge of the cap to the pin. The flashover data of the insulators (in air) point out the possibility that for air the corona and streamers from the cap and pin are of such a nature as to have a grading effect. By virtue of this grading effect a higher stress would be sustained by the insulator (tables IV and VI).

Table VI. Impulse Strength of Suspension Insulators Tested in Transformer Oil

Insulator Unit	Test	Number of Impulses	Kilovolts Breakdown	Microseconds
A	Positive full wave	15	260	1½x40
B		7	230	
C		17	265	
D	Negative full wave	20	290	1½x40
E	Negative wave—steep fronts	16	320	0.28
F		3	395	0.17

Comments: Oil temperature 24 degrees centigrade. Dielectric strength of oil 30 kv in standard cup. 0.1-inch gap, one-inch disks. Time to breakdown of *A*, *B*, *C*, and *D*, three to eight microseconds. Failure of *A*, *B*, *D*, and *E* from cap edge to pin. Failure of *C* and *F* inside cap to pin.

While the dielectric strength of porcelain is affected by and varies with the test specimen, the electrode arrangement, the method of test, and other factors, the data presented in this paper and elsewhere establish that the curves of figure 7 are representative of the voltage-time characteristic of porcelain.

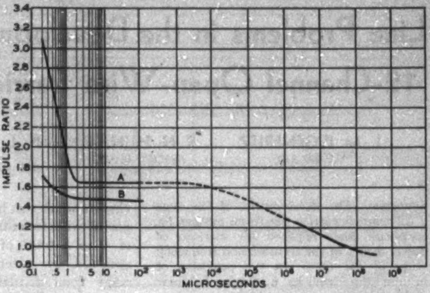

Figure 7. Impulse ratio-time characteristics of porcelain for limited applications (A) and for repeated applications (B)

Apparatus in Service

The voltage-time characteristic of porcelain is apparent also from the performance of apparatus in service. As an illustration, suspension line insulators will be considered since this apparatus is frequently stressed in service from traveling waves and direct strokes. Moreover, line insulators are well standardized and their flashover characteristics fully established.[7]

The porcelain thickness between cap and pin for standard insulators is about 0.80 inch. For the 1.5x40 microsecond wave flashover the average stress per insulator is from 100 to 90 kv for insulator strings of 5 to 16 elements. The average stress per insulator for a two-microsecond impulse is approximately 130 kv. These stresses are for equal voltage distribution over the insulator string and require multiplying by a factor greater than unity for departure, from uniform distribution. It is conceivable also that in the process of flashover stresses exceeding these average values would appear across the individual insulators for the very short time in which the flashover occurs. All considered, the good performance of modern line insulators to traveling waves is naturally expected.

Experience shows that even direct strokes seldom, if ever, puncture the porcelain in modern suspension insulators. Consider the probable stress of the porcelain from the various records of direct strokes to the line. An oscillogram of a direct stroke to a 220-kv line[8] (16-insulator assembly) indicates a rate of rise of 1,500 to 2,000 kv per microsecond, the voltage reaching a crest of 3,000 kv. Other records and analyses[9,10,11] indicate that rates of rise up to 5,000 kv per microsecond possibly are attained.

Considering 3,000 kv per microsecond as or near the upper limit for the steepness of the front of lightning strokes, from table IV and other published data[4,12,13], the average stress and its duration per element for a 16-insulator string would be approximately 200 kv and 1.2 microsecond. For a 5-insulator string the average stress and duration are 280 kv and 0.5 microsecond and for a 3-insulator string, 330 kv and 0.25 microsecond. The ability of insulators to withstand these stresses is demonstrated from the tests reported in this paper (tables IV and V) and in a previous investigation.[4]

The good performance of line insulators even when subject to direct strokes is quite understandable from fundamental considerations of the dielectric strength of porcelain. Furthermore, this analysis indicates that the steepness of the front of direct strokes possibly is not so great as assumed in the past.

Conclusions

1. The voltage-time characteristic of porcelain for limited applications is a constant voltage down to two microseconds and rises with shorter impulses nearly doubling at 0.2 microsecond.

2. For many repeated applications, the voltage-time characteristic is practically constant with relatively smaller upturn at the shorter impulses.

3. The impulse ratio on the flat part of the characteristic is 1.60 for limited applications and approximately 1.45 for repeated applications.

4. These inherent characteristics of porcelain are apparent also from the good performance of line and other apparatus subjected in service to traveling waves and direct strokes.

5. The characteristics of porcelain compared with its good performance in service indicate that the steepness of strokes on lines possibly does not or seldom exceeds 3,000 to 5,000 kv per microsecond.

Table VII. Sixty-Cycle Strength of Suspension Insulators Tested in Transformer Oil

Insulator Unit**	Test	Maximum One-Minute Hold Voltage (Kilovolts Crest)	Breakdown Voltage Kilovolts Crest	 Time Hold (Seconds)	Remarks*
G	60-cycle voltage applied one minute. One minute rest period between steps	132	145	55	Oil temperature 25 degrees centigrade. Oil strength 35 kv
H		161	166	35	
I		168	175	45	Oil temperature 20 degrees centigrade. Oil strength 17 kv
J		168	175	30	
K		161	168	10	
L		175	182	50	Oil temperature 20 degrees centigrade. Oil strength 31 kv
M		168	175	15	
N		161	168	45	
O		168	175	55	
		Average = 163			

* Dielectric strength of oil determined in standard cup. 0.1-inch gap between one-inch disks.

** Failure of *G, H, I, J, L, N,* and *O* from cap edge to pin; of *K* and *M* from inside cap to pin.

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12. FACTORS INFLUENCING THE INSULATION CO-

Line Problems in the Development of the 12-Channel Open-Wire Carrier System

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Synopsis: The development of the type *J* 12-channel carrier telephone system for open-wire lines required an increase of nearly 5 to 1 in the transmission frequency range of the lines. In the provision of suitable line facilities a number of new problems were encountered with respect to attenuation, noise and cross talk. Methods for meeting these problems and the results obtained are described.

A NEW carrier telephone system for open-wire telephone lines has been described recently.[1] This system increases the number of two-way telephone circuits which can be obtained on a single pair of wires from the previous maximum of 4 to a total of 16. This has been achieved by extending the frequency range from a maximum of about 30 kilocycles to more than 140 kilocycles. The exploitation of this new range of frequencies on open wire has involved the solution of a number of interesting problems, among which are these:

1. Not only does the attenuation of an open-wire line under ordinary weather conditions rise substantially with frequency but extremely large increases in attenuation occur at the higher frequencies when ice forms on the wires.[2,3] In spite of these effects a high degree of stability of transmission has been secured on all channels by the provision of automatic control of repeater gain and equalization.

2. New cross-talk problems created by the extension of the frequency range have been solved by the development of transposition designs with numbers of transpositions not greatly in excess of those employed for the lower-frequency systems. Problems have also arisen in controlling the cross talk around the repeaters and in reducing the effect of impedance departures between the line circuits and the equipment.

Frequency Allocations

The type *J* system operates on circuits on which type *C* carrier systems were already operating in the frequency range up to about 30 kilocycles. To provide enough frequency separation between the two systems the lower frequency limit of the *J* system was set at 36 kilocycles; the necessary frequency space for 12 channels in each direction set the upper limit at about 140 kilocycles. This range is split into two parts, one used for transmission in one direction and the other for the opposite direction. Figure 1 illustrates the relation of the frequency bands occupied by the type *J* and type *C* systems and the voice-frequency channel. Different "staggered" locations of the frequency bands are to be employed in order to simplify cross-talk problems.

Filters are used for separation of the type *J* from the type *C* and lower-frequency facilities on the same pair of wires. This separation is done by means of a combination of high- and low-pass filters which split apart the frequency ranges above and below the band between 30 and 36 kilocycles. To simplify the design of these filters, the low-frequency group of the type *J* system is transmitted in the same direction as the high-frequency group of the type *C* system. This arrangement of transmitting certain frequencies in a particular direction is generally used throughout the telephone plant in order to avoid serious cross-talk difficulties. Accordingly with few exceptions west to east transmission or south to north transmission takes place in the same frequency bands throughout the country and similarly, east to west or north to south transmission employs the same frequency bands. These are indicated in figure 1.

Line Attenuation

An open-wire pair affords the lowest-loss transmission medium of any conductor employed in the telephone plant. It is, however, peculiarly subject to the effect of weather, which may cause large and often rapid changes in the attenuation. In consequence, some form of gain regulation is required.

Even for carrier systems operating up to 30 kilocycles, manual regulation is inadequate for the longer systems and automatic devices have been provided for

Paper number 39-80, recommended by the AIEE committee on communication, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted February 15, 1939; made available for preprinting March 13, 1939.

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Discussion

Victor Siegfried (Worcester Polytechnic Institute, Worcester, Mass.): This paper is of interest in that it fills in a gap in the information on dielectric strengths of materials where no great amount of data exists. The behavior of porcelain is shown to be similar to that of other solid dielectrics in the volt-time characteristics, and in the difference in characteristics on repeated application as compared with a limited number of voltage applications.

In obtaining their data, the authors have used porcelain shells without hardware. I am wondering what difference might be expected when a cap is on the units, giving a more even distribution of stress on the inside corner next to the pin. It might be expected at first guess that the characteristics would be similar to those found, but at different values for the actual breakdown voltage, thus giving a higher actual voltage which the unit could withstand. If there is a tangible difference, it will affect the extension of these data to other cases where the actual porcelain insulator with line hardware is used.

In general, this paper shows the ability of the modern insulator to stand up under the severest stresses imposed by line conditions; in fact, the authors very significantly conclude that the proved ability of the insulators to take strokes in service indicates a maximum steepness of wave of 3,000 to 5,000 kv per microsecond. This shows us that the laboratories cannot go wrong in speeding up the wave fronts to such a value when attempting to duplicate the worst type of impulse that natural lightning is likely to produce.

P. L. Bellaschi and M. L. Manning: The tests on complete porcelain units to which Mr. Siegfried refers are given in tables IV to VII inclusive. One of the objects of the paper was to establish the volt-time characteristic of porcelain. Accordingly, plain shells, completely assembled in insulator units, tubes, etc., were tested. In these tests, different electrode arrangements were used for the various specimens. The average results of such tests are given in figure 7, which is the representative volt-time curve of porcelain.

We are in full agreement with Mr. Siegfried in that the tests verify the good performance of porcelain expected and found in service.

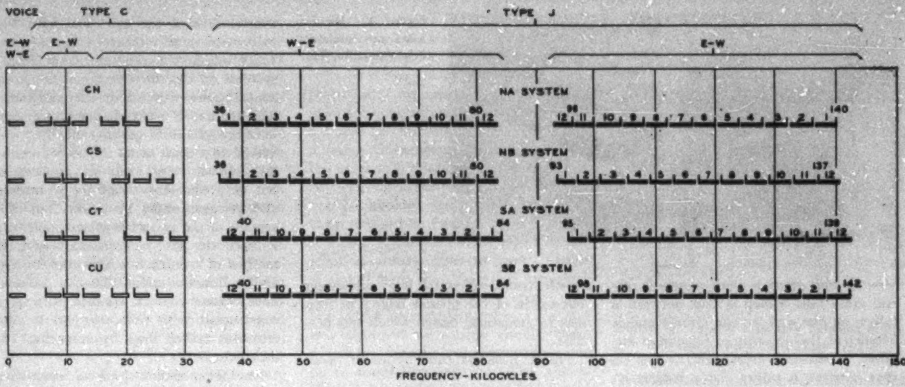


Figure 1. Frequency allocation

Note: E-W also implies transmission N-S and W-E implies S-N

most systems over 500 miles in length. The attenuation changes caused by changes in resistance of the wire with temperature or by changes in the shunt losses when insulators become wet are much larger at the higher frequencies of the *J* system, and therefore, an automatic regulating scheme is required. Tests were made on open-wire circuits to determine more precisely the characteristics needed for such a regulator. During sleet storms, when wires are covered with ice, the increases in attenuation are far beyond any caused by rain. Figure 2 shows increases which may be caused by ice as compared with the normal dry- and wet-weather values.

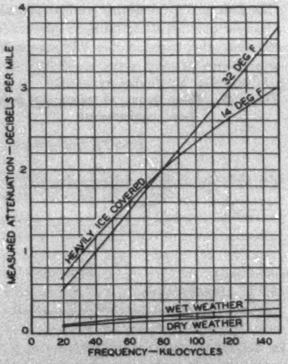


Figure 2. Attenuation variation with weather

Eight-inch spacing, 165-mil copper wire

The deposits on the wire may be actual ice, or in some cases wet snow or frost adhering to the wire. Figure 3 shows an example of such deposits. Theory shows that the increase in attenuation is caused by energy losses in the ice itself and that leakage across the insulators is usually a negligible factor.

An extensive survey of the effects of ice has been carried on at various points throughout the country during the past four years and a large amount of information has been accumulated. These tests have shown that the shape of the attenuation-frequency characteristic differs considerably for different ice formations and even if the ice deposit remains the same for a time, the attenuation-frequency characteristic may vary with temperature as in figure 2. The two upper curves of the figure were measured at different times during the same storm. There was no apparent change in deposit between the two measurements. This change in shape of the characteristic, of course, makes the regulation problem more difficult. In spite of the extreme severity of ice effects in certain regions it is expected that satisfactory reliability will be obtained on type *J* systems by placing the repeaters sufficiently close together.

Regulation Problem

In the first type *J* systems the regulator actuated by a single pilot frequency in each direction compensates for the attenuation changes caused by temperature and wet weather.

The required varieties of attenuation slopes with ice on the wires could not be provided by a simple regulator. Hence provision is to be made in later designs for a regulator with variable slope controlled by two pilot frequencies which is expected to be satisfactory in areas subjected to sleet conditions. The regulating range will also be increased so that a completely automatic control of gain up to about 75 decibels will be available.

It was found that during periods when ice coated the wires the circuit noise measured at the end of a repeater section usually decreased as the attenuation increased. This is important because otherwise the extra increase in the repeater gain to take care of the higher attenuation at such times would make the noise excessive. The study of ice conditions throughout the country which has been carried on and is still continuing will be useful in laying out repeater stations along some of the routes which eventually will be candidates for the application of type *J* systems.

Open-Wire Cross Talk

The cross-talk problem on open-wire lines is one of the most important. Cross talk is controlled by transpositions which are introduced into the various pairs in accordance with a predetermined design. The creation of the necessary designs requires consideration both of the complex theory of transpositions and measurements on lines constructed by practical methods.

However, the design of transposition systems is considerably simplified by the use of different frequencies for the two directions of transmission. The only cross talk between systems which is

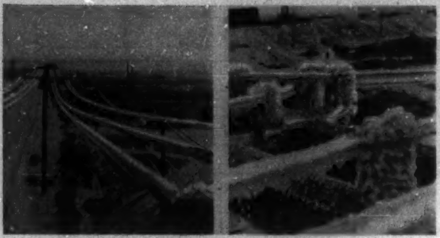


Figure 3. Ice on wires and insulators near Amarillo, Tex.

directly important is that known as far-end cross talk, which is that between a talker at one end of one circuit and a listener at the opposite or far end of another. Near-end cross talk, which is that between a talker and a listener at the same or near ends of two circuits, becomes a source of interference between circuits only when portions of it appear as far-end cross talk because of reflections at points of impedance irregularity in the circuits.

Because of the high cost of a transposition design to keep both near-end and far-end cross talk down to small values, only small reflections are permitted where open-wire and cable meet, or where circuits are terminated in equipment. A number of the difficulties which had to be overcome to attain small reflections are discussed later in the paper. With this control the transposition designer can concentrate most of his attention on far-end cross talk, the near-end cross-talk requirements are relaxed, and a cheaper transposition arrangement can be used.

What can happen when reflection occurs may be seen by comparison of the near-end and far-end cross-talk curves in figure 4. The similarity in the shapes of the two curves, and particularly the fact that the peaks occur at the same frequencies, show that what appears to be far-end cross talk is in this case mostly reflected near-end cross talk. It is for pair combinations such as this one, where the near-end cross talk is much larger than the far-end, that the closest control of reflection effects is required. With the values of reflection realized in the *J* system, reflected cross talk will ordinarily be unimportant.

To obtain satisfactory cross-talk conditions at the higher frequencies some changes in line construction are necessary. To use type *J* carrier systems on existing open-wire routes, methods were devised for modifying the line construction in as economical a manner as possible. For new lines, such as the new part of the fourth transcontinental line[5] advantage was taken of the greater degree of freedom in structural design which was possible.

Figure 5 shows three types of open-wire pole head configuration suitable for *J* system operation. The left-hand diagram shows a method of reconstructing part of one of the older types of open-wire lines built with 12-inch spacing between wires of the pairs and with the "alternate arm" transposition system which was developed for the use of type *C* systems on the side circuits of the horizontal phantom groups on alternate arms. This method is a flexible one in that one or more phantom groups may be converted at a time, as on the second crossarm shown. For such an application not only was removal of the phantoms and retransposition necessary, but the spacing of the two wires of each pair was reduced to six inches. This general method of construction was used for the Dallas-Houston and Dallas-San Antonio lines,[6] except that the six-inch pairs were constructed with new wire on a new crossarm rather than by respacing 12-inch pairs.

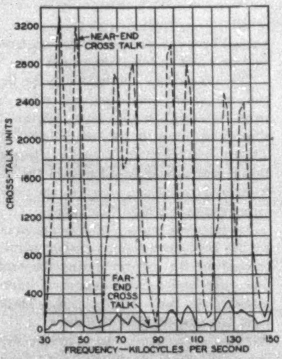
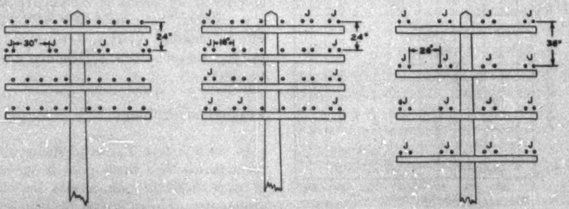


Figure 4. Near-end and far-end cross talk—J-3 transposition system

Pairs 3/4–9/10

Another common type of open-wire pole-head configuration, the middle diagram of figure 5, is that made up of eight-inch-spaced nonphantomed pairs transposed in accordance with the *K*-8 transposition system on an eight-span base. Through design studies supplemented with field experiments it was found that such a line could be converted for *J* systems much more cheaply than an alternate-arm line. If *J* systems are restricted to the pairs on the outer ends of the crossarms, with two inner pairs, about one or two transposition changes in each pair per mile are enough. This scheme was followed in reconstructing the line between Charlotte, N. C., and West Palm Beach, Fla.

For new lines yet to be built, a greater degree of latitude in structural design is naturally possible. The right-hand diagram of figure 5 shows an open-wire pole-head configuration designed to allow *J* systems to be operated on all of the pairs. The unique feature of this configuration is that, while 8-inch spacing is preserved between the wires of the various pairs,



MODIFIED "ALTERNATE ARM" LINE
12" SPACED PHANTOMED SIDES
EXCEPT
4 J PAIRS RESPACED TO 8"
J-1 TRANSPOSITION SYSTEM

MODIFIED "K-8" LINE
8" SPACED
NON-PHANTOMED PAIRS
K-8-2 TRANSPOSITION SYSTEM

NEW LINES
8" SPACED
NON-PHANTOMED PAIRS
J-2 OR J-3
TRANSPOSITION SYSTEM

Figure 5. Three types of open-wire pole head configuration

the adjacent nonpole pairs on a crossarm are separated by 26 inches and the crossarms by 36 inches. The reduction in coupling made possible by this increased spacing keeps the cross talk for any combination of pairs down to a suitable value with transposition arrangements not necessarily more complicated than those employed for the other configurations. This type of construction was used for the new parts of the fourth transcontinental line.

Figure 6 shows a comparison of the number of transpositions used in a typical section of open-wire line for various types of circuits from voice-frequency phantomed circuits to nonphantomed circuits intended for *J* system operation. From the original arrangement where there was one transposition point in every ten spans, about one-fourth mile, the number of transpositions for *J* carrier operation has been increased so that for the *J*-3 design, which was used for the new wires on the fourth transcontinental line, there are four transpositions in each eight-span interval and every pole is a potential transposition point.

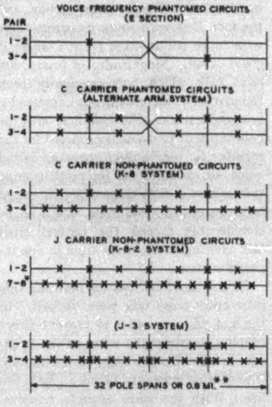
It may be seen from figure 8, however, that the number of transpositions required in pairs for *J* carrier operation is not necessarily larger than the number employed in systems intended for *C* carrier operation with a top frequency of 30 kilocycles. The superiority of the *J* system transposition arrangements as compared with those designed for *C* system operation results from the choice of specific arrangements which best limit the systematic effects for frequencies in the *J* system range.

Typical far-end cross talk measured between eight-inch-spaced pairs 11–12 and 19–20 on a new *J*-3 line and on a reconstructed *K*-8-2 line is shown by figure 7. The superiority of the new line with its fewer wires, greater wire separations, better transposition system, and smaller irregularities is evident.

Absorption Effects

The attenuation of an open-wire pair may be quite unsatisfactory if there are what are known as absorption effects, caused by induction into surrounding circuits such that energy is absorbed in particular frequency bands and the attenuation of the pair increased. These effects, which depend on the transposition arrangements in the circuits, may cause objectionable transmission distortion at critical frequencies unless the transpositions are planned to avoid them. The same arrangements necessary to control cross talk between *J* systems will automatically eliminate absorption effects with one exception. If only part of the pairs on a line are designated and transposed for *J* systems and the remaining pairs are not so transposed, absorption in a *J* pair can be caused by a nearby non-*J* pair. Consequently, consideration of the cross-talk relations at *J* frequencies between all of the pairs on the line cannot be avoided even though some of them will not be used for *J* systems.

Figure 8 illustrates the effect of absorption on three different pairs. Curves *A* and *B* show the absorption measured over the type *J* frequency range on a line of the alternate-arm type. Curve *A* was obtained on a side circuit transposed for operation at frequencies only up to about 10 kilocycles. The absorption at frequencies above this becomes very large. Curve *B* shows the absorption present on one of the *C* carrier side circuits on the same line transposed for operation up to 30 kilocycles. Curve *C* shows how absorption disappears on a nonphantomed pair specially transposed for type *J* operation. If this pair were measured at much higher frequencies, similar absorption "bumps" would be found, perhaps at frequencies of 200–300 kilocycles or higher.


VOICE FREQUENCY PHANTOMED CIRCUITS
(E SECTION)
PAIR
1-2
3-4
C CARRIER PHANTOMED CIRCUITS
(ALTERNATE ARM. SYSTEM)
1-2
3-4
C CARRIER NON-PHANTOMED CIRCUITS
(K-8 SYSTEM)
1-2
3-4
J CARRIER NON-PHANTOMED CIRCUITS
(K-8-2 SYSTEM)
1-2
7-8*
(J-3 SYSTEM)
1-2
3-4
32 POLE SPANS OR 0.8 MI.**

X = SIDE OR PHYSICAL CIRCUIT TRANSPOSITION

╳ = PHANTOM TRANSPOSITION

* PAIR 7-8 IS SHOWN SINCE PAIR 3-4 IS NOT INTENDED FOR J-SYSTEM OPERATION ON A K-8-2 LINE.

** FOR THE VOICE FREQUENCY AND ALTERNATE ARM LAYOUTS THIS DISTANCE IS 40 SPANS.

Figure 6. Illustrative transposition arrangements

Since absorption effects depend on the systematic addition of cross-talk currents along a line, a continuous succession of identical transposition sections tends toward greater absorption while a random succession of different kinds of transposition sections of different lengths will reduce it. The Dallas-Longview *J* system is operating on an alternate-arm side circuit, transposed for *C* carrier operation and without any modifications to adapt it for the higher frequencies. Because of the fortunately irregular succession of different transposition sections found here, it was possible to select, after tests, a pair with no serious absorption.

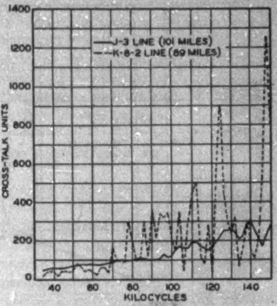


Figure 7. Far-end cross talk between eight-inch-spaced pairs 11/12 and 19/20

Construction Irregularities

With the new transposition designs, the systematic cross talk resulting from the transposition arrangements has been reduced in nearly every case so far that the remaining cross talk is controlled principally by construction irregularities. An important source of irregularity is the difference in sags of the various wires in each span of the line, particularly sag differences between the two wires of each pair. Another potentially important source of irregularity is the variation in the spacings between successive transposition poles. It is relatively easy to make this factor unimportant as compared with sag differences.

The large amount by which the cross talk can be reduced by careful methods of construction coupled with the highly developed systematic transposition patterns is illustrated by the fact that between certain pairs the cross talk in a 75-mile repeater section is reduced to a value which would be produced by a capacitance unbalance between them of less than two micromicrofarads, which is about the same in magnitude as the capacitance between wires of a foot of the

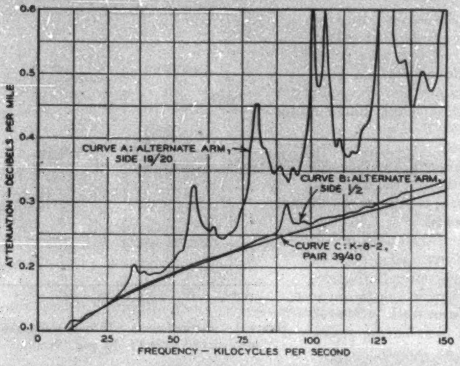


Figure 8. Attenuation of open-wire pairs of different types

Curves *A* and *B*—Side 19/20 and 1/2, respectively, on alternate-arm line, 104-mil, 12-inch, 56.7 miles, 90 degrees Fahrenheit, at Mascoutah, Ill.

Curve *A* is transposed for voice frequencies

Curve *B* is transposed for carrier operation up to 30 kilocycles

Curve *C* is pair 39/40 on K-8-2 line, 104-mil, 8-inch, 50 degrees Fahrenheit and CS insulation between Denmark, S. C., and Rincon, Ga., transposed for carrier operation up to 140 kilocycles

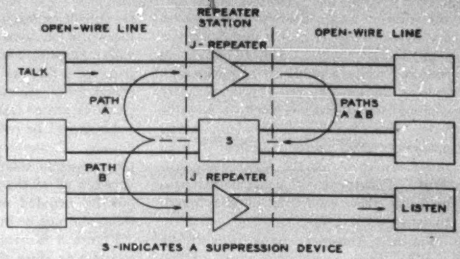


Figure 9. (above). Interaction cross talk at a J repeater station

open-wire pair. This large cross-talk reduction is in spite of the fact that at 140 kilocycles the phase change along an open-wire circuit is about 7 degrees in a single span, the shortest distance between any two transpositions, and about 28 degrees for the more common four-span interval.

Interaction Cross Talk at Repeater Points

Another type of problem was introduced by what is known as interaction cross talk. This is the cross talk which occurs from one side to the other of a *J* repeater station. Figure 9 illustrates two paths which it may take. Path *A* shows the cross talk from a system to itself which may cause transmission distortion or repeater singing while path *B* is the path of cross talk between different circuits. The essential feature of this interaction cross talk is that, as figure 9 shows, the cross-talk path at a repeater station passes through the *J* repeater and hence the cross talk is amplified by the repeater gain.

The new problems of controlling this cross talk were the result of larger magnitudes of cross talk at the higher frequencies, the larger repeater gains, and the fact that with more repeaters there were more points on a system where it could occur. Magnitudes of interaction cross talk which had previously been thought of as inconsequential assumed a new importance. For instance, with the gain of about 75 decibels proposed for the repeater for use in sleet areas, an initial value of unamplified interaction cross talk as low as 0.25 cross-talk unit would be magnified to 1,400 units, which might considerably exceed the far-end cross talk existing at the same time in one repeater section.

Several new methods for reducing this interaction cross talk were devised. In the first place, in order to prevent direct coupling between the wires of the open-wire line on the two sides of the station, it was found necessary to cut a gap in the line. With the wires entirely removed for a distance usually of about 80 feet, the line is brought into the station from the two terminal poles by means of the lead-in cables.

It was also seen to be necessary to block the paths provided by the wires of the telephone line itself. For this purpose, cross-talk suppression filters were designed and built to be installed in all of the non-*J* circuits on the line. These give losses of the order of 70 decibels at 140 kilocycles not only in the metallic transmission circuits but also in other circuits, made up of various combinations of the line wires, which may conduct cross-talk currents through the stations.

In addition to the cross-talk suppression filters and in order to provide an extra margin of safety against interaction cross-talk currents which might find their way through the repeater station by stray paths, longitudinal choke coils have been connected at the pole heads between the open wires and the lead-in cables. These coils do not disturb ordinary transmission but add high impedance in the longitudinal circuits.

These measures for controlling interaction cross talk have been found to be adequate so far as the telephone line is concerned. At an occasional *J* repeater station, however, located on a right-of-way occupied by several pole lines, there is found another pole line paralleling the telephone line with a separation sometimes as little as two to five feet between the nearest wires of the two lines. Such wires provide other interaction cross-talk paths past the repeater station and impair the effectiveness of suppression measures installed in the line on which the *J* system is operated. The by-passing effects of such a foreign line can be controlled by cross-talk suppression devices similar to those used in the telephone line wires.

Figure 10 shows a comparison of the interaction cross talk measured at a *J* repeater station before any suppression measures were installed, the other wires of the line being continuous at the station location, with the corresponding interaction cross talk when the line was run through the suppression devices in the station. The values shown would be amplified by the gain of the *J* repeater on the disturbed circuit before they reached the listener. The effect of the by-passing foreign line is illustrated by the difference between the middle and bottom curves, the bottom curve showing the measured cross talk when the by-passing line was cut to simulate the effect of suppression measures in it.

Staggered Systems

It would not be possible with the open-wire line configurations now in use to design transposition arrangements that

Table I

Transposition System	Wire Spacing (Inches)	Noise (Decibels)*	Repeater Spacing in Miles—128-Mil Wire
Alternate arm	12	+10	67
K-8-2	8	+ 5	82
J-1	6	− 2	103

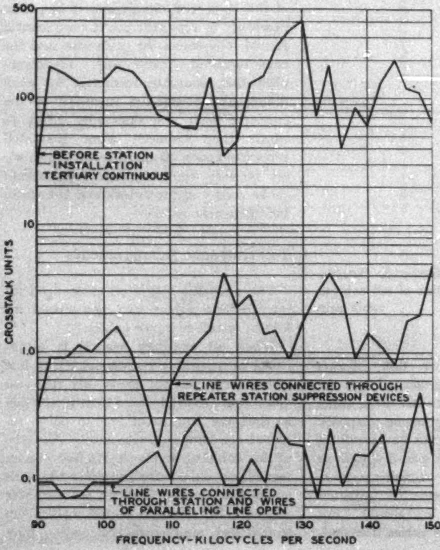
* Above reference noise, 10^{-12} watt at 1,000 cycles.

would permit the operation of identical *J* systems on all pairs. For this reason four types of *J* systems with different channel carrier-frequency allocations will be provided in the future. The frequency assignments for these systems are shown in figure 1.

The "staggering" advantage, or effective cross-talk reduction between systems, is effected because (1) the inversion or displacement of channels in the different systems with respect to each other makes the cross talk unintelligible, and (2) the reduction of the overlap between channels results in less energy being transferred between them by cross talk. The net benefits of "staggering" obtained by the allocations shown in figure 1 range from about 6 to 16 decibels.

The most effective pair assignments for the four types of *J* systems can best be obtained from actual cross-talk data on the particular sections of line involved. The "staggering" advantages obtained are sufficient so that the highest remaining cross talk will usually occur between the like *J* systems operating on nonadjacent pairs.

Figure 10. Unamplified interaction cross talk between two J circuits at an auxiliary repeater station



Noise

Observed external sources of noise in *J* systems are atmospheric static, dust storms, radio stations, power-line carrier, and power-supply systems.

Of these possible sources the more important will usually be atmospheric static which will be greatest during the summer months. In regions where dust storms occur, their effects are expected to exceed that of atmospheric static but will be more likely to occur during the winter and early spring.

Table I shows values of noise at 140 kilocycles, caused by atmospheric static, found at the open-wire line terminals of one repeater section; the values are those which it is expected will be exceeded during one per cent of the summer season extending from May to September. If the repeater spacings shown were used, the total static noise in the top channel at the end of a circuit with 20 repeaters would be 20 decibels above reference noise at the −9-decibel level. However, other factors such as ice may require the use of shorter spacings.

Line Impedance

As mentioned previously in the discussion of cross talk, it is important that the line impedances be matched closely and large irregularities be avoided. Because of the different wire sizes and pair spacings, a wide range of open-wire line impedances may be encountered. Novel construction arrangements and the development of new lead-in circuits have made it possible to secure a reflection coefficient of about five per cent at the junction between the open-wire pair and the toll entrance and office equipment at the highest transmitted frequency.

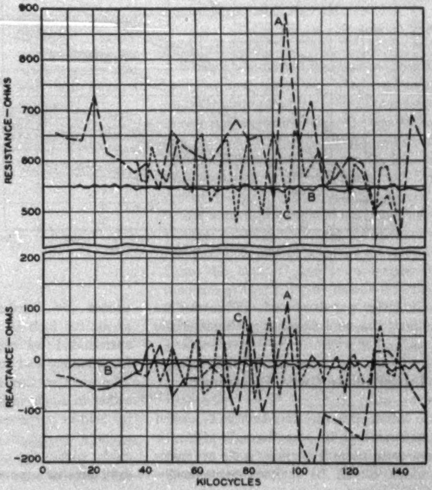
The transposition arrangement and wire spacing of a pair affect the smoothness of its impedance because they affect the reactions between circuits which

Figure 11. Impedances of open-wire pairs of different types

A—104-mil, 12-inch-spaced, side 17/18 of an alternate-arm line

B—165-mil, 8-inch-spaced, pair 17/18 of new *J*-3 line

C—128-mil, 8-inch-spaced, pair 31/32 of K-8-2 line with miscellaneous lengths of tree wire, etc.



cause absorption effects. The marked improvement which can be obtained by proper design is illustrated by comparison of curves *A* and *B* of figure 11. Curve *A* shows the impedance of a 12-inch-spaced side circuit on an alternate-arm line. This particular circuit was one intended for use at frequencies not above ten kilocycles. In striking contrast curve *B* shows the comparatively smooth impedance of an eight-inch-spaced nonphantomed pair on a new line transposed in accordance with the *J*-3 system.

"Tree" wire, a special line wire with abrasion-resistant insulation, has been used on open-wire lines for many years in places where the lines were exposed to tree branches. During line tests in Florida, another use for tree wire was found where the open-wire line, along a causeway or bridge, is subject to fouling by fishing tackle. Curve *C* of figure 12 shows what a half-mile or so of this tree wire, supplemented by several sections of 165-mil wire at railroad and power-line crossings, can do to the impedance of a 128-mil pair. To reduce the irregularities a new type of insulated line wire of smaller diameter and with thinner insulation was developed. This wire has about the same impedance characteristic as the line wire.

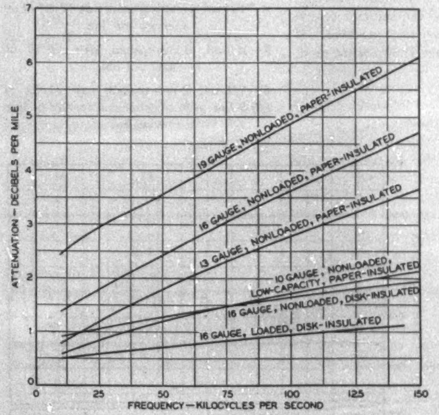


Figure 12. Attenuation of toll entrance cable pairs

Intermediate Cable Treatment

When open-wire lines have to be placed underground to pass through towns or to cross natural barriers such as rivers which cannot be spanned economically with open wire, cable is used. In the past, the circuits in such cables were frequently loaded to reduce their attenuation and to match the impedance of the open-wire circuits in order to avoid reflection effects and degradation of voice-frequency repeater balance. To load paper-insulated cable pairs for frequencies up to 150 kilocycles would require exceedingly short loading spacing, of the order of 200 feet, which would be expensive and in many cases impractical with existing manhole locations. An alternative, the use of a transformer to match the open-wire and cable impedances, was rejected as it was found impractical to design a transformer which would be adequate over the entire frequency range.

To overcome these difficulties, a new low-capacitance type of cable was developed which could be loaded to match the open-wire impedance with coil spacings about the same as those previously used. Loading coils of different sizes were developed to provide for loading to the different impedances of the open-wire circuits.

The new cable employs 16-gauge conductors in a spiral-four arrangement, supported by hard-rubber disk spacers about 0.6 inch in diameter. These are surrounded by copper and iron tapes for shielding and strengthening purposes. The units so formed may be assembled either in single units in a lead sheath as for lead-in purposes, or in multiple units, up to a maximum of seven for full-sized cable, within the same lead sheath. For duct runs or submarine cables, the multiple assembly is usually employed, and, in the latter case, with outside armoring and jute protection. If the submarine span is more than about 600 feet, intermediate submarine loading is employed.

As an alternative, it sometimes happens that where a long intermediate cable is involved, an auxiliary type *J* repeater station can be placed conveniently at one end of this cable. In this case, the filter hut described in the discussion of toll entrance arrangements in the next section may be used at the end of the cable opposite the repeater station and the cable treated as a toll entrance cable for the auxiliary office. A further alternative is to provide filter huts at the two ends of a nonloaded intermediate cable. However, if the cable is short, the new disk-insulated cable with loading is to be preferred.

Previous practice at the ends of open-wire lines has been to use paired bridle wire with weatherproof insulation and usually of smaller gauge than the line wire to connect the open-wire pairs to cable terminals mounted on the pole. Other pairs of bridle wire were connected between the open wires and protectors. Because of the much more severe reflection requirements at the higher frequencies of the type *J* system, these arrangements were no longer satisfactory. The characteristic impedance of bridle wire is roughly one-fifth of that of the open-wire circuit and it has been necessary to avoid the use of even several feet of it between the open-wire and the cable terminal or protectors. To accomplish this, separate terminals for each disk-insulated unit are mounted on the crossarm near the open-wire pairs to which they connect. Four insulated wires from each terminal go by the shortest feasible route to the longitudinal choke coils and protectors and thence to the open-wire pairs.

Toll Entrance Arrangements

The new disk-insulated cable used for intermediate cables was also suited for lead-in or toll entrance cables.

When an auxiliary station is established at a point along an open-wire line where there has not previously been an office, it is usually located close to the line so that the lengths of lead-in cable required are comparatively short. Lengths of this cable up to about 175 feet can be loaded to open-wire impedances with adjustable loading units in the repeater station. For longer lead-in cables up to 300 feet, supplementary loading may

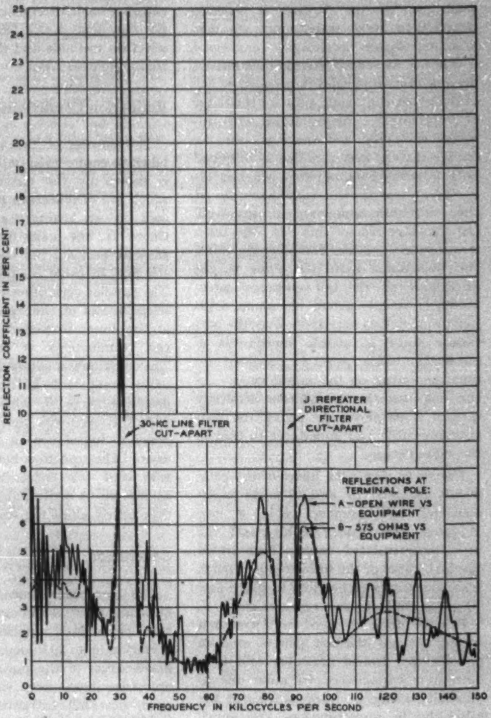


Figure 14 (above). Reflection coefficient at junction between open-wire and toll entrance equipment

be mounted directly on the pole at the cable terminals.

When an auxiliary repeater station is not close to the open-wire line, or at main repeater stations which are frequently in towns and separated from the open-wire line by greater lengths of toll entrance cable, it is still possible to use the loaded disk-insulated cable. Because of the cost of this cable and its loading, however, it has sometimes been found more economical to build a hut near the open-wire terminal pole and to separate the type *J* from the type *C* and lower-frequency facilities at that point by means of filters. The connection from the open-wire line to the hut is provided by what is usually a short length of loaded disk-insulated cable. From that point, the type *J* frequencies are led into the toll office over nonloaded paper-insulated pairs while the *C* and lower-frequency facilities are brought in over the existing pairs, usually loaded. By thus limiting the frequencies transmitted over the nonloaded

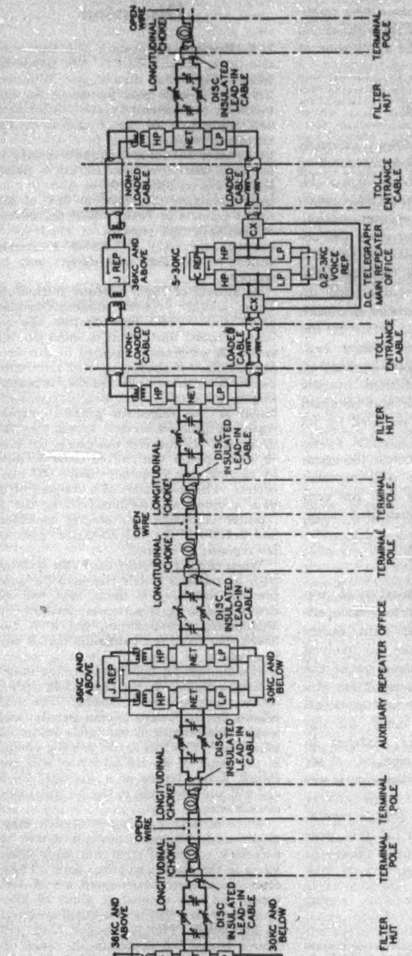

OPEN WIRE
LONGITUDINAL CHOKE
DISC INSULATED LEAD-IN CABLE
TERMINAL POLE
FILTER HUT
HP
NET
LP
NON-LOADED CABLE
LOADED CABLE
TOLL ENTRANCE CABLE
J REP
36KC AND ABOVE
5-30KC
C REP
0.2-3KC VOICE REP
CX
D.C. TELEGRAPH MAIN REPEATER OFFICE
AUXILIARY REPEATER OFFICE
30KC AND BELOW
ENTRANCE CABLE
GRP TERM
CARR SUP
12 CHAN TERM EQ
SUBSETS
TYPE C
VOICE
TELEGRAPH SOUNDERS
TOLL OFFICE

Figure 13. Toll entrance arrangements at J terminal and repeater offices

cable pairs to the *J* range, it becomes practical to design transformers for suitable impedance matching.

The line filter sets located in the hut are designed for a nominal impedance of 560 ohms which is a compromise for the range of impedances normally found with different wire sizes and spacings. An accurate match with the line is obtained with a building-out network which is adjusted at the time of installation to fit the particular open-wire pair involved. On the office side of this line filter set a transformer provides for stepping down the impedance from 560 ohms to the impedance of the toll entrance cable, which is usually about 125 ohms. Adjustment of this impedance over the necessary range to match impedances of particular cable pairs is provided by means of taps on the transformer. At the office another transformer similarly tapped is employed to match the toll-entrance-cable pair impedance to that of the office wiring.

Figure 12 shows the losses of the commonly used 19-, 16-, and 13-gauge paper-insulated toll entrance cable, a new 10-gauge low-capacity cable, and the new disk-insulated cable. Because of the high losses of the smaller-gauge pairs, it is sometimes economical to place new 10-gauge cable to save repeater costs.

For the office wiring of the *J* system a rubber-covered shielded pair is used to provide the desired flexibility and freedom from capacitance variation due to humidity changes. Its impedance at 140 kilocycles is approximately 125 ohms. The repeater and terminal high-frequency impedances are designed to match this impedance very closely.

Figure 13 illustrates the arrangement of the toll entrance equipment involved in matching the line impedance to that of the equipment with a minimum of reflection. The terminal is illustrated to the left. The high-frequency line passes to the line filter set which is here shown as located in a filter hut. There it is joined by the type *C* and lower-frequency circuits and passes through the lead-in cable and protective arrangements on the terminal pole.

Proceeding toward the right in the figure, the arrangement at an auxiliary repeater station is shown. In this case the type *J* frequencies are amplified in the repeater, but the type *C* and lower frequencies are by-passed through filters which suppress longitudinal and metallic transmission above 30 kilocycles. At the right is shown a combined type *J* and type *C* main repeater office.

Satisfactory cross talk between pairs in entrance and intermediate cables carrying *J* systems is effected through special selection methods and the application of balancing capacitors.

Reflection Coefficients

The success of the various measures taken to ensure good impedance matching is shown by the curves of figure 14, which are of reflection coefficients measured at an auxiliary repeater station. Curve *A*, the solid line, gives the coefficient between the open-wire pair and the lead-in cable at the terminal pole. The smaller variations are due partly to irregularities of the open-wire line and, at the lower frequencies, partly to the test terminations at the distant end. The contribution of the cable loading and office equipment is indicated by the dash-line curve *B*, which was obtained with the open-wire line replaced by its nominal impedance, a 575-ohm resistance. The reflection between the open-wire and toll entrance and repeater equipment is well under five per cent over nearly all of the transmitted range.

Conclusion

The successful transmission of frequencies up to 140 kilocycles over open-wire pairs as compared with earlier operation up to 30 kilocycles has involved modification of the construction of the open-wire lines, new transposition designs, new toll entrance arrangements, including new types of cable, the improvement of impedance matches in various parts of the circuits, closer repeater spacings, and, where ice is encountered, provision for much greater gain margins with more flexible regulation. By the first part of this year about 60,000 channel-miles were in service over type *J* systems.

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5. Transcontinental Telephone Lines, J. J. Pilliod. Electrical Engineering, volume 57, October 1938, pages 418–19 and 423.

6. Some Applications of the Type J Carrier System, L. C. Starbird and J. D. Mathis. AIEE Transactions, volume 58, 1939, pages 666–74 (December section).

Discussion

L. L. Burns (Southwestern Bell Telephone Company, Dallas, Tex.): The paper by Messrs. Ilgenfritz, Hunter, and Whitman has given a very good picture of the line problems encountered in the development and application of the *J* system to the telephone plant. It may be of interest to note that it has not been found necessary to use cross-talk suppression devices in paralleling wires on other leads in order to control interaction cross talk at any of the repeater points in Texas. With the advent of the 75-decibel repeater or whenever it becomes necessary to install a second system on any lead such devices may be required.

At several of the *J* repeater stations in Texas, another pole line parallels the telephone line. These lines by-pass the repeater station and act as a shunt on the cross-talk suppression applied to the telephone lines. If the loss through this shunt path is lower than the gain of the *J* repeater, singing will occur. Such was the condition found at the Edgewood repeater station when the Dallas-Longview system was cut in service. In this case the gain of the east to west repeater was limited to 33 decibels by singing at a frequency above 150 kilocycles. The installation of a low-pass filter with a cutoff at 150 kilocycles in the *J* repeater eliminated the singing condition and permitted the full 45-decibel gain of the repeater to be used.

When circuits on other lines offer a shunt path at repeater points the need for suppression measures in these lines will be determined in each particular instance by appropriate measurements, but it is believed that there is a possibility that if any suppression is required, the longitudinal coils alone will be sufficient since the longitudinal coupling is the controlling factor rather than metallic coupling. The loss offered by these coils to the metallic and longitudinal circuit is negligible below 35 kilocycles. The loss to the metallic circuit in the range of 35–150 kilocycles will not exceed a few tenths of a decibel. Each coil will add two ohms to the d-c resistance of each wire.

When metallic coupling is of such magnitude as to become important, it will be necessary to install cross-talk suppression filters in the circuits on other lines. These filters as at present designed are of two types, one with a cutoff at about 12 kilocycles and the other with a cutoff at about 35 kilocycles. The 12-kilocycle filter will offer a loss of 0.5 decibel in the range up to 12 kilocycles, rising to 70 decibels at 140 kilocycles. The 35-kilocycle filter will offer a loss of from 0.2 to 0.7 decibel up to 30 kilocycles, rising to an average value of 55 decibels at frequencies above this range. The loss of both filters in the longitudinal circuit is similar in magnitude to that in the metallic circuit.

The interaction cross-talk problem is not confined to repeater points. Where an east terminal and a west terminal are installed in the same office, the coupling between the outputs of the two terminals may be enough to cause intelligible cross talk. A part of this coupling may be direct between the two circuits and a part may be due to coupling through a third line. This third line might be a telephone cable, a power line, or signal line which parallels both open-wire leads or cables for a short distance. This condition existed in the Dallas office. It was found in this particular case that the attenuation offered by the relatively long paper-insulated toll entrance cables to frequencies in the *J* range together with the shielding effect of these cables was sufficient to prevent appreciable cross talk. Longitudinal choke coils may be installed in each circuit in one of the toll entrance cables, if necessary to minimize cross talk of this type. Such coils were installed in one of the Dallas toll entrance cables but subsequently removed when it was found that they were not required.

R. W. Linney (Southwestern Bell Telephone Company, Oklahoma City, Okla.): The authors have given an interesting description of the problems encountered, the methods of solving them, and the results obtained by their application in connection with the installation of the 12-channel carrier telephone systems to open-wire lines. There is a large number of open-wire lines on which the *J* carrier systems will be installed in the future. The toll fundamental plans will indicate the most probable lines for development with the new carrier systems. On existing lines some of the modifications necessary for the application of the *J* carrier systems may be made more economically over a period of years before the time the system is required. These lines are constantly being altered on account of ordinary deteriorations and additions and are occasionally rerouted on account of obstructions.

The following is a list of the ordinary construction activities encountered in maintaining an open-wire line which can be directed in a systematic program to effect a reduction in the costs of modifying a line for *J* carrier operation:

1. *Pole Replacements.* The poles replaced should be located to fit in with the proposed transposition layout. This involves preparation of the transposition schemes in advance.

2. *Long-Span Installations.* Long spans introduce serious problems to *J* carrier facilities. Consideration should be given to the location of transpositions and to the spacing of wires to meet the requirements of *J* carriers.

3. *Intermediate and Entrance Cable Installations.* Proposed intermediate and entrance cables with their complicated design should be compared with open-wire reroutes, keeping in mind that the cables at best offer transmission inferior to open wire.

4. *New Wire Stringing Projects.* In placing or replacing wire on a line, consideration should be given to locating it in such a position and spacing it so as to be suitable for carrier operation.

5. *Major Crossarm Replacement Projects.* Ordinarily crossarm replacements are scattered so that a wire respacing job could not be associated. However, in certain cases where a phantom is not in use, it would be practicable to respace the wires at the time a major crossarm replacement job is carried out.

6. *Rerouting Sections of Line.* Where sections of lines are relocated to clear highways and other obstructions, the new construction should be carried out to provide for the ultimate carrier requirements for the section involved. The new route should avoid paralleling other overhead lines especially near proposed *J* repeater stations to reduce interaction cross talk. Power-line carriers, power-supply lines, and radio stations at close proximity should also be avoided as these systems are a source of noise in *J* circuits.

A program including some of the items mentioned above has been in effect in connection with applying *C* and *D* carrier systems and material savings have been obtained. The more rigid design requirements of the outside plant associated with the application of the higher-frequency carriers offers an even better field of application for such a program.

R. M. Carpenter (Southwestern Bell Telephone Company, Houston, Tex.): The paper very comprehensively presents the many varied and technical problems encountered in the application of high-frequency telephone channels to open-wire telephone lines. Several factors not heretofore offering any appreciable hindrances to commercial transmission must now be given minute study and consideration. Just how well the Bell Telephone Laboratories have done the job of overcoming these obstacles is illustrated by the fact that several *J* carrier systems are now rendering high-grade telephone service in several parts of the nation under many varied conditions. The authors of this paper played the major role in the development of this carrier and its application to different types of open-wire structures, and deserve much credit for doing an excellent work.

We in Texas are very much interested in the economies offered by the 12-channel open-wire carrier system as a means of providing much of the additional toll facilities required to keep pace with the increasing usage of long-distance telephone service. This carrier system is particularly adapted to providing many additional miles of high-frequency communication channels from the large number of existing long-haul toll circuits in the Texas area. Most of the major toll lines in Texas consist of open-wire structures and there are relatively great distances between the larger toll points. An extended use of such systems seems feasible in Texas not only because of the above features, but also for the reason that the conditions contributing to line problems are not as severe as in most parts of the nation.

Firstly, deposits of ice on wires in Texas, as a whole, are not a frequent occurrence. The picture of the ice on wires and insulators near Amarillo, Tex., does not lend any strength to this statement, but, those of you who are acquainted with the storm-loading areas, know that only a small portion of Texas is in the heavy-loading area, where, for pole-line-design purposes, heavy ice may be expected at certain intervals. A somewhat larger section lies in the medium-loading area where a lighter coating of ice may be had at rare intervals, but by far the greater portion is in the light-loading area where no ice is considered for design purposes. In deference to the picture, it should be said that ice will form more frequently on the wires in the colder Panhandle section of Texas, but, in the rest of the state, in the heavy and medium areas, sleet will not be a factor more than once or twice each year, and, in the light-loading area is a rarity. Therefore, deposits of ice which greatly attenuate the high frequencies cannot be considered a major problem in most parts of Texas.

Secondly, the toll lines in Texas will have shorter lengths of entrance and intermediate cables in their make-up than will those in more densely populated sections of the country. In the 265-mile line between Dallas and Houston, there is not more than 11 miles of cable, and, in the 290-mile Dallas-San Antonio line, the cable mileage is proportionately low. Inasmuch as cable inflicts a severe penalty on the higher frequencies of this open-wire carrier system and requires costly loading as well as special types of conductors in some instances, it is readily seen that the less cable there is in a toll line, the greater economy there is in placing *J* carrier systems on the line.

Thirdly, external sources of noise will not be as great as in some sections of the country. Dust storms occur frequently in the Panhandle and other parts of west Texas, and, in recent years, some of these have been blown eastward into central and east Texas, three to five times each year, but, in general, the portion of the state having most of the major toll lines is affected little or none by dust storms. The relatively low density of population in Texas serves to keep to a minimum such external sources of noise as radio stations, power-line carrier, and power-line supply systems.

Texas has been fortunate in securing most of the early pioneering installations of *J* carrier systems, and, in view of the above favorable conditions, will, no doubt, find it economical and practicable to make an extended further use of this type of communication facility.

Some Applications of the Type J Carrier System

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MEMBER AIEE

J. D. MATHIS
ASSOCIATE AIEE

Synopsis: Previous papers before the AIEE describe the development of a 12-channel type *J* carrier system. This paper discusses some of the practical problems encountered in extending the circuit capacity of existing open-wire lines by the use of this carrier system.

The first systems of this type were placed in commercial operation late in 1938. One of these systems is discussed in detail from the standpoint of obtaining satisfactory operation with the most economical arrangement of new and existing facilities.

A 12-CHANNEL carrier telephone system for open-wire lines was described before the AIEE early this year,[1] and a discussion of the requirements of line facilities for its operation has been presented.[2] Since the first three systems to be placed in commercial operation are located in Texas, it seems appropriate to present to the South West District meeting the major problems arising from the practical application of this type system on existing open-wire plant.

In 1935 it became apparent that existing open-wire facilities on some of the major toll lines in Texas would soon be exhausted. In the case of the Dallas-Houston, Dallas-San Antonio, and Dallas-Longview lines, current growth and requirements for the future indicated that comprehensive relief would have to be provided ultimately. This probably will be a toll cable. At the time, however, the development of the open-wire 12-channel *J* carrier system made available an arrangement for obtaining a large number of additional circuits over the existing lines which would provide for the immediate requirements and also permit postponement of more costly relief measures for a number of years.

The type *J* system operates in a frequency range above that of the three-channel type *C* carrier system and can be superposed on the same conductors with the type *C*, thereby providing a total of 16 circuits from one pair of conductors. However, conductors suitable for type *C* carrier operation are not necessarily satisfactory for the operation of the new system.

The three lines under consideration were practically of the same construction, being 12-inch phantomed lines originally built for voice-frequency circuits only and later modified for the application of type *C* carrier systems. Over lines of this type, it is practicable to operate a single type *J* system without any material change in the line wire because no crosstalk considerations are involved, although it is necessary to select by transmission measurement pairs which are free from absorption effects. Where more than one system is required a transposition arrangement has been designed for use with line conductors of a nonphantomed pair spaced 6 inches apart and 30 inches between conductors of horizontally adjacent pairs. This design can be used either for new wire or for existing wire retransposed, and can be applied without regard to the existing phantomed transposition design, thereby permitting respacing and retransposing any portion of the existing wire, a phantom group at a time if desired.

Figure 1. Typical pole with extension fixture

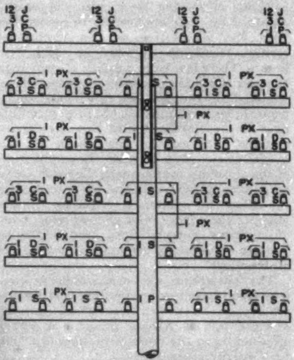

Figure 2. Pole head diagram showing circuit capacity of the Dallas-Houston and Dallas-San Antonio lines

P—Physical circuit
PX—Phantom circuit
S—Side circuit
C—*C* carrier circuit
D—*D* carrier circuit
J—*J* carrier circuit

Advance Engineering

With these operating limitations a review of the circuit requirements established a plan to place a *J* carrier system on one of the phantom groups of the Dallas-Longview line during 1938. This system would not only provide sufficient circuits to meet the additional requirements but would furnish sufficient spare circuits to release one phantom group of 12-inch wire for respacing and retransposing. This plan was not applicable to the Dallas-Houston and Dallas-San Antonio lines since circuit relief was required for the 1937 business, and the *J* carrier system would not be available until 1938. These lines each consisted of five crossarms of 104-mil wire over the greater portion of their length. An inspection showed that, although the poles

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Paper number 39-79, recommended by the AIEE committee on communication, and presented at the AIEE South West District meeting, Houston, Tex., April 17-19, 1939. Manuscript submitted January 30, 1939; made available for preprinting March 9, 1939.

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1. For all numbered references, see list at end of paper.

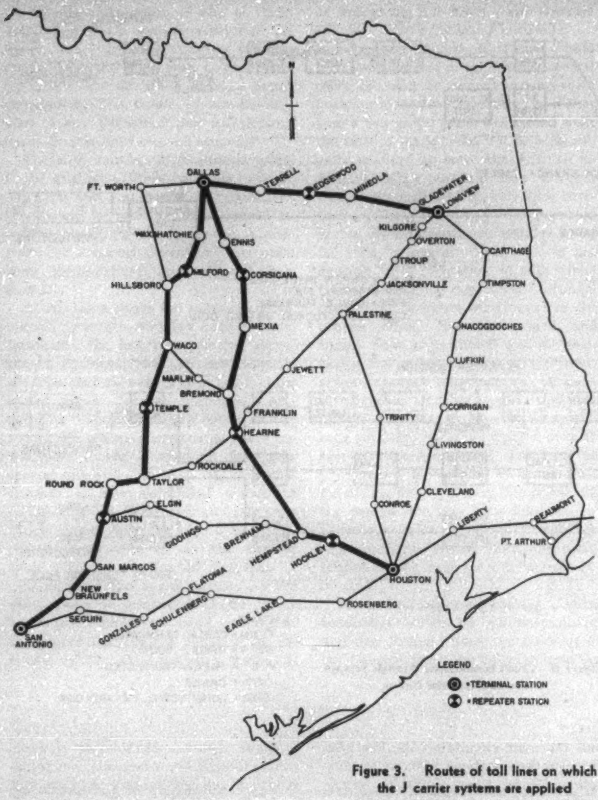
N
FT. WORTH
DALLAS
TERRELL
EDGEWOOD
MINEOLA
GLADEWATER
LONGVIEW
KILGORE
OVERTON
TROUP
JACKSONVILLE
CARTHAGE
TIMPSON
WAXAHATCHIE
ENNIS
MILFORD
CORSICANA
HILLSBORO
MEXIA
PALESTINE
NACOGDOCHES
WACO
LUFKIN
JEWETT
MARLIN
BREMOND
TEMPLE
FRANKLIN
CORRIGAN
TRINITY
HEARNE
LIVINGSTON
ROCKDALE
ROUND ROCK
TAYLOR
CLEVELAND
ELGIN
CONROE
AUSTIN
GIDDINGS
BRENHAM
LIBERTY
BEAUMONT
HEMPSTEAD
HOCKLEY
PT. ARTHUR
SAN MARCOS
HOUSTON
NEW BRAUNFELS
FLATONIA
SCHULENBERG
SEGUIN
EAGLE LAKE
ROSENBERG
GONZALES
SAN ANTONIO
LEGEND
=TERMINAL STATION
=REPEATER STATION

Figure 3. Routes of toll lines on which the *J* carrier systems are applied

were of sufficient strength to support additional crossarms, it would be difficult to maintain the necessary wire clearance with an additional crossarm below the existing wire and also that new wire so placed would be susceptible to interference from possible breaks in the wire above.

The solution of this problem was the addition of a crossarm two feet above the others on a simple extension fixture. This fixture shown in figure 1 consists of a four-inch steel H beam fastened to the pole by the through bolts which also support the two upper crossarms. By placing four pairs of six-inch-spaced conductors on the new crossarm and by using four type *C* carrier systems, 16 additional circuits were obtained to furnish the circuit relief for 1937 and, in addition to the immediate relief, four suitable *J* carrier paths were provided of which one on each line was needed in 1938. Figure 2 is a typical pole head and shows how the ultimate circuit capacity of this open-wire plant has been expanded from 69 to 133 circuits by the addition of one crossarm and eight conductors. The use of 128-mil wire instead of 104-mil wire provides greater strength and the particular location reduces the probability of interrupting 16 circuits by a single wire break or other physical interference.

The program of placing three type *J* carrier systems in service in Texas during 1938 was established. Figure 3 is a map of a portion of the state showing the routes of the lines and the principal cities along the routes. Since the length and attenuation of each of these lines are such that the carrier systems cannot operate without intermediate amplification, it was necessary that the number and locations of repeater stations be determined.

Typical System

The layout of a particular system is largely controlled by available repeater gain, existing entrance cables, line attenuation under normal and adverse weather conditions, location and availability of existing telephone buildings, and availability of commercial power for new buildings. Line attenuation is increased greatly by deposits of ice on the wire during sleet conditions. Although data are available regarding the frequency of large deposits of ice, there is very little information as to the amounts or frequency of occurrence of small deposits. Under normal wet-weather conditions the maximum attenuation of six-inch-spaced 128-mil facilities at 140 kilocycles is 0.35 decibel per mile.

On the Dallas-San Antonio line the facilities available consisted of 286 miles of six-inch-spaced 128-mil copper wire and 42,000 feet of 16-gauge nonloaded paper-insulated cable. Using repeaters having a maximum amplification of 45 decibels in

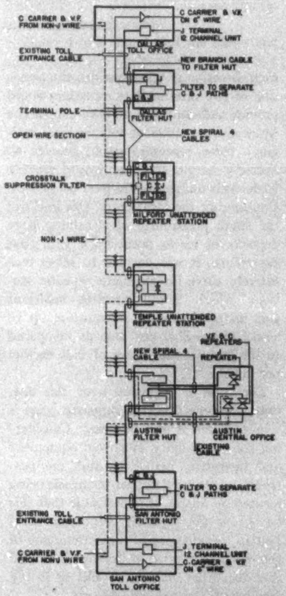
C CARRIER & V.F.
FROM NON-J WIRE
C CARRIER & V.F.
ON 6" WIRE
J TERMINAL
12 CHANNEL UNIT
DALLAS
TOLL OFFICE
EXISTING TOLL
ENTRANCE CABLE
NEW BRANCH CABLE
TO FILTER HUT
C
J
FILTER TO SEPARATE
C & J PATHS
C & J
TERMINAL POLE
DALLAS
FILTER HUT
OPEN WIRE SECTION
NEW SPIRAL 4
CABLES
CROSSTALK
SUPPRESSION FILTER
FILTER
C & J
FILTER
MILFORD UNATTENDED
REPEATER STATION
NON-J WIRE
TEMPLE UNATTENDED
REPEATER STATION
VF & C
REPEATERS
NEW SPIRAL 4
CABLE
J
REPEATER
AUSTIN
FILTER HUT
AUSTIN
CENTRAL OFFICE
EXISTING
CABLE
FILTER TO SEPARATE
C & J PATHS
EXISTING TOLL
ENTRANCE CABLE
SAN ANTONIO
FILTER HUT
C CARRIER & V.F.
FROM NON-J WIRE
J TERMINAL
12 CHANNEL UNIT
C CARRIER & V.F.
ON 6" WIRE
SAN ANTONIO
TOLL OFFICE

Figure 4. Arrangement of facilities for a typical *J* carrier system

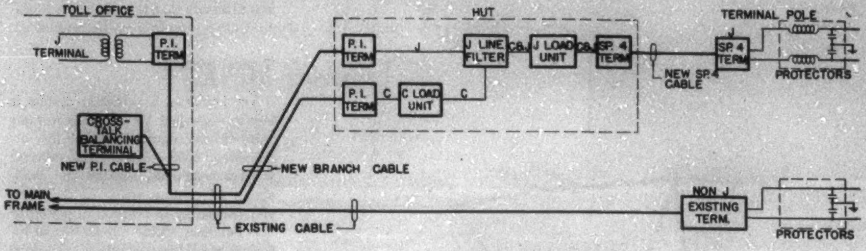
TOLL OFFICE
J TERMINAL
P.I. TERM
CROSS-TALK BALANCING TERMINAL
NEW P.I. CABLE
TO MAIN FRAME
EXISTING CABLE
NEW BRANCH CABLE
HUT
P.I. TERM
J
J LINE FILTER
C&J
J LOAD UNIT
C&J
SP. 4 TERM
P.I. TERM
C
C LOAD UNIT
C
NEW SP. 4 CABLE
TERMINAL POLE
J
SP. 4 TERM
PROTECTORS
NON J
EXISTING TERM.
PROTECTORS
LEGEND:
P.I. = PAPER INSULATED
SP. 4 = SPIRAL FOUR
TERM. = CABLE TERMINAL
LONGITUDINAL RETARD COIL

Figure 5. Circuit connections through Dallas filter hut from open wire to terminal equipment at central office

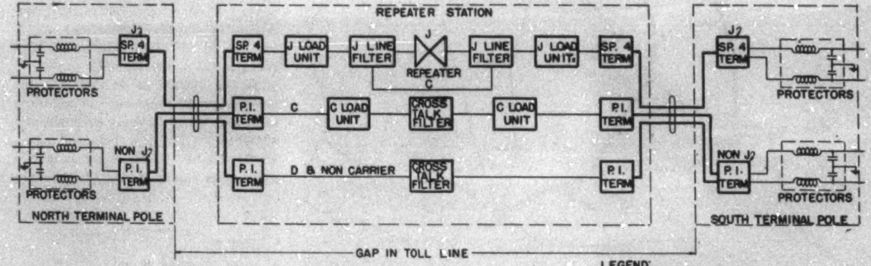
REPEATER STATION
J2
SP. 4 TERM
PROTECTORS
NON J2
P.I. TERM
PROTECTORS
NORTH TERMINAL POLE
SP. 4 TERM
J LOAD UNIT
J LINE FILTER
J
REPEATER
C
J LINE FILTER
J LOAD UNIT
SP. 4 TERM
P.I. TERM
C
C LOAD UNIT
CROSS TALK FILTER
C LOAD UNIT
P.I. TERM
P.I. TERM
D & NON CARRIER
CROSS TALK FILTER
P.I. TERM
J2
SP. 4 TERM
PROTECTORS
NON J2
P.I. TERM
PROTECTORS
SOUTH TERMINAL POLE
GAP IN TOLL LINE
LEGEND:
TERM. = CABLE TERMINAL
SP. 4 = SPIRAL FOUR
P.I. = PAPER INSULATED
= CABLE
= LONGITUDINAL RETARD COIL

Figure 6. Circuit connections through Temple unattended repeater station

each direction of transmission, the provision of two intermediate repeaters would provide sufficient gain to take care of the wet weather conditions with no extra margin; three repeaters would provide 45 decibels margin and four would provide 90 decibels margin for the over-all system. Considering the location of this line and the small probability of obtaining large deposits of ice in accordance with past experience, it was decided to select tentatively three intermediate repeater stations which would provide sufficient gain to take care of attenuation up to about 0.5 decibel per mile as compared to the wet-weather value of 0.35 decibel per mile.

For type *C* operation over this line, only one type *C* carrier repeater point is required, and it is at Austin. Considering the availability of power equipment and operating personnel and the possibility of future *J* carrier terminals being located at Austin, it is desirable that this be one of the repeater points on the *J* system. A division of the attenuation of the facilities north of Austin indicated that the other stations should be in the vicinity of Temple and Milford.

At these repeater stations amplification is needed only on the type *J* system and the other circuits on the line pass through these stations without amplification. Under these conditions energy may be transferred from the output of one type *J* repeater to the input of the same repeater or to the input of a repeater on another *J* system via cross-talk paths involving the wires which are not used for type *J* systems. The effect of this transfer of energy is accentuated by the fact that there is a large difference in transmission level between the output of one type *J* repeater and the input of the same or another repeater. In order to minimize these effects it is necessary that all wires on the line be given special treatment, including a gap in the toll line, longitudinal choke coils in all wires at terminal poles, and cross-talk suppression filters in the non-*J* pairs in the repeater station itself. In selecting locations for repeater stations, consideration must also be given to the possible coupling between type *J* systems by interaction paths involving other conductors adjacent to the toll line.

Before definite selection of repeater station locations may be made, it is necessary that each repeater section be checked in detail and in this check the entrance cable arrangement may be controlling. The newly developed spacer-insulated spiral-four cable, either loaded or nonloaded, or nonloaded pairs of the conventional paper-insulated cable may be used between the open wire and equipment. Generally the existing voice and *C* carrier circuits use loaded entrance-cable pairs and in most cases a change to nonloaded facilities would require extensive rearrangements in these circuits. In order to use nonloaded pairs for the *J* carrier and leave the *C* carrier and voice on loaded facilities, filters are placed at the terminal pole to separate the *J* carrier frequencies from the *C* and voice frequencies at that point. A limitation on the use of existing cable is that suitable pairs must be selected by cross-talk measurements and balanced at 140 kilocycles to meet the requirements of the system. The paper-insulated conductors have the largest attenuation of any of these facilities, and the loaded spiral-four the least. The various entrance arrangements from the open wire to the office equipment are described in more detail elsewhere.[3] The choice of the facility used in any particular case will depend upon the resultant over-all economy.

The large number of nonloaded pairs in the existing 1.8-mile entrance cable at San Antonio indicated that sufficient pairs could be selected which would be satisfactory from the cross-talk standpoint for *J* carrier operation. Six pairs were subsequently selected and balanced.

At Austin a single toll entrance cable, one mile in length, with two complements, terminates the line from the two directions. Although the two complements are separated by a layer shield, this cable is not suitable from a cross-talk standpoint for operation of the *J* carrier in and out of the office; therefore, at least one additional cable is required from the central office to the toll line. For this purpose a new nonloaded spiral-four entrance cable was indicated for the type *J* system with the type *C* and voice circuits continuing to use the existing cable. The separation of the type *J* circuits from the non-*J* circuits on the same pairs is accomplished by filters which are located in a small building at the junction of the toll line and the entrance cables. The use of a single entrance cable for the non-*J* wire in both directions on the telephone line indicated that it might be necessary in the future to use cross-talk suppression filters at this point. Accordingly, the filter hut was made large enough to include future cross-talk suppression filters if required as well as the line filters which separate the type *J* from the non-*J* circuits.

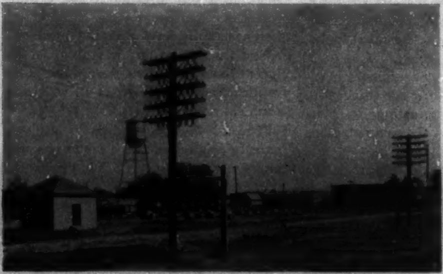

Figure 7. Arrangement of gap in toll line at unattended repeater station

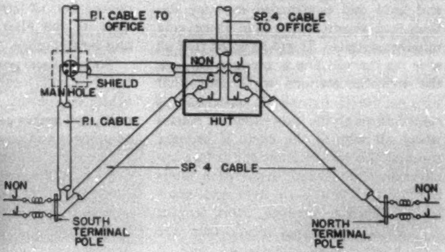

Figure 8. Cable arrangement at Austin

SP.4—Spiral four
P.I.—Paper insulated

A repeater station at Temple could have been located in the existing central office or could be located in a separate building in or near the city. In either case a new power plant was needed since the existing plant could not be economically modified to serve the *J* carrier repeaters. The telephone line is continuous through the city, only those wires used for Temple circuits being terminated in the office through one entrance cable 0.6 mile in length. This cable is not suitable for *J* operation in both directions, which would require one additional cable if the repeaters were located in the central office. Numerous signal and supply lines in proximity with the telephone line within the city offered interaction crosstalk complications. A separate repeater station near the toll line in or near the city avoids the placing of a long entrance cable, reduces the over-all system attenuation, and eliminates the problem of interaction crosstalk from paralleling lines. Other factors including cost showed very little difference between a separate station and placing the repeaters in the central office. An unattended station near the toll line was indicated.

A common entrance cable at Dallas terminates the wire on both the Houston and San Antonio lines, the terminal of the Houston line being 2.9 miles from the central office, and of the San Antonio line one mile further. This cable previously had been placed in three different sections, each section having a different make-up, and there was considerable doubt as to the number of suitable pairs for *J* operation that could be obtained. The use of either a loaded or nonloaded spiral-four cable would not improve attenuation sufficiently to change the number or materially alter the locations of the repeater stations from those tentatively selected, but would provide some additional margin for sleet conditions. The expense of loading the spiral-four cable, if placed, could not be justified by the improvement in over-all attenuation. Using either nonloaded spiral-four or existing nonloaded paper-insulated conductors requires filters at the open-wire terminus. With these considerations, it was decided that suitable pairs would be used in the existing cable until exhausted. Subsequent cross-talk selection tests have indicated that 12 pairs, 6 for each line, are available.

Since there was no suitable central-office building at Milford, the repeater station in that vicinity must of necessity be in a new building preferably near the toll line. Commercial power is available only near the town, forcing a tentative location to be selected at the edge of the city.

With these selections of entrance-cable facilities and tentative repeater-station locations, the distribution of gain and line loss by repeater sections is shown in table I. A satisfactory distribution of line loss has been obtained and an analysis of these data shows that further improvement is impracticable. Therefore, the tentative repeater-station locations were adopted.

Figure 4 is a diagram of the major line and equipment parts of the Dallas-San Antonio lead. The *J* carrier path is shown by heavy solid lines, the *C* and voice on the same wire with the *J* by light solid lines, and all other circuits, classed as non-*J*, by dotted lines. Figures 5 to 7, inclusive, show in more detail the arrangements at the huts and repeater stations. The figures for the Dallas hut

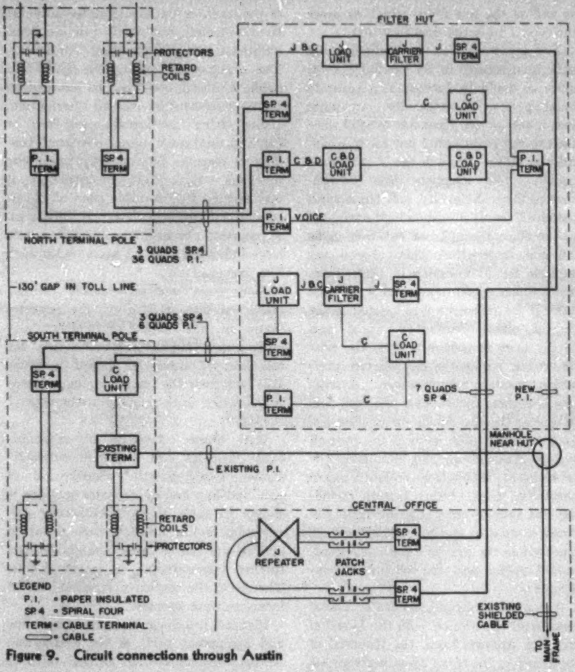
FILTER HUT
PROTECTORS
RETARD COILS
J B C
J LOAD UNIT
J CARRIER FILTER
J
SP 4 TERM
C
C LOAD UNIT
P. I. TERM
SP 4 TERM
P. I. TERM
C & D
C & D LOAD UNIT
C & D LOAD UNIT
P. I. TERM
P. I. TERM
VOICE
NORTH TERMINAL POLE
3 QUADS SP.4
36 QUADS P.I.
130' GAP IN TOLL LINE
J LOAD UNIT
J B C
J CARRIER FILTER
J
SP 4 TERM
3 QUADS SP 4
6 QUADS P.I.
SOUTH TERMINAL POLE
SP 4 TERM
C
C LOAD UNIT
SP 4 TERM
C LOAD UNIT
7 QUADS SP 4
NEW P.I.
P. I. TERM
C
MANHOLE NEAR HUT
EXISTING TERM.
EXISTING P.I.
CENTRAL OFFICE
RETARD COILS
PROTECTORS
J REPEATER
PATCH JACKS
SP 4 TERM
SP 4 TERM
LEGEND
P.I. = PAPER INSULATED
SP 4 = SPIRAL FOUR
TERM = CABLE TERMINAL
⊂⊃ = CABLE
EXISTING SHIELDED CABLE
TO MAIN FRAME

Figure 9. Circuit connections through Austin

and Temple repeater station are typical, and huts and unattended repeater stations not shown differ from these only in minor details. It will be noted that all wire on the toll line is brought through the repeater stations while only that wire on which *J* carrier is superposed is routed through the huts except at Austin where all wire to the north is brought through the hut to allow the future application of cross-talk suppression filters if required. For both huts and unattended repeater stations, short lengths of loaded spiral-four conductors are used from the six-inch-spaced wire at the terminal poles to the equipment in the buildings. A single continuously adjustable load unit is used for each pair and is located with the equipment. Paper-insulated pairs under the same cable sheath as the spiral-four conductors are used for the non-*J* wire.

As previously mentioned, the conditions at Austin were complicated by a single cable for existing circuits and a new cable for *J* carrier in both directions. Figure 8 is a diagram of the existing and new cables to the filter hut and terminal poles, and figure 9 shows the interconnection of circuits and equipment used at the filter hut, terminal poles, and central office.

Terminal and repeater equipment in existing offices is located in space adjacent to other equipment terminating toll circuits, and makes use of the common office equipment and power plant.

The new repeater stations and the filter huts are arranged for unattended operation. The equipment in the filter huts is such that no adjustments or attention are required other than periodic inspections. In the unattended repeater stations the power-supply equipment is automatic in its operation. Although periodic maintenance attention is necessary, it is desirable that any abnormal condition be recognized as soon as practicable and a system of alarms has been provided from each unattended station to an adjacent main repeater or terminal office. This alarm has been arranged to operate by direct current over one conductor between offices without interfering with existing telephone circuits but at the expense of one d-c telegraph path. For fuse failure, rectifier failure, power off, power restored, high-low voltage, high-low temperature, fire, burglary, pilot channel failure, and end of pilot channel control, alarms are sent and identified. A questionable alarm may be rechecked from the attended office.

Special Problems

Some of the problems encountered in connection with the other two systems may be of interest. At Corsicana, a repeater point on the Dallas-Houston system, a filter hut was used on only one side of the repeater station. The situation which led to this arrangement is that an intermediate cable in the Dallas-Houston line extends 0.2 mile north and 0.5 mile south from the local central office. As it is necessary that the *J* system operate through this entrance cable and since space was available in the local central-office building, repeater equipment similar to that installed in unattended buildings was placed in one room in the office.

The section of cable north of the central office terminates on a corner in a business district with all adjacent property occupied by buildings, making it more economical to use loaded spiral-four cable to this location rather than extend the existing cable to an available site and provide the necessary filter hut and equipment. For the longer cable, it was more economical to provide the filter equipment in a hut in order to use existing facilities. Although this cable terminates in a fully developed residential area, a site for a filter hut was obtained adjacent to an alley in the rear of one of the residences facing the street on which the terminal pole is located.

The use of nonloaded paper-insulated pairs in existing entrance cables has been mentioned. However, it is in general not practicable for cross-talk reasons to use all the nonloaded pairs which are available in one cable, and the selection of pairs suitable for type *J* operation is illustrated by a discussion of the methods used on the Dallas cable. The Dallas cable is composed of three sections of different make-up. The section nearest the central office, 1.3 miles long, and the intermediate section, 1.6 miles long, each contained 22 idle nonloaded pairs, and the third section, one mile long, had only 6. The Houston line terminates at the end of the second section, the third section extending the cable to the San Antonio line.

Since the number of cable pairs to the San Antonio line was limited to a maxi-

Table I. Distribution of Gain and Loss by Repeater Sections

Repeater Section	Cable: Length (Miles)	Cable: Loss (Decibels)	Open Wire: Length (Miles)	Open Wire: Wet Weather Loss (Decibels)	Maximum Tolerable Attenuation in Decibels per Mile at 140 Kilocycles Using 45-Decibel Repeaters	Maximum Tolerable Attenuation in Decibels per Mile at 140 Kilocycles Using 75-Decibel Repeaters
Dallas-San Antonio system						
Dallas-Milford	3.9 miles 16 gauge	17.50	49.2	16.4	0.560	1.165
Milford-Temple	Nominal	Nominal	84.7	27.3	0.532	0.885
Temple-Austin	1.1 miles spiral-4	2.20	74.4	24.8	0.575	0.980
Austin-San Antonio	1.1 miles spiral-4 1.8 miles 16 gauge	10.30	78.5	26.2	0.635	0.825
Dallas-Houston system						
Dallas-Corsicana	2.9 miles 16 gauge	13.20	52.5	17.5	0.610	1.175
Corsicana-Hearne	0.5 miles 16 gauge	2.25	90.5	30.2	0.484	0.805
Hearne-Hockley	Nominal	Nominal	85.6	28.6	0.545	0.875
Hockley-Houston	5.6 miles 16 gauge	25.20	29.1	9.7	1.190	1.700
Dallas-Longview system						
Dallas-Edgewood	1.4 miles 16 gauge	6.30	55.3	21.0	0.984	1.240
Edgewood-Longview	0.5 miles 16 gauge	2.30	69.7	26.5	0.614	1.040

mum of six and since the rate of circuit growth over the two lines was expected to be approximately the same, requiring cable relief over the entire distance when the branch to the San Antonio line was exhausted, an objective of six pairs to each line was set up.

Measurements of cross-talk coupling at 140 kilocycles in terms of inductance and capacitance unbalance were made between each pair and all other pairs in each section and the pairs rated in their order of desirability. It is of interest that this required a total of 854 measurements. Those pair combinations whose coupling of the mutual-inductance type was high were rated as the least desirable. This was done because capacitance balancing was to be used to obtain cross-talk reduction. The more desirable pairs in the first two sections were connected through to the six pairs in the last section by cut-and-try method until the over-all condition was such that all six pairs were acceptable. By a similar procedure, using the remaining pairs in the first two sections, six pairs to the Houston line were made acceptable. No record is available as to the number of tests made in the cut-and-try process.

A cable terminal on which balancing capacitors were mounted was installed in the central-office building and connected to the selected pairs. This terminal contained 66 small adjustable wire-wound capacitors which were connected between each pair and every other pair. The capacitors were adjusted to reduce to a minimum the capacitance component of the cross-talk coupling.

Buildings

For the three *J* carrier systems, four new repeater stations and eight filter huts were needed. The same type of construction was used for all: concrete foundation with floor slab above grade, double four-inch brick walls with rock-wool insulation between but with solid brick at corners and openings, pitched roof with wood framing, fire-resistant wallboard ceiling, fire-resistant composition shingles, and heat insulation above ceiling and below floor slab.

All of the racks for equipment in the unattended repeater stations are arranged in three rows with power, repeater, and line equipment in separate rows within a floor space of 17 feet by 16 feet which will allow the ultimate installation of six repeaters in each building. The entrance cables from the terminal poles enter from iron conduit through the floor and are racked and spliced on the side wall adjacent to the line bays. The stubs from the cable terminals at the top of the line bays are carried overhead to splices on the wall. A ceiling height of 13 feet is maintained above the equipment but reduced along the pitch of the

Figure 10. Equipment in unattended repeater station

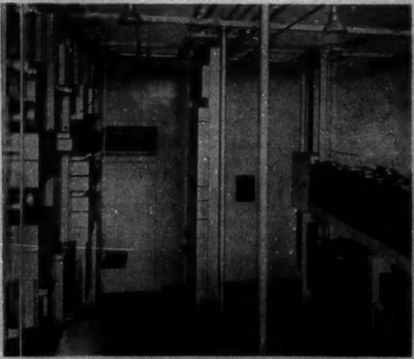

Figure 11. Typical hut and filter equipment

roof to 11 feet 8 inches at the side walls.

For all huts except that at Austin, three adjacent bays of racks are needed. With these along one side wall of the hut, the opposite side is available for splicing the entrance cable. At Austin an ultimate of nine racks, for filters in both directions of transmission, led to the use of racks along opposite sides of the hut with a splicing pit under the floor made accessible by trap doors in the floor between the lines of racks. In this case, the cable terminals are installed at the bottom of the racks with their stubs dropped directly through the floor slab into the splicing pit. The racks in the hut are seven feet high and a ceiling height of eight feet is used. Figures 7, 10, 11, and 12 are pictures of a typical repeater station, typical filter hut, and the special hut at Austin.

For correct operation of the equipment, temperature limits of 32 to 110 degrees Fahrenheit are desirable. Also, it is necessary that there be no precipitation of moisture on wiring or equipment. To maintain the desired conditions, each of the huts is equipped with a two-kilowatt blower-type electric heater arranged to operate at low temperature or high relative humidity, but with operation blocked when the temperature reaches 95 degrees. Each new unattended repeater station is equipped with a four-kilowatt heater similarly controlled and, on account of the heat dissipation of power plant and vacuum tubes, also has forced ventilation which is operative under conditions of high temperature. The system of forced ventilation consists of spun-glass intake filter, exhaust fan, electric-solenoid-controlled shutters at intake and exhaust, and thermostat, and is interconnected with the office alarms to prevent fan operation in case of fire.

Conclusion

Upon completion of the buildings, equipment installation, and line-facility rearrangements, adjustments in the equipment were made to match the lines used. Networks associated with the terminal and intermediate amplifiers were adjusted so that the amplification for any particular frequency would be equal to the attenuation at that frequency in the preceding repeater section; the automatic pilot channel equipment[1] compensates for attenuation changes. In repeater sections containing long toll entrance cables, it was necessary to sacrifice range of automatic pilot channel control to obtain the best equalization. However, satisfactory equalization and range of pilot channel control were obtained in every case.

As mentioned previously, the Dallas-Longview system operates on 12-inch spaced phantomed wire. In figure 13 the attenuation characteristics of two possible pairs are shown. The absorption peaks of pair *B* at 92 and 127 kilocycles are within the frequency range of channels number 4 and 12 of the *J* system and would impair the quality of those channels if pair *B* were used. Therefore, pair *A* is used as the regular path for the system. The quality of the channels obtained from these systems is shown by figure 14. Curve *A* is representative of that obtained from a system operating over six-inch-spaced wires; *B* and *C* are the best and poorest obtained from the Dallas-Longview system.

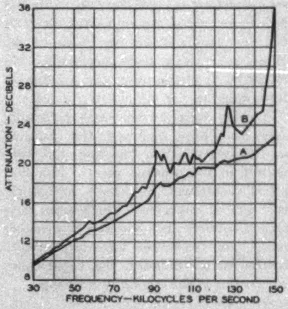

Figure 13. Attenuation of two 12-inch-spaced phantomed pairs of the Edgewood-Longview repeater section

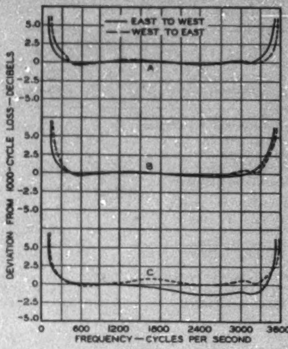

Figure 14. Quality of derived circuits

A for typical channel of systems on six-inch-spaced wire, *B* and *C* for the best and poorest channels of system on 12-inch-spaced wire

The Dallas-San Antonio, Dallas-Longview, and Dallas-Houston systems were placed in commercial service in September, October, and November 1938, respectively. Experience with these systems is that the circuits obtained compare favorably with those obtained from any other facilities in quality and continuity of service, and that a definite need has been fulfilled in providing an economical method of increasing the capacity of the existing plant.

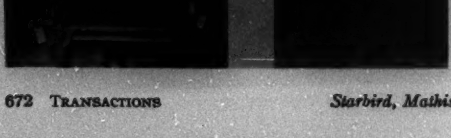

Figure 12. Austin hut and filter equipment

References

1. A 12-Channel Carrier Telephone System for Open-Wire Lines, B. W. Kendall and H. A. Affel. AIEE Transactions, volume 58, 1939, pages 351–60 (July section).

2. Line Problems in the Development of the Type J Open-Wire Carrier System, L. M. Ilgenfritz, R. N. Hunter, and A. L. Whitman. AIEE Transactions, volume 58, 1939, pages 656–65 (December section).

Discussion

Earl W. Lipscomb (Western Union Telegraph Company, Dallas, Tex.): In view of the practice of loading cables for use in voice and carrier circuits up to about 30 kilocycles, it is somewhat striking to note that loading is not used for the *J* frequencies, although greater normal attenuation and greater loss from impedance mismatch would be expected at these higher frequencies. The necessity for closely spaced loading coils at *J* frequencies may make loading prohibitive, but it would be interesting to know of the measures used to minimize high-frequency losses in standard nonloaded paper-insulated cable.

Treatment is prescribed for wires on the line which are not used in *J* systems to prevent their coupling the high output of the *J* repeaters back into the input of these repeaters. Similar difficulty may be experienced from wires on adjacent pole lines owned by other companies. While it may be possible to apply the same treatment to the foreign-owned wires and lines, it would be of interest to know something of the losses these measures would introduce into the foreign circuits. It appears that the problem may become more difficult in the event that the other companies desired to use similar high frequencies on their circuits. Field tests during the installation of the *J* systems probably have indicated a separation between lines beyond which the coupling effect need not be considered.

T. L. Keathley (Southwestern Bell Telephone Company, Houston, Tex.): In the paper so ably presented by Messrs. Starbird and Mathis on the subject: "Some Applications of the Type *J* Carrier System" you were told that four circuits of 0.128 copper wire were placed on pole-top extension fixtures on the Dallas-Houston and Dallas-San Antonio lines. It was also pointed out that the two wires forming a circuit were only six inches apart and transposed on point side transposition brackets at an average interval of 260 feet or on every other pole in the line. Several unusual and interesting construction features were involved in the operation of placing this wire.

The crossarm positions on a pole line are termed gains, and are numbered from the top downward. Incidentally, this will explain the derivation of the term "zero gain" which has been applied to the arm on the pole top fixture. Since the existing top arm is designated as the first gain, anything above that must have a lower number, and zero has been selected. Ordinarily the crossarms and wire are added to a pole line from the top downward; but this order is reversed in the case of placing wire in the zero gain. The new wire must be placed above the working circuits and it is very important that it be kept clear of the existing wire during the placing to avoid service interruptions. This is done by placing what is known as an extension side arm fixture on each of the arms on which the new wire is to be placed. This side arm is a two-inch by four-inch piece of timber three feet in length with four notches at one end to engage the new wire. At the other end and also at an intermediate point, this side arm is equipped with S-shaped strap-iron brackets, which hook under and over the arm that is to carry the new wire, the notched end extending beyond the crossarm.

After the wire is pulled out on the ground in 0.7-mile lengths, it is placed on the extension crossarm by laying-up sticks made of long cane poles with hooks on one end. Should the poles be high, linemen climb the poles and raise the wires to the sidearms with hand lines. The final sag and adjustment is then made by pulling at short intervals, this being necessary because the numerous transpositions in the circuits are effected on six-inch point side brackets and the line wire will not adjust itself around these brackets without some assistance. Each of these brackets has four pins for making the transpositions and is so arranged that one pin of each pair is higher than its mate; this is done in order that the left-hand wire (going in direction with the line) can pass over and change positions with the wire on the right, and at the same time afford clearance between the two conductors.

The spans on these leads averaged about 130 feet, and sag requirements on this length of span at 70 degrees Fahrenheit is $9^1/_2$ inches. Seventy degrees Fahrenheit is being quoted as that was about the average temperature prevailing at the time wire was placed.

Ordinarily, where wire is being sagged and the regular one point drop brackets are used, lengths of approximately one mile can be pulled on straight runs, but, due to the slack required at the numerous 6-inch point side brackets, shorter pulls had to be made, and a rather novel arrangement was worked out.

A groundman stationed at the sagging blocks was equipped with a 62-*A* test set. The tying crew consisted of four linemen, two of whom where also equipped with small test sets attached to one of the new circuits. The men with test sets worked on transposition points at all times, and the others on intermediate poles. When the proper sag was obtained through telephone conversation with groundman at the blocks, all four men completed their ties and each proceeded four poles forward and repeated the operation.

It might be of interest to know that in placing the four circuits of wire between Dallas and San Antonio and between Dallas and Houston, a distance of 555 pole miles, the following material was involved:

1,173,760 pounds of 128 copper wire amounting to 2,240 circuit miles

33,600 six-inch side point transposition brackets

20,640 H-beam pole-extension fixtures

22,880 eight-pin six-inch-spaced crossarms

266,400 Pyrex insulators

A total of 82,400 manhours, or 10,300 mandays of labor were required in placing the wire, extension fixtures, crossarms, and other associated materials. An oddity, in connection with material used on this project, is that approximately two miles more wire was required due to the use of point-side transpositions than would normally be required on ordinary brackets.

R. P. Brown (Southwestern Bell Telephone Company, Dallas, Tex.): The paper, "Some Applications of the Type *J* Carrier System," presented by Messrs. Starbird and Mathis brings out that the first three commercial installations of these systems were made in Texas. This is of considerable significance and brings about a thought as to what condition existed in Texas that would necessitate three separate installations of these systems so soon after the development and all about the same time. It is about this condition that my discussion will be confined.

In the national plan for handling intercity toll communications, there have been designated eight regional centers so located as to serve certain areas and each connected to all the others with direct lines so as to minimize the number of switches and furnish the highest possible grade of toll service. In the Southwestern area one of the regional centers is located at Dallas, and for handling the toll traffic in the area and connecting Dallas with the other regional centers there are seven major trunk routes through Texas which are described as follows:

(*a*). Dallas-Houston
(*b*). Dallas-San Antonio
(*c*). Dallas-Longview
(*d*). Houston-Beaumont
(*e*). Cisco-West
(*f*). Dallas-Red River (Oklahoma)
(*g*). Dallas-Cisco

The latter two mentioned which required relief before the conception of the 12-channel open-wire carrier were cared for by the placing of underground toll cable during 1930.

At that time the question of relief on the other trunk routes was becoming pressing and relief by placing cable seemed to be the proper method. Studies were prepared on that basis and as the relief was first necessary in the sections to Houston and San Antonio an inverted Y layout with the base at Dallas, the fork near Waco, and the two branches extending to Houston and San Antonio appeared to be the plan to follow. The Longview and Beaumont cables would follow later as would the extension of the Dallas-Cisco cable beyond Cisco.

To care for the growth until the cable relief was forced the open-wire pole lines were being developed to the maximum circuit possibilities by the use of the single- and the three-channel open-wire telephone carrier systems. Under this expansion the physical wire had grown to about five crossarms and by using the various instrumentalities the telephone circuit possibilities had been expanded to approximately 70 circuits.

With the information that a new carrier system was being developed that could be superposed on some of the present wire without sacrificing any of the present telephone circuits, thereby providing 100 or more additional telephone circuits, and which could be grown in groups of 12 telephone channels at a time without an extreme expenditure for the initial installation, it was apparent that this new development was just the thing required here in Texas to care for our problem and the first question was just how quickly could it be made available. As a result, the plans for relief by the placing of toll cables were laid aside and new plans developed on the basis of using the equipment that was under development and which would be rushed to completion due to the immediate requirement.

To use the newly developed 12-channel system on present open-wire leads it would be necessary to do some wire respacing and retransposing before the second system is installed; however, with the initial installation sufficient circuits would be available to care for growth and still allow present wire to be taken out of service for the rearrangements required to make the future additions. Where the circuit requirements could be cared for until the new carrier became available, as in the case of the Dallas-Longview line, the first installation would be placed on present wire and then the rearrangement work carried out, but in the cases where additional circuits had to be made available immediately, wire properly spaced and transposed for the new carrier system was placed and growth cared for using present available equipment until the new equipment was ready.

With the placing of the 12-channel systems in service for the longer distances it becomes possible to relocate the present terminals of the one- and three-channel systems to intermediate points in these trunk routes as well as in other sections, creating a very flexible circuit arrangement.

In conclusion it appears that the development of the new 12-channel open-wire carrier system was completed just in time to fit into the toll-line plan for Texas and definitely postpone for a considerable time the placing of toll cables over new routes.

R. B. Webb (Southwestern Bell Telephone Company, Dallas, Tex.): The development of a new toll instrumentality such as the type *J* carrier system immediately raises the question as to where this type of carrier can be most advantageously applied. Each major open-wire toll line must be studied to determine if the expected growth can be cared for most economically by providing additional physical circuits, additional three-channel and single-channel carrier systems, the use of the type *J* carrier systems, or by different combinations of all of these methods of relief.

The first step involved in the preparation of a toll line study is to secure a large amount of basic data, the chief elements of which are as follows:

1. Estimate of circuit requirements on the toll line, by circuit groups for several years into the future.
2. The present circuit arrangement and the number and types of carrier systems already in use on the line.
3. Basic toll-line data, covering the age of the line, pole heights, number of crossarms, and the maximum number of crossarms that can be placed.
4. Toll-entrance-cable data, listing the length, gauge, and number of conductors in the toll entrance cable at each central office involved.
5. The number and location of present carrier repeater stations.
6. The number and approximate location of repeater stations required for *J* carrier.

The most feasible plans of caring for the growth are developed from an analysis of the requirements and the local situation. The period of the study is determined by local conditions, although in general it is desirable to carry the study to a date after which the facilities will be common under all plans. Circuit layouts are made for each year considered, and all circuit additions and rearrangements, and the means used to effect them, are indicated by appropriate codes. These circuit layouts give, by successive steps, the complete program of caring for the growth under each plan during the study period.

The long-distance network is so complex that it is frequently necessary to include on the circuit layouts much more of the toll plant than the particular toll line under consideration. For instance, in the Dallas-Longview study, it was necessary to include the Longview-Shreveport line, the Longview-Jacksonville-Tyler line, and the Tyler-Mineola line. In so far as possible, the same number of toll circuits is cared for under each plan, and, if this is not done, the difference in facilities provided must be considered in interpreting the financial results of the study.

After the circuit layouts are developed, the additional outside plant construction, central-office equipment, and other items can be priced, and the total capital requirements obtained. Engineering cost studies are then made to determine the annual charges and operating expenses for each plan. The financial results thus obtained will usually indicate the proper method of toll-line relief.

The general study methods just outlined were employed in 1936 to determine the proper relief program on the Dallas-San Antonio, Dallas-Houston, and Dallas-Longview toll lines. Because each of these toll lines was approaching its maximum fill and because of the circuit growth expected, it was evident that the initial *J* carrier installation should be made on each of these lines either in 1938 or 1939. As the three toll-line problems were very similar it will only be necessary to comment on the Dallas-San Antonio study.

In this study, it was only necessary to determine what the penalties would be for deferring the *J* carrier from 1938 until 1939. It developed that thus deferring the *J* carrier relief would have required the stringing of so much toll wire and the placing of so many three-channel carrier systems that it was obviously an uneconomical procedure, and the installation was therefore carried out in 1938.

In this particular case, the study solution to the problem appeared fairly obvious from the start, since the circuit growth was about 12 circuits a year. However, in the case of a toll line not so heavily loaded, and where the annual growth is not so rapid, the economical time to install the initial *J* carrier will not be so apparent. For instance, in the study on the Houston-Beaumont toll line, the circuit growth was only about six circuits a year. The two plans of relief considered were: first, to install an initial *J* carrier system in 1939 and additional *J* systems as required; second, to provide other facilities in 1939 and 1940 and install the initial *J* carrier in 1941. This study was carried out over a ten-year period in order to make the two plans as nearly comparable as possible. It was found that the *J* carrier system should be installed in 1939 and this system will be placed in service in May of this year. Studies are to be made on other open-wire toll lines to determine when and where the type *J* carrier should be used.

L. C. Starbird and J. D. Mathis: The comments by Messrs. Keathley, Webb, and Brown are of interest in that they present additional information about the problems encountered in the application of these systems, together with amplification of some of the items mentioned in the paper. Mr. Lipscomb's comments leave several unanswered questions.

In regard to the use of nonloaded paper-insulated pairs for the *J* frequencies versus loaded pairs for the *C* frequencies, the normal attenuation of the nonloaded paper-insulated pairs is unfortunately very high. However, because of the close spacing of load coils required to load these conductors at *J* frequencies it is generally more economical to provide sufficient amplification to override the loss than it is to provide the loading. Impedance-matching transformers are provided at the filter hut and in the central office to minimize the loss to the *J* system and the crosstalk between systems that would be caused by an impedance irregularity. Nonloaded cable pairs may also be used for the *C* frequencies; however, in the application discussed, loaded pairs were already available for the *C* systems, and it was more economical to use these than to provide the amplification and impedance matching required if nonloaded pairs were used.

In regard to possible measures of reducing cross talk caused by paralleling wire near a *J* carrier repeater station, except for the more severe cases, this cross talk will be chiefly caused by longitudinal current which may be sufficiently suppressed by the insertion of longitudinal choke coils only. These coils have practically no effect on metallic currents and would not interfere with the operation of a carrier system over the paralleling wire at frequencies up to about 150 kilocycles. In the more severe cases of parallel it would be necessary to insert cross-talk suppression filters in the paralleling wire. The loss caused by these filters in the pass band is from about 0.1 decibel at voice frequency to 0.7 decibel at 30 kilocycles for each point where applied. In case the paralleling wire is to be used for carrier current at frequencies above 30 kilocycles, it will be necessary to co-ordinate repeater-station locations in order to prevent level differences. By co-ordinating repeater locations, the filters required on each line because of the systems on that line would serve to minimize the interaction cross talk without other special measures.

The questions are discussed in more detail in the papers listed as references and in the discussion with reference to those papers.

The Submarine-Cable Plow

C. S. LAWTON
NONMEMBER AIEE

A METHOD of protection of submarine cables by placement "underground" was described during the process of its development in an article[1] written for ELECTRICAL ENGINEERING in May 1938. The work then projected has since been successfully carried out, and the first cables scheduled to be plowed under, to protect them from being fouled by fishing gear dragged along the ocean bottom by steam trawlers, have been laid and buried in trenches to a depth of from half a foot to approximately two feet. In the latest operations, sections of cable 25 nautical miles (of 6,087 feet) in length have been dropped into a continuous furrow cut in the ocean bottom, from 500 to 2,700 feet below the surface of the sea. It is the purpose of this paper to describe the engineering features of the entrenching operation, and in a companion paper[2] will be found a description of a device, the submarine-cable depthometer, by which is measured the depth, in inches below the ocean bottom surface, at which the plowed-in cable actually lies in its trench.

Figure 1 shows the location of Western Union cables in trawling areas, with tabulations of interruptions and faults known to have been caused by trawlers since 1920. In general it will be noted that, if anything, damage has been heavier in the winter months when weather conditions are at their worst for the repair ships. Some of the cables are much more important than others from a traffic standpoint but the trawlers have no respect for that feature. It is estimated that trawler damage has cost all cable companies together as much as $500,000 in some years.

In the past, efforts to combat the damage have been concentrated principally upon the use of heavier and more costly cable, and the elimination of final repair splices, with their attendant bights of excess cable, from dangerous areas. The improvement resulting from these efforts has not been great because of economic limitations, and the practical difficulty of handling much heavier cable in deep water, in the first case; and because of reluctance to disturb cable beyond the limits of any one repair unnecessarily, and the time factor, in the second case. The most widely accepted type of cable for use in trawler areas is armored with ten number 1 Birmingham wire gauge soft iron wires, has an overall diameter of $1^3/_8$ inches, weighs 9 tons (of 2,240 pounds) per nautical mile in air or about 6.2 tons per nautical mile in sea water, and has a tensile strength of 20 tons. Many but not all of the interruptions and faults cited have occurred in this type of cable, the manufactured cost of which is about $2,000 a nautical mile.

For many years it had been jokingly said that some day the cables would have to be plowed under, but so far as is known, an American company was the first to give the idea the serious consideration it deserved, the attitude finally adopted being that although it always had sounded fantastic, nevertheless if successful it offered advantages too great to be passed up any longer without at least an effort being made to determine whether it was practicable.

Procedure

In order not to interfere with the operation of a working cable until the last possible moment, the procedure agreed upon for trenching was as follows: the ground would be explored in the vicinity of the existing line across the dangerous area for bottom as free as possible from irregularities, rocks, or other obstructions, and lowest possible towing resistance. A route would be chosen approximately parallel to the existing cable, and an independent section of cable would be buried along it, with both ends buoyed well clear of the vulnerable stretch. After thorough testing to verify that the section had been properly buried, the working cable would be cut on both sides of the vulnerable length and joined through on the buried section, the original length thus removed from circuit then being recovered, and if its condition justified it, repaired and made available for future use.

It soon was decided that the trenching and laying must be done as one operation. From a consideration of the weight and volume of cable to be handled in a single crossing of the trawler grounds it was also evident that the required amount could not be accommodated on the plow itself, but would have to be paid out from the ship's tanks through the plow as it progressed, it being essential not to interrupt the operation from start to finish if the trench were to be continuous.

Early Trials

Due to urgency of the matter, the first experiments in 1934 were conducted with plows which while incorporating the basic principles,[3] nevertheless were built from preliminary designs before there had been a real opportunity to come to grips with the technical problem and before the mechanics could be fully mastered. From these experiments much valuable information was obtained, however, which probably could not have been gathered in any other way. As the ship was to operate off the Irish coast it was decided to construct the plows in England, but in the intervals while the ship was proceeding to England and the plows were being built, a one-fourth-scale working model was constructed in the laboratory in the United States and tried out on a nearby beach. Wire rope was stretched along the ground under water to simulate cable and was buried by the model in both sandy and muddy bottom. The model dug in quickly but showed a tendency for the stern to lift a little when handling cable with much tension on it. Tension was important because in order to pass long lengths of cable through the plow in one uninterrupted operation, it was imperative that no slack accumulate ahead which would lead to a foul, and the only sure way of avoiding slack was to keep plenty of tension on the cable. The effect of the elevation of the towing position on the tendency for the stern to lift was obvious, and in an attempt to bring this force into more nearly horizontal alignment with the center of ground resistance, the towline was shackled directly to the vertical guard plate below ground level, a thin flat-bar link being used on edge to cut through the ground ahead. Fins at various angles were tried on the sides of the share to grip the ground on each side but these were of doubtful assistance in holding the rear down and were finally abandoned altogether. The towing tension for the model without cable was about 150 pounds. When burying wire rope on which 50

Paper number 39-54, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 5, 1938; made available for preprinting December 30, 1938.

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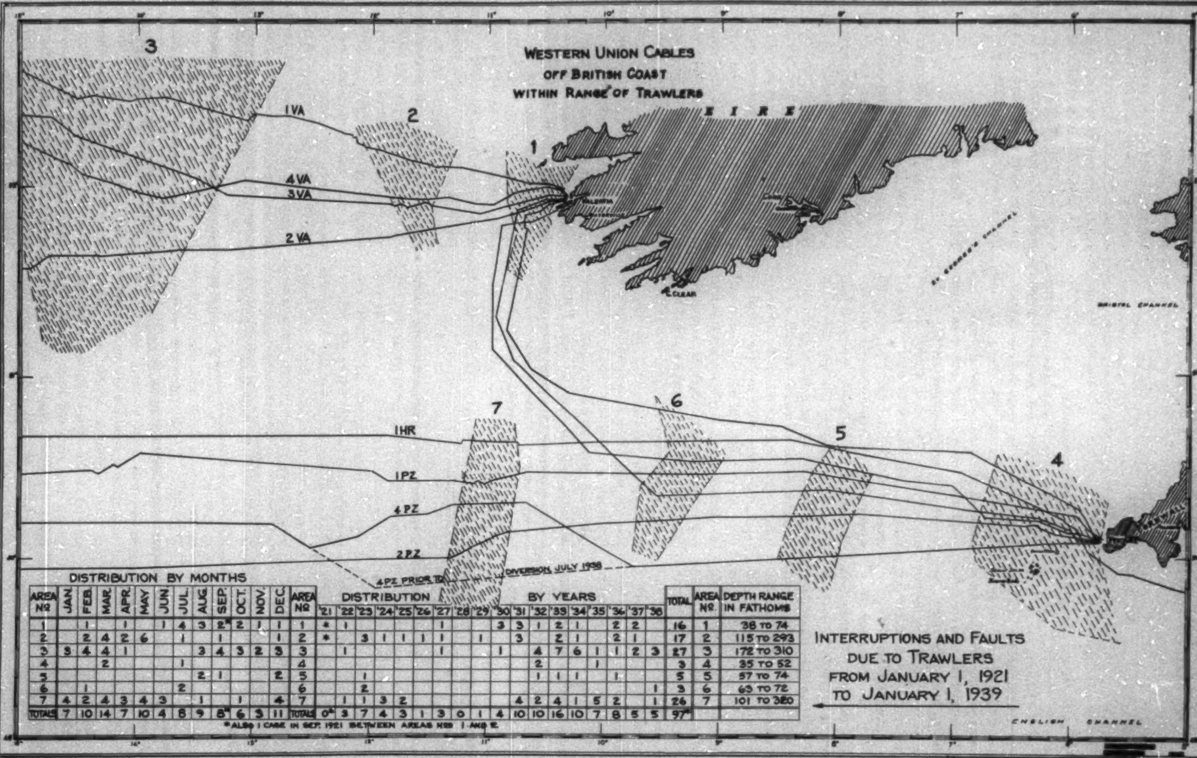
1. For all numbered references, see list at end of paper.


WESTERN UNION CABLES
OFF BRITISH COAST
WITHIN RANGE OF TRAWLERS
EIRE
1
2
3
4
5
6
7
1VA
4VA
3VA
2VA
1HR
1PZ
4PZ
2PZ
4PZ PRIOR TO DIVERSION JULY 1938
VALENTIA
C. CLEAR
ST. GEORGE'S CHANNEL
BRISTOL CHANNEL
ENGLISH CHANNEL
CORNWALL
15°
14°
13°
12°
11°
10°
9°
8°
7°
6°
5°

INTERRUPTIONS AND FAULTS
DUE TO TRAWLERS
FROM JANUARY 1, 1921
TO JANUARY 1, 1939

AREA No	DISTRIBUTION BY MONTHS												AREA No	DISTRIBUTION BY YEARS																		TOTAL	AREA No	DEPTH RANGE IN FATHOMS
	JAN.	FEB.	MAR.	APR.	MAY	JUN.	JUL.	AUG.	SEP.	OCT.	NOV.	DEC.		'21	'22	'23	'24	'25	'26	'27	'28	'29	'30	'31	'32	'33	'34	'35	'36	'37	'38			
1		1		1		1	4	3	2*	2	1	1	1	*	1					1			3	3	1	2	1		2	2		16	1	38 TO 74
2		2	4	2	6		1		1			1	2	*		3	1	1	1	1		1		3		2	1		2	1		17	2	115 TO 293
3	3	4	4	1				3	4	3	2	3	3		1					1			1		4	7	6	1	1	2	3	27	3	172 TO 310
4			2				1						4												2			1				3	4	35 TO 52
5								2	1			2	5			1									1	1	1		1			5	5	57 TO 74
6		1					2						6			2															1	3	6	63 TO 72
7	4	2	4	3	4	3		1		1		4	7		1	1	3	2						4	2	4	1	5	2		1	26	7	101 TO 320
TOTALS	7	10	14	7	10	4	8	9	8*	6	3	11	TOTALS	0*	3	7	4	3	1	3	0	1	4	10	10	16	10	7	8	5	5	97*		

*ALSO 1 CASE IN SEPT. 1921 BETWEEN AREAS NOS 1 AND 2.

Figure 1. Location of trawling areas off British coast

pounds tension was held, the towing tension was increased by about 10 pounds. Although the sliding friction was rather high, rollers or other moving parts to assist the passage of the cable were looked upon with some disfavor, because of the presence of so much sand and grit and the liability that they would be put out of action.

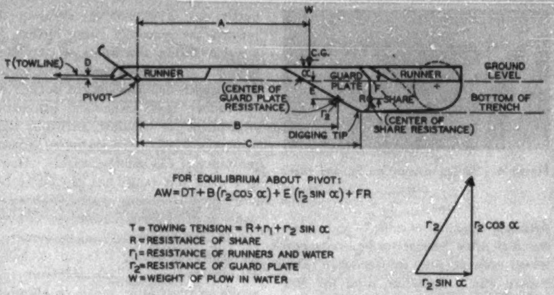

W
A
C.G.
T (TOWLINE)
D
PIVOT
RUNNER
α
GUARD PLATE
RUNNER
GROUND LEVEL
(CENTER OF GUARD PLATE RESISTANCE)
E
F
R
SHARE
BOTTOM OF TRENCH
r_2
B
DIGGING TIP
(CENTER OF SHARE RESISTANCE)
C
FOR EQUILIBRIUM ABOUT PIVOT:
AW=DT+B(r_2 COS α)+E(r_2 SIN α)+FR
T = TOWING TENSION = R+r_1+r_2 SIN α
R = RESISTANCE OF SHARE
r_1 = RESISTANCE OF RUNNERS AND WATER
r_2 = RESISTANCE OF GUARD PLATE
W = WEIGHT OF PLOW IN WATER
r_2
r_2 COS α
r_2 SIN α

Figure 2. Mechanics of the plow

The full-scale trials took place on a wet sandy beach on Bigbury Bay, near Plymouth, England, with horses and a tractor to furnish pulling power. The first plow tried embodied modifications by the ship's personnel to the original design for the purposes of increasing the full digging depth (from 12 inches to $19^1/_2$ inches, it being felt that 12 inches was insufficient on uneven ground), decreasing the frictional resistance of the cable (by introduction of a roller aft), and eliminating all possibility of the plow's remaining in a capsized position when landed on the bottom (by introduction of hoops over the top). The width of the cable trunk and consequent width of share were also increased as a precaution against broken wires and rucked-up outer-serving which might cause blocking of the passageway and jamming. The same dynamic characteristics noted with the model were exhibited by this plow, which dug a 14-inch to 18-inch depth of trench and developed 110 hundredweights towing tension. (This is the British hundredweight of 112 pounds.) Efforts to back-fill the trench were soon abandoned because the scrapers tended to lift the stern of the plow and because they were felt to be ineffectual and unnecessary due to the silting action of sand under water.

No trials had been made yet on hard bottom, and with the emphasis being placed on the necessity for weight aft to keep the stern down, the matter of unit bearing pressure on the digging tip was given scant consideration. This tip was well forward in the early designs, so that with the upward component of tension bound to develop in towing from a ship, there was not much encouragement for the tip to penetrate hard bottom. It soon became evident that the mechanics of the plow required further study; also that through the use of models the desired information could be collected in a much shorter time and at much less expense than with full-sized plows.

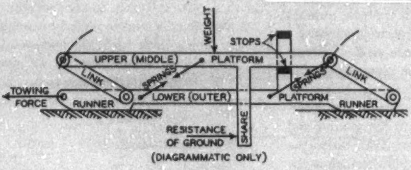


Figure 3. Diagram of automatic trench depth adjustment mechanism

Model Experiments

Space does not permit going into the experiments in detail, but as the conclusions dictated subsequent plow design they deserve to be mentioned. Briefly they were as follows, after 255 trials:

1. The displacement of material caused by the passage of the share does not obey the laws of streamline flow as in the case of fluids. The shape of entrance has little effect upon the resistance encountered. Over 60 model shares were tested, including square, half-round, and streamlined noses, and entrance angles in five steps from vertical to $22^1/_2$ degrees with horizontal.

2. Shares having small entrance angles with the horizontal give no reduction in towing resistance and do not retain their full grip in the ground unless heavily weighted. Therefore it is not safe to rely upon any shape of share to keep the plow in the ground, as a substitute for dead weight. A vertical entrance gives the smoothest tension graph.

3. Attempts to turn a furrow as on land do not succeed under water. Most of the material is forced aside, even with scoop-shaped shares.

4. Due to the flat deck plate, plows with their center of gravity aft will volplane at very moderate speeds, and once started will continue volplaning at lesser speeds.

5. The best weight distribution is with the center of gravity slightly forward of the digging tip because of (4) and also because on making bottom the plow will settle down on the digging tip and the two forward runners, in which position it is ready to penetrate hard ground in the same manner as an old-fashioned anchor resting on one fluke and its stock.

6. With a plow heavy enough to keep a vertical-entrance share digging properly, there will be enough weight to keep the cable from lifting the stern, even if the center of gravity is placed according to (5).

7. Resistance obeys no infallible law, varying widely in the same material with the same share. If the share is kept in the ground, speed of towing has no appreciable effect on resistance. In very hard bottom the relation between width of share and resistance is almost linear whereas in very soft bottom a reduction of 50 per cent in width is not accompanied by any noticeable reduction in resistance. Therefore a narrow share gives a steadier tension graph through different kinds of bottom than a wide share. Resistance usually varies about as the 3/2 power of the depth, but in some cases it varies more like the square. More experiments are necessary before these relationships can be definitely established. When expanding any one design to scale the resistance seems to go up about as the cube of the linear dimensions and this ties in well with investigations undertaken a few years ago on the holding power of anchors.[4]

8. Shares with parallel sides cause the material to be displaced along a fault line perhaps 30 degrees from horizontal and to leave mounds to either side which are roughly triangular in shape, their combined cross-sectional area being equal to the area of the trench below normal ground level. After passage of the share the material gradually, but not immediately in clay or hard bottom, slips back into place, covering the trench. Tidal action greatly assists this of course. No observations were possible in locations entirely free from tidal influence.

Figure 2 shows the elements of a cable plow in contact with the ground. Runners are attached to a deck plate. The share is a long steel casting cored out to

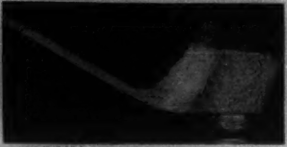


Figure 4. Stellite overlay on tip and guard plate

permit the passage of cable to and underneath an after "sheave" or grooved wheel which rolls it into the trench. (This sheave was a departure from the first design as in spite of probable bearing difficulties it was felt to be necessary to relieve the friction on the cable as much as possible.) Ahead of the share and welded to it is the triangular guard plate which protects the digging tip and allows the share to surmount obstructions. Naturally a good design aims not at equilibrium but at a safe margin in the factors on the right-hand side of the first equation. For weight distribution as recommended in (5) dimension C must exceed A, and as the lever A determines the effectiveness of the weight it is easy to see why a plow must be both long and heavy.

Design of Present Plow

Indications from the model experiments were that it would be unwise to design a plow for a trench depth beyond ten inches because of the anticipated maximum towing tensions. However, it was felt that the need for a deep trench was not as great in hard ground as in soft and that a way might be worked out to let the depth of trench vary automatically with the consistency of the ground, thus leveling off the towing tension. (The above figure of ten inches was based on the types of bottom available for model work, which were many but naturally did not extend much beyond low water mark.) This was accomplished by constructing the plow in two parts linked together like members of a parallel rule,[5] the middle part, corresponding to the upper member of the rule when set in a vertical plane, carries the share and cable guides. The outer part, corresponding to the lower member of the rule, carries the runners and towing lugs. Resisting the link motion and tending to keep the two parallel parts together are four helical tension springs, two of which are mounted so as to be adjustable (figure 3).

When the ground resistance reaches a predetermined maximum, the springs become active, allowing the middle part to swing aft and up, thus lifting the share partly out of ground as necessary to prevent the resistance rising further, if possible within the limits of the link motion. In the present plow the maximum trench depth below the bottom of the runners is 16 inches and automatic adjustment is possible between this figure and 8 inches at a maximum ground resistance of 255 hundredweights. However even this range has proved to be insufficient to ensure against higher resistances as at times a resistance of over 300 hundredweights has been encountered in digging a trench 8 inches deep by $4^1/_4$ inches wide. This is about the minimum practicable width, as allowing for $^3/_4$-inch walls the cable trunkway is but $2^3/_4$ inches wide, which leaves only $^1/_2$-inch clearance for the cable at splices.

Figure 4 shows a close-up view of the digging tip and guard plate. The abrasive wear has been so severe that only recently have we been able to protect the tip and plate edge well enough for them to survive one operation. This has been accomplished by streamlining the tip, not to reduce the towing resistance, but to spread the wear over a greater area, thus reducing unit bearing pressure, and by grinding the plate edge to a 15-degree double chisel edge, then applying number 1 grade stellite to the wearing surfaces.

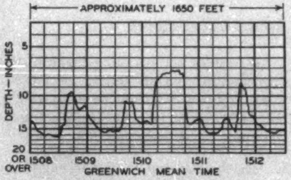


Figure 5. Extract from penetration record for 1HR cable, showing net depth of trench below normal ground level

The runners are made with a narrow tread so that when the plow passes into really soft bottom where the resistance can be counted upon to ease off considerably, they will sink into the ground and allow the deck to settle down on to the mud. With runners 12 inches below the deck the maximum depth of trench is thus increased to something approaching 28 inches. This figure is probably unattainable because of the mound thrown up beside the share which would keep the deck from settling down to normal ground level.

The way in which the variable depth feature functions in actual practice is illustrated by the graph in figure 5, an extract from the record taken last summer (1938) while burying the Western Union 1*HR* direct Paris cable across number 7 trawling area. The means for obtaining this and other records will be described later.

To pass cable through the plow with minimum tension on it and yet to avoid the possibility of slack occasionally creeping in necessitates some means of measuring the cable tension at the plow, as the formula in use for computing residual tension is not sufficiently accurate, the conditions at the ship cannot be held sufficiently uniform, nor can the ship be controlled with sufficient exactness to rely otherwise upon anything but a large margin of safety. It was therefore decided to incorporate a cable dynamometer[6] in the design of the present plow. This takes the form shown in figure 6, and utilizes the offset in the cable line required to place it below ground level by measuring the reaction on a grooved sheave over which the cable takes a bend of 36 degrees.

At the same time it is required to know whether cable is sinking to the bottom ahead of the plow or whether the plow is pulling the cable down forcibly. A pivoted inverted-U-shaped member arranged to swing over the forward cable bellmouth and counterweighted in such a way as to cause it to seek the vertical bears lightly against the underside of the cable, changing its angular position to accommodate itself to the elevation of the cable entering the bellmouth. This is called the "cable feeler."[7]

For measuring the depth of trench, or penetration, a weight is trailed along on the end of a rod free to swing in a longitudinal vertical plane. The weight rides along on the ground beside the share. It changes its angular position to accommodate itself to changes in elevation of the share with reference to the ground.[8]

A pendulum is used to record the angle of the plow's deck with the horizontal. It swings longitudinally so as to give slopes of ground traversed, also to furnish a useful check on the penetration gauge.

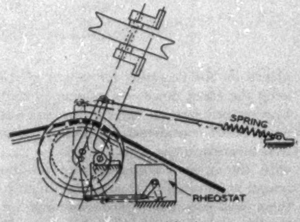


Figure 6. Diagram of cable dynamometer on plow

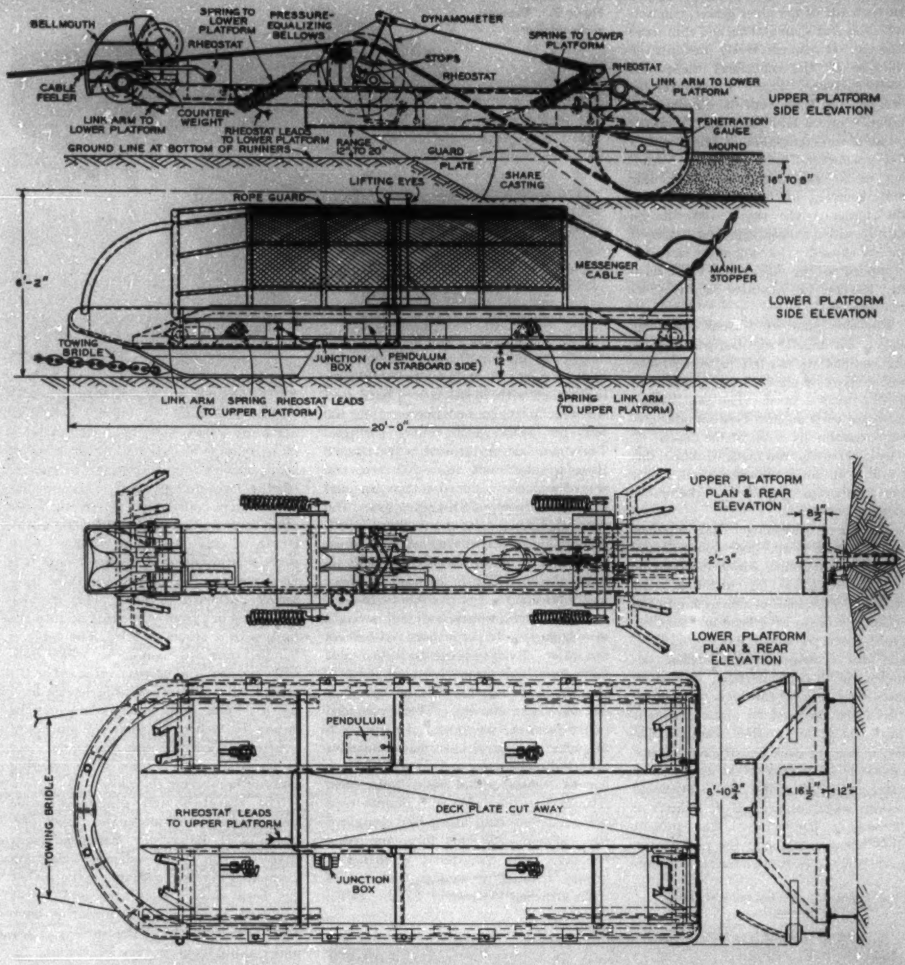

BELLMOUTH
SPRING TO LOWER PLATFORM
PRESSURE-EQUALIZING BELLOWS
DYNAMOMETER
RHEOSTAT
SPRING TO LOWER PLATFORM
STOPS
RHEOSTAT
RHEOSTAT
LINK ARM TO LOWER PLATFORM
UPPER PLATFORM SIDE ELEVATION
CABLE FEELER
LINK ARM TO LOWER PLATFORM
COUNTER-WEIGHT
RHEOSTAT LEADS TO LOWER PLATFORM
RANGE 12" TO 20"
PENETRATION GAUGE
MOUND
GROUND LINE AT BOTTOM OF RUNNERS
GUARD PLATE
SHARE CASTING
16" TO 8"
LIFTING EYES
ROPE GUARD
6'-2"
MESSENGER CABLE
MANILA STOPPER
LOWER PLATFORM SIDE ELEVATION
TOWING BRIDLE
JUNCTION BOX
PENDULUM (ON STARBOARD SIDE)
12"
LINK ARM
SPRING
RHEOSTAT LEADS (TO UPPER PLATFORM)
SPRING
LINK ARM (TO UPPER PLATFORM)
20'-0"
UPPER PLATFORM PLAN & REAR ELEVATION
8½"
2'-3"
LOWER PLATFORM PLAN & REAR ELEVATION
PENDULUM
TOWING BRIDLE
RHEOSTAT LEADS TO UPPER PLATFORM
DECK PLATE CUT AWAY
JUNCTION BOX
8'-10¾"
16½"
12"

Figure 7. General arrangement of plow

Damping is necessary to iron out shocks and jerks incidental to the plow's motion. It is accomplished by having the pendulum submerged in a special heavy grade of castor oil.

A drawing and photograph of the completed plow are shown in figures 7 and 8. The superstructure is designed to keep the various lines from fouling the plow while lowering and getting under way.

The angular motions of the cable dynamometer, cable feeler, and penetration gauge are transmitted to rheostat arms through linkages, whereas the pendulum rheostat is mounted on the same shaft and in the same container as the pendulum itself.

Telemetric System Between Plow and Ship

The operation of rheostats under hydrostatic pressures up to 1,200 pounds per square inch presented a few difficulties which however were successfully overcome by enclosing the units in containers filled with castor oil, chosen because it is not injurious to rubber insulation and has a reasonably low coefficient of compressibility. The containers communicate with a central bronze bellows also filled with castor oil and with enough capacity to absorb the volume changes caused by the hydrostatic pressure. Transmission of the external pressure to the interiors of the containers equalizes the pressure on each side of the cover gasket, connecting cable and shaft glands, and eliminates leakage. It also effectively prevents the collapse of the containers themselves under pressure. Figure 9 illustrates the design. The leads from the rheostats are taken to an oil-filled junction box on the plow where connection is made with a four-conductor "messenger cable" leading to the ship and to four recording milliammeters and ballast resistances on the bridge. The ship's 110-volt d-c supply, with a voltage regulator to keep it steady, is used. One side of the circuit is grounded to the ship's hull. Each rheostat has one side grounded directly to its container.[9]

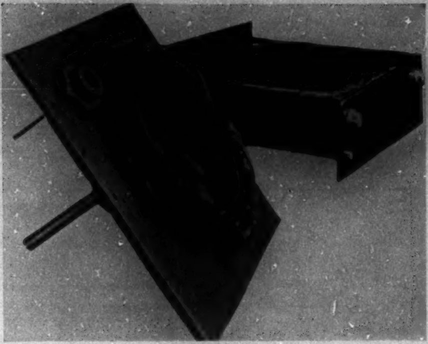
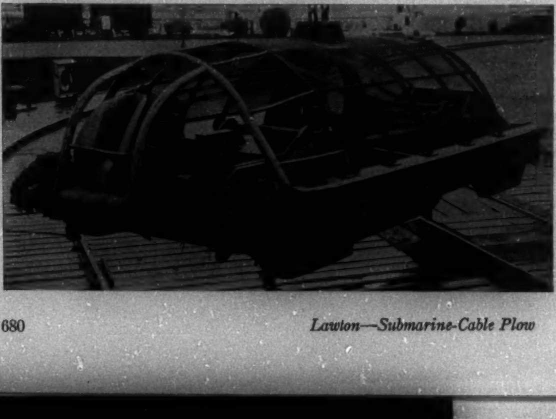


Figure 9. Rheostat and container

For close analysis a chart speed far above that usually supplied with recording instruments was felt to be desirable, and a speed of one inch per minute was finally decided upon. The ship is also equipped with an echo-type sounder giving a continuous graph of the depth. A simple alteration was made to enable this graph to be run at the same speed, giving five continuous graphs over the entire plowing operation.

The use of a messenger cable separate from the plow towline introduced a complication in handling which it would be desirable to avoid, but although other means of transmission such as sound impulses and radio have been investigated, a cable seems the only practicable method. The idea of incorporating electrical conductors in the towline itself had to be abandoned due to the high stresses involved and necessity for almost constant readjustment in towline length. The first messenger cable constructed became unreliable after very little use in deep water apparently due to vibrational stresses which caused fatigue failures in the copper conductors. The present cable has gone through the past season successfully. It has conductors of cadmium copper. Each insulated conductor is sheathed with 26 galvanized hard-steel wires of 0.022-inch diameter and 100 tons per square inch tensile strength. These are laid up around a coir heart, then wormed with tarred hemp, and served with compounded cotton tape and one layer of compounded jute yarn. The over-all diameter is about three-fourth inch. The cable has an ultimate tensile strength of 83 hundredweights. It is as near to being in torsional balance under tension as it is possible to make a cable and has the extremely light weight of only one pound per fathom (six feet) in sea water. Even so it has to be provided with buoyancy near the plow end to prevent its being run over by the plow when getting underway. This is provided in the form of two groups of eight-inch-diameter alloy-steel ball floats made for the trawling industry and sewed up in long canvas "sausages" of nine balls each. Attached to the cable with manila rope stoppers, they trail along like elongated toy balloons. In this form the eddy resistance and consequent vibration is greatly reduced as against that for 18 balls attached separately to the cable.

Figure 8. General view of plow



The messenger is made in one length of 2,000 fathoms because splicing is very difficult due to its construction, and if an accident occurs by which the cable is damaged it is essential to have plenty of spare uninterrupted length after cutting off the damaged portion. The surplus cable is kept coiled down flat in one of the after cable tanks on the ship, the entire length of cable being in circuit. With every readjustment in towline length it is necessary to readjust the length of messenger outboard to suit. This is done by paying out from or picking up into the tank with a steam winch. The cable is handled over a dynamometer which indicates tension, and passes overboard at the stern from a special portable derrick having a 20-foot steel boom which can be swung to either the port or starboard quarter for keeping the messenger clear of the ship's twin screws when lowering away or recovering the plow.

The rheostats and messenger cable were manufactured under the close supervision of H. Kingsbury, European plant engineer, The Western Union Telegraph Company, London, with whom the author has been closely associated and whose technical knowledge and initiative have contributed greatly to the success of the undertaking from its very beginning.

Lowering the Plow

The plow itself weighs about 180 hundredweights in air. It is handled from the forward well deck by one of the foremast derricks specially reconstructed to give greater outreach and equipped with its own steam winch. The whole gear was tested to 400 hundredweights before being used. A strong line is taken from the forward hold, over one of the twin sets of cable gears, thence over one of the bow sheaves and along the outside

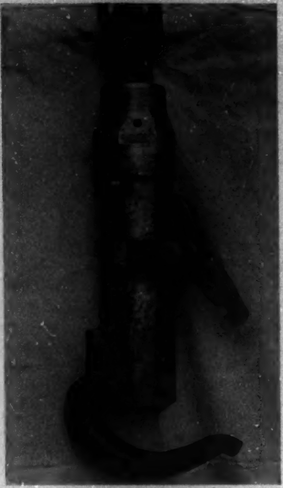


Figure 10. Releasing gear

of the ship to the well deck where it is made fast to a special attachment on top of the plow. The derrick purchase is also made fast to the same attachment and serves to lift the plow (with towline and messenger cable also attached) over the bulwark and lower it about 20 fathoms over the side. Picking up then commences on the other line (and towline) which brings the plow up transversely under the ship's overhanging bow platform and headed to port. The derrick line is next unshackled and the plow is in position for lowering to the bottom.

While still on deck a rope strop has been reeved through the cable passageway in the plow and its two ends shackled together. With the plow under the ship's bow the strop is unshackled and the end passing into the forward end of the plow is joined onto a rope passing over one of the three large bow sheaves, over the other cable paying-out gear and down to one of the cable tanks where it is spliced to the cable end. Previous to the commencement of operations a mushroom anchor has been planted with a length of heavy ground chain at the position where the tow is to commence. A buoy is moored to the tail of the mushroom to facilitate subsequent recovery. Another buoy is moored to the end of the laid-out chain. The ship now steams slowly up to the latter buoy, takes it aboard, transfers its moorings to the after end of the strop through the plow, and drops the mooring overboard. The ship's head is swung into position 90 degrees clockwise from the course to be followed, and the plow is ready for lowering.

A special form of releasing gear[10] which would not release the plow when the ship's head dropped suddenly on the swell had to be designed, as the ordinary form lets go immediately upon relief of tension, and the plow with its large deck area sometimes cannot sink quite as fast as the ship is liable to drop it, when the lowering rate is compounded on the swell motion. This apparatus (figure 10) contains a hydraulic cylinder and plunger working somewhat like a door-closer. After release of tension a compression spring forces the plunger slowly down while a needle valve governs the rate of escape of the water therefrom. Resumption of tension before the plunger has completed its travel immediately returns the plunger to its initial position by recompressing the spring, a foot-valve in the cylinder opening to permit re-entrance of water. If tension is not re-established before completion of the plunger travel, this being the period for which the device is set, the plunger throws a trigger and opens the tumbler hook. The period is set by adjusting the needle valve. Ten seconds has been found to be satisfactory. After the plow has landed on bottom and the required ten seconds have elapsed, the lowering line and releasing gear are hove back on board.

While lowering the plow it is essential that ship and plow do not change their relative headings by as much as 180 degrees, for in that event the plow may be swung around in a complete circle upon the commencement of towing. If this happens, the cable will have a turn around the towline and the foul cannot possibly be eliminated except by recovering the plow. For this reason we prefer to lower away at the shallow end of the job, usually in about 90 fathoms, also because the towline with which the ship gets underway is shorter at this end and it is easier to pay away chain than to pick it up under high stress in readjusting the length with changes in depth of water.

Keeping Cable Clear

To provide a good spread between cable and towline at the top, as soon as the plow has been landed on bottom the cable-end rope is transferred aft along the starboard side of the ship to and over a large sheave mounted thereon somewhat abaft amidships, so as to leave the ship at that point instead of the bow. To accomplish this requires that a strop be previously rigged over the side sheave with both ends brought forward. The cable-end rope is parted at a connection and the strop is inserted by joining the rope on to its ends, the lower one of which is then dropped overboard. Following this, the ship swings to course and moving ahead (figure 11) resumes paying out towline and messenger and begins to pay out cable as necessary for the commencement of towing. It should be noted that the plow slides down the cable-end rope rather than the cable itself, which avoids possibility of damage to the cable.

Towing Position

Early in the experimenting the question of the best towing point on the ship came

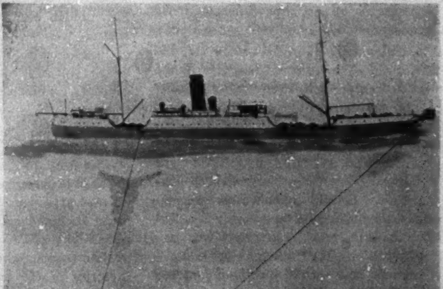


Figure 11. Plow on bottom. Ship paying out towline

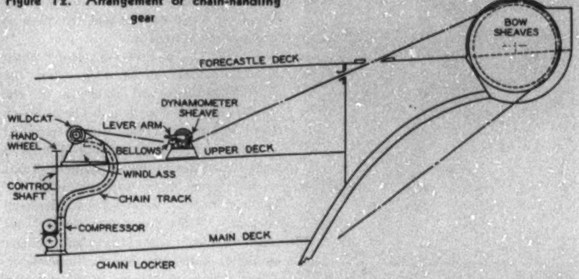
up. Contrary to popular impression it is not the stern. When a ship swings, she pivots on a point well forward of amidships. If the stern is held, the rudder is not effective. Cable repair ships are equipped to handle rope and cable over the bow and their powerful cable engines are forward, not aft. It was therefore natural to choose the bow as the towing position.

Towline

In order to obtain a horizontal pull at the plow it was obvious that the towline would have to be ballasted. The original arrangement consisted of from 40 to 85 fathoms of $1^1/_4$-inch ground chain at the lower end, shackled to a steel and hemp grapnel rope. The ship's cable drums are suitable for handling this type of rope under the stresses usually experienced in cable repair work, but they provide only a frictional grip which is insufficient to prevent slippage when paying away under high tensions. Bulky connections in the rope every few hundred fathoms were a constant source of trouble in passing the drum, being apt to take riding turns and lead to slipping and surging which was ruinous to the rope and dangerous for those handling it. The tendency of rope to twist under tension made necessary the use of a special roller-bearing swivel ahead of the chain. The features which finally eliminated rope for towing however, were the fact that chain had to be handled anyway before the plow could be taken back on board, and the limits imposed on the size and therefore strength and weight of rope by the ship's cable drums. The grapnel rope mentioned had a very low factor of safety but was the largest rope which could be accommodated.

A continuous chain for towing has the advantage of more weight to absorb the vertical component of the towing tension, greater springiness due to the curve produced by its suspended weight, freedom from torsional stresses under tension, and practically no restriction upon the length which can be manufactured without connections. Should connections be required they could be more easily handled than those in rope. A more positive grip is provided by the "whelps" or "snugs" of the wildcat over which chain is handled on a windlass. Against these had to be considered the extra lift in recovering the plow at the deep end of the tow and the fact that the ship's windlass was not designed for such a heavy load.

Figure 12. Arrangement of chain-handling gear



Whereas space limitations and the ship's regular duties prevented the installation of larger cable drums, a more powerful windlass could be and was installed within the existing space. The plow weighs 155 hundredweights in water. The longest lift is in 450 fathoms depth where the vertical leg of chain in suspension weighs 200 hundredweights, giving a maximum total dead load of 355 hundredweights on the windlass and chain. Naturally with the ship responding to a swell, the live load at times raises this much higher. The windlass rating is 400 hundredweights at a speed of 50 feet a minute, which is really slower than desirable, but when the plow is lifted the weight decreases as the chain comes inboard.

The chain is one-inch-diameter stud-link made in nickel-steel by the die-lock process in one continuous length of 700 fathoms. It has an ultimate tensile strength of 126,000 pounds (1,125 hundredweights) and was proof-tested to 84,000 pounds (750 hundredweights). It weighs 40,102 pounds in air, or about 0.445 hundredweight per fathom in sea water. It was manufactured at Chester, Pa., under the supervision of E. R. Harrall, who is on the staff of our engineer of lines, New York. The strength/weight ratio is about double that for ordinary one-inch stud-link wrought-iron chain.

Chain Handling

When it came to handling the chain on a wildcat, complications arose. The usual anchor chain is picked up under a relatively small load, as the weight of the anchor and suspended chain is not great in terms of the chain's strength. It is usually payed out on the run with the windlass engine declutched and only moderate braking, whereas in our case we have to both pick up and pay out chain under very heavy loads.

The action of a wildcat is something like that of a chain sprocket on a bicycle except that the grip is on the outside of the links and we are not dealing with an endless chain which, unless it contains slack, cannot ride up out of the sprocket. The ideal cat for picking up under load is not the ideal one for paying out under load, because it is almost impossible to achieve perfect uniformity in the pitch of the links of a long chain.

If two wildcats could be accommodated the ideal design would include one for picking up, which would have the pitch of the pockets slightly greater than that of the links, and another for paying out, with the pitch of the pockets slightly less than that of the links. Unfortunately there was no room for two cats in our case. The best we could do was to exercise great care to make the pitch of the cat exactly right and then clamp down a rigid specification for pitch uniformity in the chain itself. The length over any six links when stressed to the proof load was successfully held to a tolerance of $^1/_{16}$ inch, which is about one-tenth the tolerance allowed on an anchor chain of this size by the Navy. As a further safeguard against links riding up out of the pockets of the cat, a special snubbing arrangement was designed, consisting of a curved chain track and multiple compressor installed between the locker and the cat. The whole arrangement is shown in figure 12. A water-cooled brake 40 inches in diameter by 13 inches wide installed on the windlass intermediate shaft has capacity to absorb some 300 horsepower.

The ship's cable dynamometers could not be used with chain passing directly down through the deck to the windlass, and their sensitivity falls away rapidly toward the higher end of their scale making them unsatisfactory for measuring towing tensions anyway. In order to avoid passing the chain through the deck at too small an angle of intersection, a guide sheave had to be introduced ahead of the wildcat. The obvious thing to do was to utilize the reaction of the chain against this wheel, due to its change in direction, to measure tension. To do this, the middle portion of the shaft upon which the wheel turns was made larger in diameter and eccentric to the two ends, like a cam. The wheel is mounted on two Timken tapered roller bearings. The shaft itself is also free in the two end bearings (also tapered rollers) but is keyed to a lever arm extending horizontally to a fulcrum on top of a large bronze bellows mounted on the same cast-steel bracket as the shaft. The offset of the cam center is in the same line with but opposed to the lever arm. Tension on chain passing under the wheel causes an upward reaction on the offset cam and the shaft end bearings, which produces a downward reaction of the lever arm on the bellows. Communicating with the bellows are five pressure gauges, one close by and at practically the same elevation which is used as a monitor, one near the windlass, two back-to-back on the forecastle head, and one on the bridge. The bellows system is filled with a light grade of oil. To calibrate the dynamometer, weights are suspended over the bow on the chain and pressure readings are taken on the monitor gauge. To calibrate the other gauges, pressure is created artificially in the bellows by screwing down on the upper adjustable stop of the lever arm until any desired reading is re-established on the monitor. The other gauges can then be marked directly in hundredweights tension from the calibration curve of the monitor. A disadvantage is that, except for the monitor, the gauges are all at a higher elevation than the bellows. It follows that when tension is removed from the chain these gauge lines are below atmospheric pressure due to the gravity head which the bellows, being flexible, refuses to support. Very slight air leakage has thus far been unavoidable except by keeping enough pressure artificially on the bellows to maintain all lines above atmospheric pressure. It is possible that an electric repeating system from the monitor gauge could be worked out which would be easier to maintain but thus far no very dependable way of making a direct conversion from hydraulic pressure to electrical readings has been discovered. The fluid displacement in such a system is very small and cannot be greatly increased without interfering with its greatest asset, the practically linear response over the entire range of tensions. The dynamometer is illustrated in figure 13.

To assist in quick determinations of the amount of chain towline required to keep the vertical component of towing tension within a reasonable maximum at the head of the plow, a series of graphs has been prepared, of which figure 14 is a sample, each for a different length of chain. The graphs also indicate the horizontal distance from ship to plow, the amount of chain, if any, on the ground ahead, and the horizontal component of towing tension, all based on the catenary formula. To obtain this information for any length of chain it is necessary only to enter the proper graph with the depth and towing tension at the ship. Small differences in depth between ship and plow are usually negligible, as is the distortion of the catenary curve due to water resistance against the chain at plowing speeds.

Passing Cable Through Plow

If it were possible to maintain the speed of ship and the cable tension at the ship with precision, and if the depth were fairly uniform, the ideal condition for passing cable through the plow would be to so adjust towline length as to keep the plow at the exact distance behind the ship where the cable, sinking at its natural rate, were tending to make bottom. Due to so many variables however, this point is constantly shifting and the towline cannot be controlled with sufficient agility to attempt successfully such a procedure. Cable laid with no more than sufficient tension at the ship to place it on bottom free of residual tension tends to sink in a straight line, the angle of sinking depending upon the speed of the ship. Excess tension at the ship distorts the straight line toward the catenary shape, increasing the length in suspension. For moderate excess tensions the line is still nearly straight, the curvature in the lower portion being only slightly less than that due to inherent stiffness in the cable where it comes tangent to the bottom, and therefore fairly local. If under these conditions the plow is brought up to the ideal or "critical" point of tangency it has to come fairly close before the cable feeler begins to indicate the lifting of the vertical angle of cable approaching the plow. Should the mark be overshot the cable angle rises steeply and there is almost inevitably a bad lead with high friction against the upper part of the bellmouth, requiring immediate corrective action at the ship. Figure 15 illustrates this condition. There is bound to be considerable lag when checking the speed of a ship or changing the shape of a long length of suspended cable, for which reason it is felt safer in the present state of the science to keep the plow a safe distance astern of the critical point, with cable entering the bellmouth from a few degrees below horizontal. By far the greater part of the cable laid last summer (43 miles) was placed on bottom ahead of the plow without any noticeable increase in lead friction from this feature.

It is significant that whereas before the advent of the messenger cable and electrical instruments it was a difficult problem to avoid accumulations of jute and compound, broken wires, etc., which eventually blocked the cable passageway of the plow and terminated the tow, there has been no trouble of this description since.

Residual Cable Tension

Observations made of the plow upon recovery point to the existence of lateral subsurface currents in the areas worked, cable markings almost always showing a tendency for cable to enter the plow bellmouth a bit off the centerline, not always on the same side. This lateral displacement of cable from its natural line cannot be controlled. It results in a certain minimum residual tension in the cable as left by the plow, usually sufficient to eliminate any helical set remaining in the cable from coiling in circular

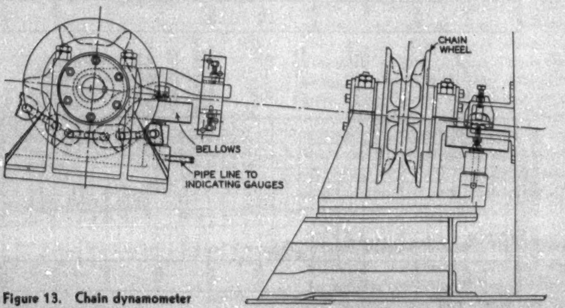


Figure 13. Chain dynamometer

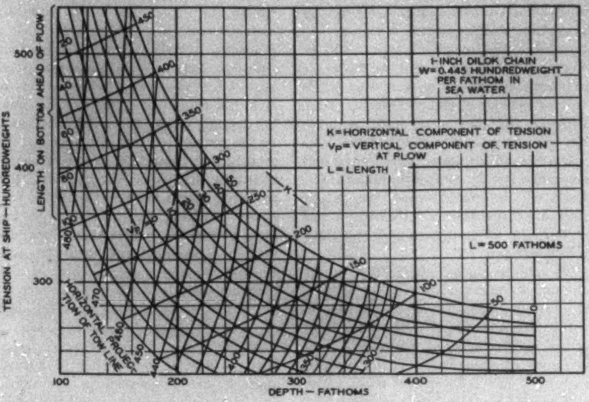


Figure 14. Sample of chain graphs

tanks, but at times the cause of anxiety when passing over irregularities in the ground. The amount of tension left on the cable by the plow is registered on one of the milliammeters. At the same time, ground slopes are registered on another. Records of the ship's speed permit transformation of the time ordinate of the pendulum graph into distance, after which the slopes may be integrated to produce the actual contour of the ground where irregularities have been observed. The penetration graph is then consulted and the bottom of the trench laid off. A scale template of the catenary curve of the cable under the corresponding residual tension laid over the drawing of the trench will at once show whether there has been any suspension of cable across a depression, and if so whether the suspension has been enough to lift the cable out of the trench (figure 16 is a sample irregularity).

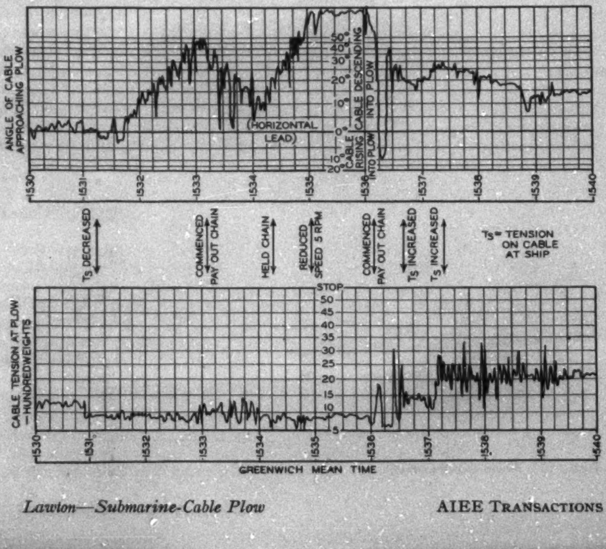
It is impossible to retrace any charted route with exactness. The irregularities which are apt to cause suspension are extremely small in extent, from the navigational viewpoint. No sounding machine on a ship rising and falling to a swell could possibly detect them. Their danger lies in the rate of change of ground slope, not in the degree nor amount of slope. An advance survey of the ground with one of the pendulums, recently given the name of "clinometer," mounted upon a small sled can and does serve to indicate the general character of the bottom, but cannot be relied upon to provide any information as to individual irregularities to be encountered. The only prudent practice at present is therefore to use a type of cable of high specific gravity and with enough unit weight in water to come within the sensitive range of the ship's dynamometer, and to place that cable in the trench with the least possible residual tension.

Devices have been considered which would furnish a pull on the cable by the plow itself, thereby relieving the cable of tension behind, but their application is difficult. We are trying to perform an operation in which over a period of several hours, perhaps 150 tons or more of cable are passed uninterruptedly through a hole only $2^3/_4$ inches wide which is inaccessible and out of sight. Given a practical solution of the problem of the power supply, any hauling device is apt to go wrong at times. If for any reason the machine failed to maintain a speed as great as the speed of the plow's advance or the cable became jammed, its grip would be a liability rather than an asset. Traction from caterpillar treads could be used as a source of power but slippage would be a serious problem. There are already three lines between the ship and plow. Addition of a power transmission line to an electric motor on the plow would call for prodigious feats of seamanship if it were to be handled successfully in the open ocean. As it is, operations are confined to the very best of weather and this cannot be counted upon to last long off the Irish coast nor to give much warning when it passes.

Adjusting Length of Towline

It will be observed that in adjusting towline length, there are two conditions to be met: The control of the vertical component of towing tension at the plow and the need for keeping the plow back of the critical point where the cable is landing. In hard ground, more prevalent out to 250 fathoms depth than beyond, it is necessary even with the heavy all-chain towline to keep the plow far behind the landing point of the cable,

Figure 15. Behavior of cable as plow passes through critical point (from 1HR record)



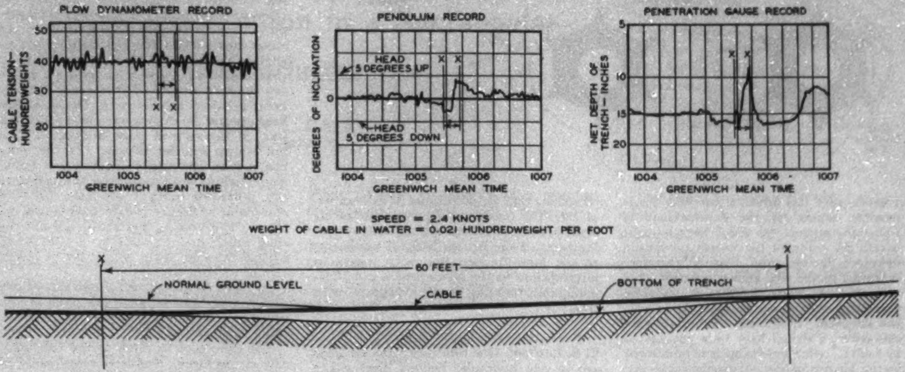


Figure 16. Indication of possible cable suspension, from instrument records

whereas in very soft ground farther out it may be necessary to pay out the entire towline to put the plow far enough astern to keep clear of the critical point. It is hoped that by continuation of model experiments a form of share developing less resistance can be evolved, which will improve the first condition. The second can always be met by having plenty of reserve towline in the locker.

Core Soundings

In determining the consistency of the bottom, a streamlined weight carrying fins set at a slight angle to give it rotation like a rifle shell is allowed to drop to the bottom on a special stranded sounding wire with ball-bearing swivel attachment. Fixed to the lower end of the weight is a bit made of three-quarter inch pipe which is forced into the ground by the impact and brings up a core of bottom material. From the appearance of the material and depth of penetration of the bit, a rough idea can be obtained of the probable towing resistance. This useful device was developed by the commanding officer of Western Union's cable ship "Lord Kelvin," Captain M. H. Bloomer, whose enthusiasm and pioneering skill, both as a navigator and seaman, have been invaluable to the whole plowing development.

Table I. Summary of Data From Plow Instruments

	4PZ	1HR
Cable tension at plow (hundredweights)		
Maximum	45	31
Minimum	11	9
Average	30	19
Vertical angle of cable into plow (degrees)		
Maximum	+50	+30
Minimum	− 5	−10
Average	+10	− 1.3
(+ = downward into plow)		
Ground slope (degrees)		
Maximum	4	5
Net depth of trench (inches)		
Maximum	over 20	over 20
Minimum	7	3*
Average	14	9

* Momentary only.

Cables Trenched

During the past summer (1938) Western Union's *4PZ*, the high-speed loaded cable, and *1HR*, the direct Paris cable, have been trenched across number 7 trawling ground and although only time can furnish conclusive proof of the success attained, the instrument records look good. At no point along either route (*4PZ*, 24½ nautical miles; *1HR* 18 nautical miles) did the share come completely out of the ground. Table I gives a summary of the technical data obtained from the plow instruments. Only one really bad spot was recorded from the point of view of suspension across an irregularity on either route. This is the example given in figure 16. Although crossings were made in the vicinity with the depthometer it could not be found, but this is not surprising considering its small extent. Ground resistance eased off on the *4PZ* route beyond 250 fathoms but the *1HR* was hard going all the way out to 400 fathoms where the plowing terminated.

There were parts toward the western end of both jobs where the cable lead was not good because with the length of towline available the plow could not be brought far enough astern to avoid the critical point. Cable tensions carried in the eastern part of *4PZ* were much higher than on *1HR*, whereas the penetration on *4PZ* was more satisfactory at the western end. Due to continued hard ground in the deeper parts of *1HR* the vertical component of towing tension at the plow even with full towline rose until it appears that the share was prevented from maintaining its full grip.

The outlook is for better results with more practice in deep water and increased length of chain towline. The plow is still in its infancy. Eventually it is hoped to handle lighter types of cable and to develop auxiliary uses such as the protection of harbor cables from ships' anchors, and the burying of military cables.

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Discussion

W. S. Gorton (Bell Telephone Laboratories, Inc., New York, N. Y.): To one who has been familiar with the unpredictable and the unknown factors involved in the laying and picking up of submarine cables, Mr. Lawton's paper presents a striking record of engineering achievement. It is significant,

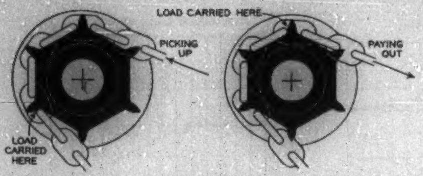

Figure 1

I think, that this advance has been made possible largely by the development of measuring instruments which have brought within the realm of the known previously unknown factors, and that it therefore conforms to the well-known criterion that we can make little progress in scientific knowledge until we can measure the quantities involved. It is most appropriate that this criterion should have been enunciated by Lord Kelvin, whose name is so prominent in the history of the submarine-cable art. All the resources of engineering, however, would have been of no avail without the ability instantly to adapt the various controls to meet the exigencies of sea and weather. This required seamanship of a high order.

The thoroughness of the treatment does not leave much room for questions, but I should like to ask Mr. Lawton why the hydraulic bellows system, used to measure the pull of the chain, had trouble from air leakage. Systems of this sort are widely used, and the makers seem to have overcome the leakage problem. It would seem that a spring could be used to exercise a force on the bellows and prevent the pressure from ever falling below atmospheric. I should be glad to have a fuller explanation of the action of the wildcat; it is not clear to me why the chain has to be made to such rigid specifications for pitch uniformity.

J. J. Gilbert (Bell Telephone Laboratories, Inc., New York, N. Y.): The procedure of plowing in submarine cables which has been described is a valuable contribution to the submarine-cable art. The solutions to the rather difficult mechanical problems in the design of the plow are very ingenious and the operations involved in its use represent a great advance in cable-laying technique. It is hoped that the expectations of the author will be fully realized, so that plowing will prove to be an unfailing means of placing cables where they will not be interfered with by other users of the ocean bottom. The paper mentions precautions that have been taken toward keeping the plow right-side up on the ocean bottom. I would be interested to know whether in actual experience the plow upsets very often. Also, is there any danger when the plow upsets of its righting itself in the wrong direction, that is, of turning a side somersault? The paper gives a very interesting summary of cable interruptions in the trawling areas off the British Isles. I would like to ask how the situation with regard to interruptions in the Nova Scotia and Newfoundland trawling areas compares with this.

C. S. Lawton: One difficulty with air leakage in the hydraulic bellows lines arises from the fact that the gauges on the forecastle head must be removed for safety in bad weather. This means having a cock and union coupling just under each gauge, the tightness of which is not easily maintained. Also, an hour may elapse between the time when the dynamometer is put in readiness for operation and the time when the plow is landed on bottom and towing tension builds up. During lowering a check must be kept on the chain tension in order to keep a proper adjustment of the load between towline and lowering line, but the chain tension does not rise high enough during this period to overcome all the vacuum on the dynamometer gauges.

A spring, such as Doctor Gorton suggested, could be employed to keep the lines above atmospheric pressure while the dynamometer was not in use, but if left in place during operation it would interfere with the readings. We are in fact using an equivalent arrangement now, but it is only partially effective for that reason.

In regard to chain uniformity, figure 1 of this discussion illustrates the preferred designs where space allows two wildcats to be fitted: one for picking up and the other for paying out with the pitch of the pockets set as explained in the text of the paper. In paying out chain under load, if the pitch of the pockets and consequently the snugs be slightly less than that of the chain, the oncoming link has more clearance in getting over the snug (at the bottom of the sketch) and a slight irregularity of chain pitch will not matter if this normal clearance is enough. As each snug comes off at the top, the whole chain moves forward slightly so as to bring the load onto the preceding snug. This little readjustment does no harm but is necessary to take care of irregularities in pitch. However, if this same cat is used for picking up, riding and jumping is bound to occur, as the conditions are reversed. In order to function smoothly on chain containing pitch irregularities the snugs and pockets for picking up must be slightly greater in pitch than the chain, so that the load is carried on the snug where the chain is coming off, the chain actually being pried off the cat by a circular wedge held against the deep groove into which the links in the vertical plane project. This is not shown on the sketch. As the snug comes free, the whole chain moves backward slightly so as to transfer the load onto the preceding snug. By this means a reasonable clearance can be maintained between the oncoming link at the top and the snug which rises to meet it, so that small irregularities in chain pitch can be taken care of without trouble.

As there was no space for two cats in our case it was essential that the pitch of the snugs and pockets be exactly right for the chain at all points.

The difference in pitch of the two wildcats in the figures is greatly exaggerated of course.

With reference to Mr. Gilbert's discussion, in the early designs of plows in which the weight was much less than the present design, hoops were fitted over the top of sufficient strength to support the full weight if the plow landed upside down, and of such a shape as to cause it to roll over immediately.

This is not necessary now that we are using instruments which give instantaneous indications on board ship of the plow's behavior. The pendulum will not assume the position of 0 degrees (plow deck horizontal) or anything approaching this unless the plow is right-side up. If the plow were to capsize, the pendulum would swing over against one of its two stops and remain there, giving a reading greater than 90 degrees.

Since the instruments have been installed there has never been a case where the plow has turned over. The attachment of the lowering line is made well above the plow's center of gravity and the only critical moment is after release of this line when the towline is still snubbed up short to keep it from fouling and the ship's head is rising and falling to the swell. Once the towline has been paid away to a length substantially greater than the depth of water there is no danger on any ordinary bottom. Trial tows made with a transverse pendulum in use over long distances have shown no dangerous canting of the plow to either side.

Trawling off Nova Scotia and Newfoundland appears to be on the increase but to date it does not cause as much trouble as it does off Ireland. The cables affected on the European side are mostly main transatlantic sections, whereas on this side the damage is mostly confined to short connecting cables of lesser individual importance from the traffic standpoint. It is expected to use the plow on this side after the other side has been protected.

Current Distribution in a Rectangular Conductor

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ASSOCIATE AIEE

Synopsis: The extensive use of rectangular conductors for large alternating currents has made the determination of their electrical characteristics a problem of practical importance. Although these conductors are usually used in groups and in special configurations, no solution other than that of direct measurement has been given even for the basic problem of the distribution of current in a single isolated conductor. The mechanical complications of the direct measurement method are great, and the results obtained are necessarily an approximation.

This paper presents a method by which the current density inside a rectangular section may be analytically determined if the surface current density, a comparatively easy quantity to determine, is known.

THE distribution of current over the cross section of a conductor is the determining factor in the calculation of voltage drop, power loss, and mechanical forces. When uniform, as in direct current, the calculations are comparatively simple. A conductor carrying an alternating current, however, has a varying magnetic field associated with it which causes a variation in current density, both in magnitude and phase at each point of the cross section. This gives rise to great difficulty in the calculations.

The magnetic field inside the conductor, which is the cause of the nonuniform distribution, is produced by the current in the conductor, and currents in adjacent conductors. In view of this, early analytical investigations were limited to single isolated conductors of a circular cross section. Here, radial symmetry makes a one-dimensional analysis possible, Heaviside[1] and Lord Kelvin[2] obtained the solution in terms of Bessel functions.

If the conductor is not circular, or if more than one conductor is considered, the problem is two dimensional, and greater difficulty is experienced. Special cases of two circular conductors have been solved.[3–5] Dwight[6] has given the results for tubular and flat conductors.

Various attempts have been made to approximate the current distribution in rectangular conductors. These have been completely experimental, in that the current distribution was measured directly,[7] or calculated by assuming the conductor to be made up of a number of small sections over which the current density was assumed constant.[8] Others[9,10] have considered elliptical cross sections.

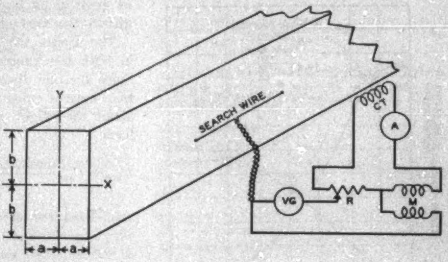

Figure 1. Cross section of rectangular conductor, showing co-ordinate system and measuring circuit

Object

This paper presents a method of determining the current distribution over a rectangular section. It is partly experimental and partly analytical, in that the boundary conditions for the differential equation of current distribution are measured on the surface of the conductor, and by calculation, the current density at any point inside the conductor determined. A single isolated bar is considered in this initial work, but with little change, the general case may be solved.

Theory

The conductor is assumed to be of unit permeability, homogenous in composition, sufficiently long and far enough removed from its return circuit to be unaffected by it. With these conditions, end effect and proximity effect are absent and the current distribution over the cross section may be considered two dimensional and all flow axial.

The origin of co-ordinates (x,y) is at the center of the rectangle whose dimensions are $2a$ and $2b$ (figure 1). Mathematical derivations are given in appendix I, and are briefly as follows:

1. From Maxwell's equations, the differential equation of the current density is obtained.

2. The solution of the differential equation is obtained, using the measured values of the surface current density as boundary conditions. The process is the conventional method of product solutions and sums of these.[11]

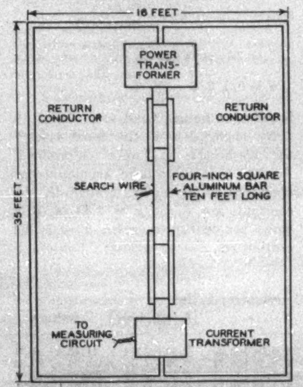

Figure 2. Diagram of connections for test

Paper number 39-1, recommended by the AIEE committee on basic sciences, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted April 8, 1938; made available for preprinting November 23, 1938.

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This paper is part of a dissertation presented for the degree of doctor of philosophy at Yale University.

The author wishes to acknowledge his gratitude to F. R. Dallye of the Aluminum Company of America for the loan of a number of machined bars for use in these tests; A. T. Sinks of the General Electric Company for the loan of a calibrated current transformer; and his indebtedness to Lars Onsager, assistant professor of chemistry, W. W. Sherwood, formerly of the electrical engineering department, and H. M. Turner, associate professor of electrical engineering, all of Yale University, for their aid and encouragement in this work.

[1] For all numbered references, see list at end of paper.

3. The current density is given by

$$U_{x,y} = U_{a,b}\frac{\cosh \epsilon x \cosh \alpha y}{\cosh \epsilon a \cosh \alpha b} + \Sigma A_n(\cosh \mu_n x \cos ny) + B_m(\cosh \Psi_m y \cos mx) \qquad (1)$$

where

$U_{x,y}$ = current density at the point (x, y)

$U_{a,b}$ = current density at the point (a, b), a corner of the cross section

$\epsilon, \alpha, \mu_n, \Psi_m$ = constants defined in appendix I

A_n, B_m = coefficients of the Fourier series developed from the measured values of surface current density

x,y = co-ordinate distances measured in centimeters

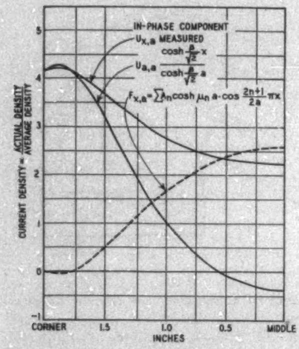

Figure 3. In-phase component

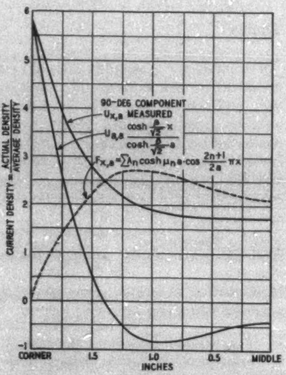

Figure 4. Ninety-degree component

Figures 3 and 4. Measured values of current density and boundary conditions

Four-inch-square aluminum bar at 60 cycles, 29 degrees centigrade

4. The a-c resistance of the conductor may be calculated easily and directly from the real part of the term (see appendix II)

$$Z = \frac{\rho U_{a,b}}{\int U_{x,y}\,dx\,dy} \text{ ohms per unit length}$$

Application to a Square Bar

As a check on the theory presented, a four-inch-square aluminum bar was tested. The circuit dimensions are shown on figure 2. The surface current density was measured as described in appendix III. For convenience, the values are given in terms of (actual current density)/(average current density) so that an ordinate of, say, 2.5, means that the current density at that point is 2.5 times as great as it would be if the current distribution were uniform (figures 3 and 4).

The square bar allows simplifications in both test procedure and calculation. Since the bar dimensions are equal and the current distribution symmetrical about the origin, equation 1 takes the form

$$U_{x,y} = U_{a,a}\frac{(\cosh \beta x/\sqrt{2})(\cosh \beta y/\sqrt{2})}{\cosh^2 \beta a/\sqrt{2}} + \Sigma A_n\left(\cosh \mu_n x \cos \frac{2n+1}{2a}\pi y + \cosh \mu_n y \cos \frac{2n+1}{2a}\pi x\right)$$

The function, $F_{x,a}$, which is expanded in a Fourier series, is equal to the difference between the measured surface current density, $U_{x,a}$, and the calculated quantity, $U_{a,a}(\cosh \beta x/\sqrt{2})/(\cosh \beta a/\sqrt{2})$. This function vanishes at the end points, thus the series in odd half harmonics is convergent at these points.

$$F_{x,a} = U_{x,a} - U_{a,a}\frac{\cosh \beta x/\sqrt{2}}{\cosh \beta a/\sqrt{2}} = \Sigma A_n \cosh \mu_n a \cos \frac{2n+1}{2a}\pi x$$

$$n = 0, 1, 2, 3 \ldots$$

is shown on figures 3 and 4.

By application of the usual rules,[12] the coefficients A_n were determined. For the four-inch-square aluminum bar operating at 29 degrees centigrade, the constants are (using $\rho = 2.84 \times 10^{-6}$ ohms per centimeter cube, $a = 5.092$ centimeters, $f = 60$ cycles):

n	μ_n	$A_n(\cosh \mu_n a)$	$\cosh \mu_n a$
0	$0.94+j0.90$	$2.46+j3.03$	$11.46-j\,58.71$
1	$1.17+j0.71$	$0.29-j1.00$	$-169.23-j\,90.83$
2	$1.63+j0.51$	$-0.18+j0.17$	$-1705\ +j990$
3	$2.19+j0.38$	$0.06-j0.03$	$-12{,}570+j32{,}800$

$U_{a,a} = 4.14 + j5.90$, $\beta = 0.9133\,(1 + j)$

The contour charts, figures 5 and 6 show values of current density and phase angle at each point of the cross section as determined by calculation. A three-dimensional sketch of lines of equal current density is given in figure 7.

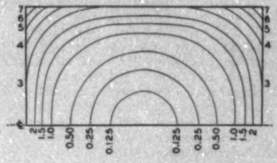

Figure 5. Lines of equal current density

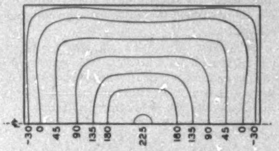

Figure 6. Phase variation in degrees from phase of total current

(– indicates lead)

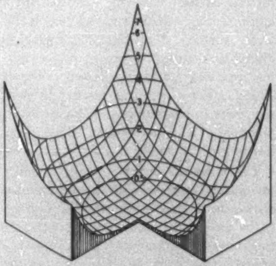

Figure 7. Isometric view of lines of equal current density

Figures 5–7. Distribution and phase variation of current density

Four-inch-square aluminum bar at 60 cycles, 29 degrees centigrade

The a-c resistance of the bar per unit length is the real part of

$$Z = \frac{\rho U_{a,a}}{\int U_{x,y}\,dx\,dy} = \frac{4.14 + j\,5.90}{103.71} \times 2.84 \times 10^{-6}$$

or 11.76×10^{-6} ohms, which is slightly more than four times the d-c resistance.

Verification of Solution

The validity of the expression for current distribution was established by comparing: (1) its integrated value with the known total current in the bar, and (2) the individual values at particular points with those obtained by direct measurement.

1. COMPARISON OF CALCULATED TOTAL CURRENT WITH MEASURED VALUE

$$\underset{\substack{\text{Cross}\\\text{section}}}{\int\int} U_{x.y}dx\,dy =$$

$$\int\int\left\{U_{aa}\frac{\left(\cosh\frac{\beta x}{\sqrt{2}}\right)\left(\cosh\frac{\beta y}{\sqrt{2}}\right)}{\cosh^2\frac{\beta a}{\sqrt{2}}} + \Sigma\frac{A_n}{\cosh\mu_n a}\left[\cosh\mu_n x\cos\frac{2n+1}{2a}\pi y + \cosh\mu_n y\cos\frac{2n+1}{2a}\pi x\right]\right\}dx\,dy$$

$$= \int_{-a}^{+a}\int_{-a}^{+a} U_{x,y}dx\,dy = 4\int_0^a\int_0^a U_{x,y}\,dx\,dy$$

$$= U_{a.a}\frac{8}{\beta^2}\tanh^2\frac{\beta a}{\sqrt{2}} + \Sigma(-1)^n \times \frac{A_n 16a}{(2n+1)\pi\mu_n}\tanh\mu_n a$$

$n = 0, 1, 2, 3 \ldots$

$\beta^2 = j\,1.668$
$\tanh^2 \beta a/\sqrt{2} = 0.9947 - j\,0.0005$
$\tanh \mu_0 a = 1.0003 + j\,0.0004$
$\tanh \mu_n a \approx 1.0000 + j\,0.0000$
$n = 1, 2, 3$

The following tables show the values of each integral term, and the effect on the sum of including the next term. Value of each term:

$$\frac{8}{\beta^2}U_{a.a}\tanh^2\frac{\beta a}{\sqrt{2}} = 27.91 \quad - j\,19.59$$

$$+\frac{16aA_0}{\pi\mu_0}\tanh\mu_0 a = 77.55 \quad + j\,10.39$$

$$-\frac{16aA_1\tanh\mu_1 a}{3\pi\mu_1} = 1.74 \quad + j\,6.32$$

$$+\frac{16aA_2\tanh\mu_2 a}{5\mu_2\pi} = -\,0.35 + j\,0.66$$

$$-\frac{16aA_3\tanh\mu_3 a}{7\mu_3\pi} = -\,0.09 + j\,0.07$$

Value of integral:

Including $n = 0$ term	$105.46 - j\,9.20$
$n = 1$ term	$107.20 - j\,2.88$
$n = 2$ term	$106.85 - j\,2.22$
$n = 3$ term	$106.76 - j\,2.15$

The actual value, on the basis of an exactly square section, since the reference quantity used was average density, is equal numerically to the area of the bar 103.71 + j 0. The agreement is thus approximately three per cent.

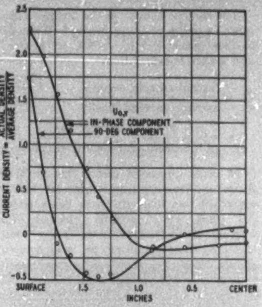

Figure 8. Comparison of calculated and measured values

Measured points indicated by dots

$U_{0,y}$ is the current density at points on Y axis, figure 1

2. COMPARISON OF CALCULATED AND MEASURED VALUES

By using two two-by-four-inch bars clamped rigidly together to give the square section for test, it was possible to insert measuring wires to determine the values of the current density along a plane through the center of the bar. This comparison is shown on figure 8.

Conclusions

1. A method for determining the current density at every point of the cross section of a rectangular conductor of any dimensions has been developed.

2. The method is partly experimental and partly analytical, in that the solution of the differential equation of the current density is obtained using the measured boundary values.

3. A-c resistance of the bar is determined.

4. Application of the method to a square bar is given, and curves showing the magnitude and phase of the current density over the cross section presented.

5. An extensive investigation on bars of various dimensions is now in progress. It is hoped that the necessary conditions for a completely analytical solution will be determined.

Appendix I. Mathematical Derivations

Maxwell's equations in centimeter-gram-second practical units are

$$\nabla XH = 0.4\pi U \qquad (2)$$

$$\nabla XE = -10^{-8}\frac{\partial B}{\partial t} \qquad (3)$$

where H is the magnetic field, E the dielectric field, U the current density, and B the flux density. Since the only component of current density is in the Z direction, and the permeability unity (2) and (3) may be written as

$$\frac{\partial H_y}{\partial x} - \frac{\partial H_x}{\partial y} = 0.4\pi U$$

$$\frac{\partial E}{\partial x} = \rho\frac{\partial U}{\partial x} = \frac{\partial H_y}{\partial t}10^{-8}$$

$$\frac{\partial E}{\partial y} = \rho\frac{\partial U}{\partial y} = \frac{\partial H_x}{\partial t}10^{-8}$$

Combining these, the well-known differential equation of current distribution is obtained

$$\frac{\partial^2 U}{\partial x^2} + \frac{\partial^2 U}{\partial y^2} = \frac{4\pi j\omega U 10^{-9}}{\rho} = \beta^2 U \qquad (4)$$

Solutions of (4) obtained by the method referred to hereinbefore[11] may be expressed in the form

$\cosh \epsilon x \cosh \alpha y$
$\cosh \mu x \cos ny$
$\cosh \Psi y \cos mx$

where

$\epsilon^2 + \alpha^2 = \beta^2$
$\mu^2 - n^2 = \beta^2$
$\Psi^2 - m^2 = \beta^2$

If the sum

$$U_{x.y} = C\cosh\epsilon x\cosh\alpha y + \Sigma A_n\cosh\mu_n x\cos ny + \Sigma B_m\cosh\Psi_m y\cos mx$$

is assumed as a solution, and

$$C = \frac{U_{a.b}}{\cosh\epsilon a\cosh ab}$$

$$n = \frac{2r+1}{2b}\pi$$

$$r, s = 0, 1, 2, 3, \ldots$$

$$m = \frac{2s+1}{2a}\pi$$

the coefficients A_n and B_m may be determined by application of the usual rules for Fourier coefficients.[12]

$$A_n = \frac{1}{b\cosh\mu_n a}\int_{-b}^{+b} \times \left[U_{a.y} - U_{a.b}\frac{\cosh\alpha y}{\cosh\alpha b}\right]\cos ny\,dy$$

$$B_m = \frac{1}{a\cosh\Psi_m b}\int_{-a}^{+a} \times \left[U_{x.b} - U_{a.b}\frac{\cosh\epsilon x}{\cosh\epsilon a}\right]\cos mx\,dx$$

The series thus obtained reduces to the boundary values at the surface, and is convergent in the interval (±a, ±b).

Appendix II. Resistance at 60 Cycles

1. A-c resistance may be calculated from the ratio of power loss to current squared:

$$R_{a-c} = \frac{\int_v U_{x,y}^2\, dx\, dy}{[\int U_{x,y} dx\, dy]^2} \text{ ohms per unit length}$$

2. A simpler and more direct calculation gives the same result. Since all flow is axial, the potential difference between points in any two planes perpendicular to the axis of the conductor is the same.

At the corner of the bar, the magnetic flux linking this filament is wholly external to the conductor. Thus the drop produced by the current and resistance in this filament is the potential difference between the two planes, if only the flux inside the conductor be considered.

The term internal impedance may be defined as that due to the resistance of the isolated conductor, and the inductance due to the flux linkages inside the conductor.

$$Z = \frac{\rho U_{a.b}}{\int U_{x,y} dx\, dy} \text{ ohms per unit length}$$

The real part of this expression is the a-c resistance per unit length.

Appendix III. Method of Measurement

The current density at any point of the conductor may be measured by means of an insulated wire laid along the bar, parallel to the axis and making electrical contact at each end only (figure 1). This wire is broken at the middle and the two ends brought out. The potential difference between these ends is equal to the resistance drop in the element of the bar immediately adjacent to the wire, since the induced voltage in the search wire and bar element cancel.

The measuring circuit is an a-c potentiometer. When the vibration galvanometer, *VG*, indicates a condition of balance, the voltage of the search wire is equal to the voltage produced by the current from the current transformer, *CT*, through the resistance, *R*, and the mutual inductance, *M*. Thus

$$V_{\substack{\text{search}\\ \text{wire}}} = I_{\text{amm.}}(R + j\omega M) = (ir)_{\substack{\text{bar}\\ \text{element}}} = U\rho l$$

or

$$\text{current density in bar element} = I_{\text{amm.}}\left(\frac{R + j\omega M}{\rho l}\right)$$

The magnitude and phase of the current density is thus determined.

In order to have a more useful reference quantity, the particular value of current used for test was eliminated by introducing the term average density. This is defined as:

$$\text{Average current density} = \frac{\text{total bar current}}{\text{area of bar}} = \frac{I_{\text{amm.}} \times \text{current-transformer ratio}}{\text{area of bar}}$$

The current density in the bar was calculated in terms of this quantity. Thus, the surface measurements shown on figures 3 and 4 were obtained from:

$$U = \frac{\text{actual current density}}{\text{average current density}} = \frac{(R + j\omega M)\text{ (area of bar)}}{(\rho l)\text{ (current-transformer ratio)}}$$

In order to avoid errors due to stray fluxes, the measuring apparatus was located about 50 feet from the bars being tested. All leads were carefully twisted. The absence of extraneous voltages was easily tested by short-circuiting the "search wires" (figure 1) at the bus bars and using the measuring circuit in the usual manner.

This method of measurement is applicable to all current-density measurements where the variation is in one plane only, and remains constant perpendicular to this plane.[7,7a]

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Discussion

Ernst Weber (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): The method of combining the analytical solution with an experimental determination of the boundary conditions is most interesting in itself but has the disadvantage of any experimental method, namely to be applicable only to the special case under investigation. A general analytical solution would be of advantage, but could be secured only with great difficulties. Thus one could transform the rectangular boundary into a circular one by conformal mapping of the geometry. The differential equation (4) in the author's paper would, of course, then become much more complex but the boundary conditions, which now represent the major obstacle to a complete solution, could easily be satisfied.

There is, however, an approximate method available. Though the author does not give the expression for the magnetic field, one could easily obtain it from Maxwell's field equations. Constructing the magnetic field lines, one would find nearly elliptical curves, which partly penetrate into the conductor and partly close through the surrounding air. By a slight deformation of these magnetic field lines, namely making one of them exactly coincide with the rectangular boundary of the conductor, one would have the boundary conditions $H_x = 0$ at $x = \pm a$ and $H_y = 0$ at $y = \pm b$, which can be satisfied in a manner very much similar to the author's method using Fourier series expressions. Thus one would get

$$A_n = \frac{1}{\mu_n \sinh \mu_n a} \times \int_{-b}^{+b} \Big[\sum_m mB_m \sin ma \cdot \cosh \psi_m y - C\epsilon \sinh \epsilon a \cdot \cosh \alpha y \Big] \cos ny \cdot dy$$

and

$$B_m = \frac{1}{\psi_m \sinh \psi_m b} \times \int_{-a}^{+a} \Big[\sum_n nA_n \sin nb \cdot \cosh \mu_n \Psi - C \cdot \alpha \sinh \alpha b \cosh \epsilon x \Big] \cos mx \cdot \alpha x$$

corresponding closely to the author's expressions and one would have to add the additional condition

$$\int\int U\, \alpha x\, dy = I$$

which will determine the constant *C*. This permits direct evaluation of the current distribution and should not deviate very much from the measured one. It would be interesting to compare the results in both cases and possibly use this approximation for precalculation of field and current distributions as well as for a determination of the resistance increase at alternating applied voltages.

In the method of the author it is, of course, not necessary to verify the solution by computing the total current numerically. This can serve only to demonstrate the relative importance of the various terms in the infinite series in the particular case chosen.

John L. Daley: Doctor Weber's comment on the limitations of any experimental method is appropriate. However, the method described in the paper is not limited to the particular application used therein for illustration. The method is quite general, but in each case an experimental de-

The Submarine-Cable Depthometer

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THE PROBLEM of plowing submarine cables[1] into the ground or sand making up the bottom of the ocean requires a means of checking the depth of burial of the cable after the plowing operation has been completed. Measuring the actual depth in inches below the surface of the ground is the purpose of a magnetic detecting and measuring device known as the cable depthometer. This magnetic detector is contained in a sled as shown in figure 1, designed for towing by the cable ship along the ocean bottom. The detecting operation utilizes the fact that ocean cables have a protective sheath of iron or steel wires around the copper core and the insulation. The magnetic field arising from the magnetization of the cable sheath, together with the distortion created in the earth's magnetic field by the cable sheath, generates an electromotive force in detecting coils in the sled, when these coils are moved in a substantially horizontal plane through such a field.

As the electromotive force generated in a single detecting coil or set of coils is a function not only of the speed at which the coils pass over the cable, but also of the amount and distribution of iron or steel in the cable sheath, of the degree of magnetization, of the strength and direction of the earth's field, of the gain or amplification of the amplifying and recording apparatus used to measure the signal on board the cable ship, and of other factors, determination of the distance between the sled and the cable, based upon the signal strength only, requires the evaluation of too many factors. To make compensation unnecessary for speed and other factors, a second set of detecting coils is mounted in the sled six feet behind the first set, as shown in figure 2.

Figure 1. Cable depthometer sled

The first coil set is mounted in a protecting pot at the forward end of the sled, and the second coil set in a similar pot at the after end. The second set of coils, known as the "after" set, is so constructed that its voltage-response versus distance-to-cable characteristic differs considerably from that of the first or "forward" set of coils. As the sled is towed over the cable, signals are delivered in sequence from first the forward and then the after set of coils. Measurement of the distance to the cable is accomplished by observing the ratio of the signal from the forward set to that from the after set, this ratio being a function of the distance only between the sled and the buried cable, increasing from 1.25 at zero inches of depth to 4.4 at 15 inches. Doubling the speed of the sled and the attached detecting coil sets will double the signal from both sets of coils, without changing the ratio.

Figure 2. Sled framework showing forward and after coil pots

This sequence of signals is transmitted to the amplifying and recording apparatus on board the cable ship through a pair of wires in the center of the sled towing line. As the frequency of such signals is in the neighborhood of one cycle per second, and as the maximum signal amplitude obtainable from the type of cable used in the plowing is approximately 0.3×10^{-6} amperes at 15×10^{-6} volts, a d-c amplifier of special design is used. The input circuit consists of cam-operated breaker points which provide sharp interruptions of the input current through the low-voltage winding of a transformer, inducing relatively high-voltage peaks in the high-voltage winding. By this method, the approximately one-cycle-per-second signal generated in the detecting coils becomes the modulation on a 240-cycles-per-second signal, for amplification with a conventional transformer-coupled amplifier tuned to 240 cycles per second. The output of the amplifier is rectified and operates a d-c recorder, making an inked record of the signals. It is by inspection of this inked record that the ratio of the forward to the after signal can be computed and the depth of the buried cable at the point of crossing quickly determined.

As the generation of signals in the detecting coils depends upon motion of the coil sets through the magnetic field sur-

Paper number 39-55, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 5, 1938; made available for preprinting December 22, 1938.

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[1] For all numbered references, see list at end of paper.

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termination of the boundary conditions is necessary.

The approximate method suggested has its advantages. In fact, with appropriate adjustments, the method used by Bewley and Poritsky ("Intersheet Eddy Current Loss in Laminated Cores," ELECTRICAL ENGINEERING, March 1937, pages 344–5), on a related problem, would give a similar treatment.

The accuracy of any approximate method could be established by comparison of a particular case with the exact method presented in my paper.

The use of conformal mapping presents an interesting possibility.

The second verification of the solution was given in the paper to demonstrate the convergence of the series. In the case of least rapid convergence, that of the square cross section, reasonable accuracy is obtained with only a few terms.

rounding the cable, the maximum signal results when the sled follows a course perpendicular to the line of the cable. The procedure followed in the operation of the sled is to tow it back and forth across the buried cable, charted as shown in figure 3, obtaining a number of crossings in each mile. While such a series of readings is far from being a continuous record, nevertheless it affords an opportunity for judging the performance of the cable plow.

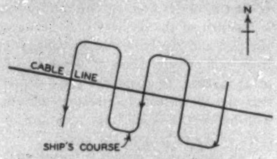

Figure 3. Towing course of ship

Amplifying the statement in the opening paragraph, the signal indicating the presence of a magnetic object such as an ocean cable, is the result of the progression of detecting coils from the uniform magnetic field of the earth into the nonuniform magnetic field surrounding the magnetic object, and thence back into the uniform field of the earth, the signal intensity being proportional to the rate at which the flux changes through the detecting coils. In the case of a long object such as an ocean cable, the field is the result of three factors. First, magnetic poles are formed longitudinally along the cable sheath, with lines of force substantially parallel to the cable between poles, much on the order of a long bar magnet. This type of magnetization produces very little flux linkage with the detecting coils. Second, magnetic poles are formed in planes perpendicular to the length of the cable; thus one side of the cable sheath becomes a north pole and the opposite side a south pole. This type of magnetization, which may be called "transverse magnetization," results in considerable flux linkage with a detecting coil, as shown in figure 4. Third, the high magnetic permeability of the cable sheath causes a distortion in the earth's magnetic field. Motion of the detecting coil or coils into this region induces a voltage in one direction in the circuit as the flux linkage is increased and voltage in the opposite direction as the flux linkage decreases.

Since some types of motion of a single detecting coil in the earth's field generate very large interfering voltages, it is necessary to add a second coil, having the same product of turns times average area as the first coil, and parallel to it. The two coils are then connected in series differentially, that is, poled oppositely, with the result that voltages in the coils induced by rotary motion in the earth's field are 180 degrees out of phase in the circuit and cancel. Two coils, one above the other, rigidly fastened together and differentially connected, are shown in figure 5 as they progress in the horizontal arrow direction over the cable shown in cross-section, with the iron sheathing wires *a*, the gutta percha insulation *b*, and the copper core *c*. For the sake of simplicity, only the lines of force due to the transverse magnetization of the cable are shown with the polarization assumed horizontal, omitting the lines of force of the earth's field. Since the detecting coils are differentially connected, some degree of cancellation of the signal from the cable is experienced. However, since the upper coil is at a greater distance from the cable than is the lower coil, a comparatively low voltage is generated and cancellation of the voltage generated in the lower coil is small. The greater the distance between the coils, the less the cancellation. However, the physical considerations of keeping the coils parallel and preventing relative motion between them, make the smaller values of separation desirable.

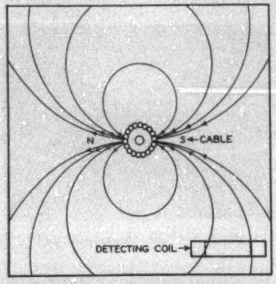

Figure 4. Transverse magnetization of cable showing flux linkage with one coil

If the mere detection of the presence of a magnetic object such as an ocean cable is all that is required, the use of one pair of detecting coils is sufficient, as the exact voltage magnitude will then be of little importance. In the determination of the depth to which the cable is buried, the use of the second set of detecting coils makes proper compensation for speed and other factors. The difference in characteristics between the second or after coil set and the first or forward set is illustrated in figure 6. The ordinates are percentages of the signal at zero inches depth, and the abscissas distances between the bottom of the coil pots and the cable, both being logarithmic scales. It is to be noted that the ratio of the forward signal, designated by curve *F*, to the after signal, curve *A*, is unity at zero inches, while at 15 inches the *F* ordinate is 3.5 times that of the *A* ordinate. In actual practice, however, the coil sets are so constructed that the ratio of *F* to *A* at zero inches is 1.25, increasing to 4.4 at 15 inches. The relationship between the ratio of the two signals and the distance between the bottom of the sled and the cable is shown in figure 7.

Actual records are shown in figure 8, the right-hand record being that of a one-inch burial of the cable and the left hand record that of an eight-inch burial. In general, there are two distinct voltage peaks in the signal from each coil set, one peak when the flux is increasing most rapidly through the coils and one when the flux is decreasing most rapidly. These are observable in the left-hand record of figure 8 and are designated *a* and *b* for the forward coil set which passes the cable first, followed by the two peaks *c* and *d* for the after coil set which is spaced six feet behind the forward set. Whether the two peaks of each signal are equal in magnitude or not depends upon the position of the composite magnetic vector representing the field around the cable sheath plus the distortional effect of the iron in the sheath on the earth's field. If this vector is perpendicular to the line of motion of the detecting coils in the sled, the two peaks will be equal in magnitude. Calculation of the ratio is based on the average.

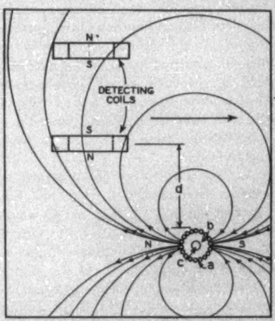

Figure 5. Transverse magnetization of cable showing flux linkage with balanced coils

There are numerous practical problems to be solved in developing a mobile magnetic detector operating on the nonuniform magnetic field around iron or steel objects. The choice of design is restricted by the necessity for dragging such a detector along a rough ocean bottom, under water pressures up to 1,200 pounds per square inch and at distances behind the ship up to 6,000 feet.

The most serious problem is that of elimination of potentials due to some types of motion of the detecting coils through the earth's field. Coil sets must be constructed so that the magnetic vector of the lower coil is equal in magnitude and opposite in direction to the magnetic vector of the upper coil. In other words, with n coils rigidly fastened together in any one system, the vector sum of all their magnetic vectors must be zero. In a two coil system, the number of turns times the average area of one coil must equal that of the other coil, the two must be differentially connected in the circuit, and must be parallel. In detecting magnetic objects the size of an ocean cable, it was experimentally determined that the cancellation of the earth's field or what was termed a "balance" must be correct within one part in 12,000 if the interference were to be held below two per cent of full scale. As the product of the number of turns and the average area of each of the coils used is 25,380 turn inches squared, the coils must be within approximately 2 turn inches squared of each other. The winding of the coils must also take into consideration the high pressures to be encountered and the prevention of coil deformation which would change the average area of the turns.

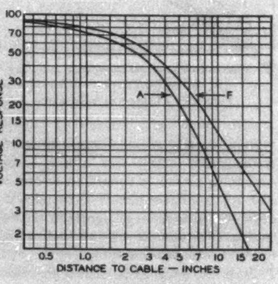

Figure 6. Voltage response-distance characteristics

The other consideration is the matter of parallelism. Two coils such as shown in figure 9 are assumed to be rigidly fastened together. They are also assumed to be out of parallel or balance by the fixed angle a. The entire system is first considered stationary in the earth's field H, with the lower coil at an angle of wt with the field and the upper coil at an angle of $wt - a$ with the field.

The following definitions are used:

W = rotative speed of the coil system in the earth's field
N_1 = number of turns in the lower coil
N_2 = number of turns in the upper coil
A_1 = average area of the lower coil
A_2 = average area of the upper coil
H = magnetic intensity of the earth's field
B = $\mu H = H\mu$ is unity as the balancing is conducted in a location free from magnetic objects
ϕ_1 = flux linkage of the earth's field with the lower coil
ϕ_2 = flux linkage of the earth's field with the upper coil

The flux linkages of the two coils can then be written:

$$\phi_1 = BN_1A_1 \sin (wt)$$

and

$$\phi_2 = BN_2A_2 \sin (wt - a)$$

As the instantaneous values of the potentials generated in the coils can be written as

$$e_1 = \frac{-d\phi_1}{dt}$$

and

$$e_2 = \frac{-d\phi_2}{dt}$$

$$e_1 = -wHN_1A_1 \cos (wt)$$

and

$$e_2 = -wHN_2A_2 \cos (wt - a)$$

From inspection it is apparent that the constant terms must be equal and that the periodic terms must also be equal, that is;

$$N_1A_1 = N_2A_2 \text{ and } a = 0$$

Assuming that the angle a is finite and that the condition of $N_1A_1 = N_2A_2$ has been satisfied, the net voltage developed by rotation in the earth's field will be as follows:

$$e_1 - e_2 = -wHN_1A_1[(\cos wt) - (\cos wt - a)]$$

As long as this angle a is finite, the interference developed will be proportional to the rotative speed w of the system. As vibration or shock will develop comparatively high values of w, the experimentally determined figure of one part in 12,000 to which the balance must be held is understandable. The maximum value the angle a may have, is the angle whose sine is 8×10^{-5}, 0.0047 of one degree of arc.

Earlier in this paper the so-called "ratio" method was described, having as its basis two different coil sets which had unlike voltage-response versus distance characteristics as shown in figure 6. This difference in characteristics may be accomplished in a number of ways; for instance, the first or forward set of coils can be constructed in the flat "pancake" manner, while the second or after set of coils can be constructed with the turns concentrated at the outer diameter of the coil; in other words, the geometries of the two coils would differ. The most satisfactory method tried, however, involved coils all of similar geometries, and depends upon their disposition for the desired effect. Figure 10 illustrates the coil arrangement of the depthometer sled, in which the two coil sets are six feet apart. At any given speed, the peak signal generated in a single detecting coil will vary as one over the square of the distance between the coil and the cable, beyond a certain minimum distance. The detecting coil does not begin to operate as a point of no dimensions, and the signal generated in the detecting coil does not obey the inverse square law, until this minimum distance of approximately $1^1/_2$ diameters of the coil has been reached. To simplify calculations in the following analysis, however, it will be assumed that the signal generated varies as one over the distance squared for the entire range. The following definitions are given:

s = speed of the sled in the direction perpendicular to the line of the cable
d = distance between the bottom of the sled and the cable in inches
k = a constant involving the permeability of the cable sheath, its cross section, the vector magnetic intensity of the earth's field, etc.
N_1A_1 = $N_2A_2 = N_3A_3 = N_5A_5 = NA$
N_4A_4 = $2NA$
e_1 = peak signal generated in no. 1 coil, e_2 in no. 2, etc.

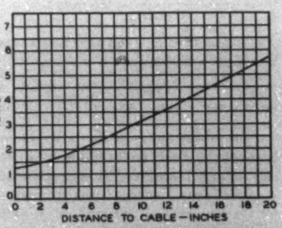

Figure 7. Ratio-distance characteristic of dephthometer

e_f = peak signal generated in the forward coil set

e_a = peak signal generated in the after coil set

The signal generated by the forward set of coils will approximate the difference between the individual coil signals, thus:

$$e_f = e_1 - e_2$$

The signal generated by the after set of coils will approximate the sum of the upper and lower coils number 3 and number 5 minus that of the center coil number 4. Note that the magnetic vectors of the upper and lower coils are in the same direction, while that of the center coil is in the opposite direction and of twice the magnitude. The vector sum of the three coils in the system is therefore zero, which fulfills the requirement that no signal be generated by rotary motion of the system in the earth's field. As the peak signal of any coil is directly proportional to the speed s, to the number of turns N, to the average area A, and to the constant k, and inversely proportional to the distance between the coil and the cable squared, the following expressions can be written:

$$e_f = e_1 - e_2 = \frac{ksNA}{(d+5)^2} - \frac{ksNA}{(d+13)^2}$$

$$e_a = e_3 - e_4 + e_5 = \frac{ksNa}{(d+5)^2} - \frac{2ksNA}{(d+9)^2} + \frac{ksNA}{(d+13)^2}$$

The ratio of the forward coil set signal to that of the after signal cancels the factors of k, s, and NA and involves only the distance d between the bottom of the sled and the cable, thus:

$$\frac{e_f}{e_a} = \frac{\dfrac{1}{(d+5)^2} - \dfrac{1}{(d+13)^2}}{\dfrac{1}{(d+5)^2} - \dfrac{2}{(d+9)^2} + \dfrac{1}{(d+13)^2}}$$

This expression can be reduced to the following:

$$\frac{e_f}{e_a} = \frac{(d+9)^3}{6(d+9)^2 - 32} = \text{the ratio}$$

As the lowest value d can have is zero, the minimum ratio is:

$$\frac{9^3}{6(9)^2 - 32} = \frac{729}{486 - 32} = 1.6057$$

As the (-32) figure in the denominator is small compared to the $6(d+9)^2$ quantity, it may be omitted without introducing much error, in which case the equation for the ratio reduces to simply $(d+9)/6$, the equation for a straight line having a slope of one in six and an intersection on the Y axis of 1.5. The following tabulation compares the calculated values with the actual experimentally determined values, also showing the calculated forward coil set signal e_f and the after signal e_a.

d in Inches	e_f	e_a	Ratio, Calculated Value	Ratio, Actual
0	0.03410	0.02130	1.61	1.25
5	0.00691	0.00288	2.40	2.00
10	0.00255	0.00080	3.21	3.18
15	0.00122	0.00030	4.04	4.43
20	0.00068	0.00014	4.86	5.70

The above treatment makes a number of assumptions which introduce error; however, the experimental and calculated values follow each other reasonably well.

Since the coil arrangement discussed here behaves symmetrically it is immaterial whether the sled is towed upon its top or bottom surface. The front-quarter view of the depthometer sled shown in figure 1 shows the stepped arrangement on the sides of the sled, designed to prevent riding on other than the top or bottom surfaces.

It is necessary to construct the sled and all fittings of a nonmagnetic material. Everdur, a manganese-silicon-copper alloy, is used. The completed sled is approximately ten feet in length, 21 inches high, weighs 2,600 pounds in air, and 1,800 pounds in salt water. The coil pots shown in place in figure 2 are filled with castor oil to provide a means whereby the extreme pressures at the ocean bottom can be taken care of through the relative incompressibility of the oil, thus relieving the pots and the conductor glands of the necessity of withstanding pressures up to 1,200 pounds per square inch. However, the castor oil has a cubical temperature coefficient of expansion of approximately ten times that of the Everdur metal making up the pot, and a temperature rise of 30 degrees Fahrenheit would cause the oil in one pot to attempt an expansion of about seven cubic inches, which is resisted, would build up an internal pressure of about 2,500 pounds per square inch. Therefore it is necessary to provide means whereby the oil can expand and contract with temperature changes. A Sylphon bellows is installed in the sled for this purpose, accommodating large volume changes in the oil with pressure differentials of a few tenths of a pound per square inch.

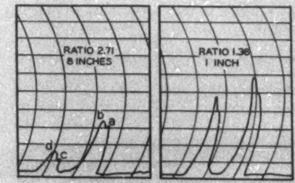

Figure 8. Recorded crossings of cable

Left—Eight inches; right—one inch

The towing cable having a breaking strength of 14 long tons (2,240 pounds to the ton), is especially designed for the purpose of towing the sled and carrying the detecting coil connections to the cable ship. It has as its main feature torsional balance, that is, the towing cable will not twist appreciably when load is applied. To accomplish this, there are two layers of steel wires 0.05 inch in diameter, the inner layer having 44 wires laid up right hand, with the second or outer layer of 50 wires laid up left hand. The messenger pair consists of two conductors twisted together so as to form a complete transposition every two inches. This steel towing cable is spliced on to bronze wire ropes 40 feet ahead of the sled to avoid the generation of signals in the detecting coils due to relative motion between the steel towing cable and the sled. There is also a second possibility for interference in the steel towing cable. Magnetic flux from poles formed on the steel wires will thread through the space between the two electrical conductors, and a change in this flux will generate a potential in the circuit. This type of interference occurs only when the tension in the steel towing cable is suddenly increased or decreased, and is of no importance during normal towing operations.

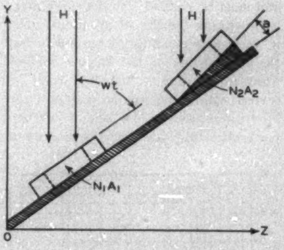

Figure 9. Theory of balancing

The apparatus must be prepared to make a chart record of signals varying widely in magnitude. As the peak signal generated in the detecting coils varies roughly as one over the distance squared, an ocean cable which will operate the re-

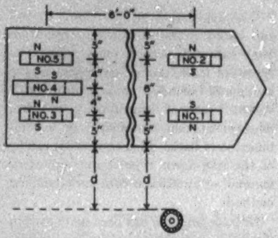

Figure 10. Depthometer coil arrangement

cording apparatus full scale at zero inches from the sled bottom (5 inches from the lower coil inside the sled), will give only seven per cent at 15 inches from the sled bottom. The accurate recording of such a wide range is impracticable. To provide a means whereby the smallest signals can be accurately read, and still prevent the maximum signal from exceeding 100 per cent or going off scale, recourse is had to a so-called "exponential amplifier," in which the output voltage of the amplifier varies as some root of the input voltage. Stated algebraically:

$$e_o = k \sqrt[n]{e_i}$$

where e_o is the amplifier output voltage, e_i the input voltage, and n positive and greater than unity. The general procedure is to rectify the signal after four stages of amplification, filter, and apply to various combinations of the control and suppressor grids of the first two stages of the amplifier. Application of this controlling direct voltage must be rapid enough to control the signal which brought it about; at the same time, too rapid a control will cause low-frequency oscillation.

The extremely low level of signal available at the input of the amplifier requires greater than usual precautions. The maximum signal obtainable from the type of cable used in the plowing is 0.3×10^{-6} amperes through the amplifier input resistance of 50 ohms, or 15×10^{-6} volts. The interference level from bumping of the sled along the bottom, tube noises in the amplifier, interrupter contact noise, etc., is around 6×10^{-9} amperes or 0.3×10^{-6} volts. The contacts of the interrupter are brought together and separated by a cam mechanism, making higher contact bearing pressures possible, which assists in reducing noise. The input transformer which has its low-voltage winding in series with the detecting coils and the interrupter, requires a triple iron magnetic shield to guard against stray changing magnetic fields and also the effect of the ship's rolling and pitching in the earth's magnetic field. A change in the amount of flux threading through the primary winding of the transformer is as productive of potentials as if the change took place through the detecting coils.

The expression for the maximum voltage developed in a single detecting coil of N turns and an average area of A centimeters squared, rotating at the rate of f cycles per second through the earth's field H is as follows:

$$E_{\max} = 2\pi f N A H \times 10^{-8}$$

where

NA = 25,380 turn inches squared = 163,700 turn centimeters squared

H = approximately 0.5 maxwell per square centimeter, the total intensity of the earth's field

f = approximately 0.5 cycles per second, the rate at which the coils were turned in the process of balancing

then

$$E_{\max} = 2\pi(^1/_2)\ 163{,}700 \times 0.5 \times 10^{-8} = 0.0026 \text{ volts}$$

This is the value then of the voltage developed by a single detecting coil. In order to reduce this voltage from 0.0026 to the desired interference level of 0.3×10^{-6}, the second differentially connected balancing coil must develop an $E_{\max}$ within 0.3×10^{-6} divided by 0.0026 or one part in 8,667 of the $E_{\max}$ of the first coil. This calculated figure of roughly one part in 9,000 compares favorably with the experimentally determined value of one part in 12,000. The net maximum rate of change of magnetic flux density through the two differentially connected detecting coils to produce a signal of 0.3 microvolts, the interference level, is 1.8×10^{-4} maxwells per square centimeter per second.

Operation of the depthometer on ocean cables of various histories has brought out the important fact that the plowing operation causes a considerable drop in the magnetization of the cable, which however, is recovered to a great extent in the first year. The loss is probably due to the bending and "working" of the cable in passing over various sheaves on both the cable ship and the cable plow, and ranges in value from one-half to two-thirds of what may be called the ultimate magnetization of the cable sheath. In other words, recently plowed cables exhibit magnetizations of from one-third to one-half of that found in cables laid a number of years previously.

Although the depthometer was intended originally for use in the special application of measuring the depth to which ocean cables had been buried, it has been found very useful in locating ocean cables[2] and in obviating the danger of "hooking" the wrong cable when cloudy skies or dim horizons prevent the taking of accurate navigational sights, or when an error made at the time that the cable was laid appears on the chart in a manner additive to that of the observational sight taken at the time of subsequent repairs. By the use of the detecting feature of the depthometer, the relative positions of two or more cables in a particular locality may be determined, after which grappling operations may be conducted with comparative safety.

The experience of two years' work with this measuring and detecting device has demonstrated its usefulness in both the special application of measuring the depth of burial of ocean cables and also the routine application of locating cables prior to repair operations. The practicability of constructing and balancing the detecting coils so that they may be dragged along ocean bottoms has been demonstrated, together with the ability of the d-c amplifier and interrupter to operate in the field at electrical levels usually thought of as strictly for the laboratory.

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2. PROTECTION OF SUBMARINE CABLES BY PLACEMENT "UNDERGROUND," ELECTRICAL ENGINEERING, volume 57, May 1938, page 204.

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THE ESSENTIAL operating facilities placed at the disposal of toll operators for the switching and completion of long-distance calls have, in a general sense, remained substantially unchanged since the early days of toll switching. It is still common practice, in toll offices, to have toll lines and local subscribers' trunks terminate in multiple jacks and lamp signals with associated cords and plugs to permit the operator to answer and extend calls between local stations and stations at distant toll centers. In addition, toll operators are still dependent on the assistance of inward and through operators for connecting within and through distant toll centers.

As toll systems become more complex due to the extension of the system and development of new services, with the complexity of operating routines to meet the ever increasing demand for service and speed, there is a general tendency toward the adoption of new switching methods, such as combined line and recording, even though these methods require the toll operator to give a greater amount of undivided attention to each call during the build-up period. Experience has demonstrated that with this service, a greater number of operators are required, especially during peak hour periods, to accept and extend toll calls without undue delay.

Another factor which has become important in recent years is the widespread use of toll dialing for the extending and completion of outward toll calls, even in exchanges using cord-type boards. This method of operation, requiring that opperators use different procedures on the various types of calls, make it more difficult to train operators, and inevitably increases the likelihood of error.

Development of the remote-control toll board has made it possible to retain the advantages of these new methods of toll operation, and at the same time achieve a real saving in time and labor. Placing before the toll operator more efficient switching facilities enables her to receive and clear toll calls more rapidly, thereby vitally affecting both operating and plant costs. In many cases, the resulting increased line efficiency will avoid the need for new circuits which would otherwise be required to reduce subscriber and operator waiting time until the circuits become available.

The remote-control toll board, together with its associated electromechanical switches, is complete in itself for the switching of toll within its own office, as normally completed by means of toll boards of the cord type. In addition, it incorporates features to facilitate the completion of calls by the originating toll operator to stations within and through distant toll offices. Associated with each toll board are groups of finders which automatically extend incoming calls to idle operators. Simplified links, key senders of the quick-action pushbutton type, supervisory lamps and keys, together with primary and secondary outgoing selectors, give toll operators access via key-sender method, to both toll and local stations.

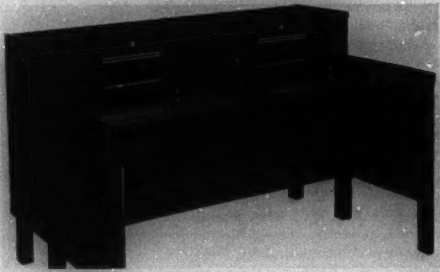

Figure 1. Remote-control toll switchboard section

While outgoing selectors are provided for the remote-control toll board, their use is not confined to this board alone. The switches are grouped, to form what could be considered a toll automatic exchange with trunk channels connecting to the associated local central-office equipment. Distant toll boards, either of the remote-control type or of the cord type, when equipped with automatic dial trunks and repeaters may, if desired, make use of toll automatic exchange switches.

The application of the remote-control toll board is not confined to operation with Strowger central offices, since by means of trunk groups and repeaters, the automatic switches will operate with varied types of central-office systems, whether manual, automatic, or a combination of both. Line equipment can be of the ring-down type, common-battery manual, or automatic with varied dialing methods.

Notable progress has been made within the last two decades in converting metropolitan, suburban, and rural exchange areas to automatic operation. Paralleling this development, there has been a strong tendency to provide dialing methods to handle the complex suburban toll traffic within large metropolitan toll areas. Small urban areas and rural communities have likewise grown into comprehensive short-haul toll networks utilizing various forms of dial tandem operation. In such networks, provision for direct calling between subscribers is utilized to quite an extent when such calls can be set on a predetermined meter rate basis. Likewise, various forms of automatic timing are being increasingly employed to permit direct dialing between local and distant automatic exchanges thereby eliminating the need of going through a toll switchboard with operator supervision. Calls which cannot well be placed on a predetermined rate or automatic timing basis must of necessity be handled by toll operators and it is for such traffic that the remote-control toll board has been designed.

A number of automatic networks involving long-haul toll lines have been placed in service with very satisfying results. With the assistance of improved toll switching equipment, more efficient means are provided for the speedier handling of calls over automatic switching networks. Heretofore, automatic network dialing has been retarded due to the

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Paper number 39-56, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 21, 1938; made available for preprinting December 30, 1938.

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use of dialing trunks added to existing manual boards as an applique unit and using ordinary dials, in lieu of modern trunk selection or specially designed boards equipped with sending equipment capable of sending both long and short call numbers without delaying the short call numbers.

Principle of Operation

Briefly, the remote-control toll board provides for connections from local subscribers to toll, toll to local subscribers, and toll to toll by means of electromechanical switches under the control of toll operators. Incoming calls, either from trunk or toll lines, terminate on the banks of a finder switch. A signal received over any line or trunk automatically causes a finder to operate and connect to the calling line. This call is immediately connected to an idle operator's headset, at which time the operator receives a double impulse of high-frequency tone, commonly called "zip-zip" tone. Upon being informed of the desired toll station, the operator sets up the toll group code by depressing digit keys in her key-sender set. The operation of digit keys causes a preselector, which is tied direct to the finder, to select a nonbusy line within the group dialed. The operator, by means of a link circuit (which corresponds to a cord circuit in the ordinary type of toll board) supervises connections and does the necessary ticket timing.

Physical Equipment

The physical equipment consists of operators' switchboard equipment shown in figure 1. The key shelves of this switchboard are equipped with a variable number of links and associated keys and lamps in quantities dependent on position loads. The detail of a key shelf together with its associated turret is pictured in figure 2. It will be seen from this figure that each link consists of a talk and monitor key, a front supervisory lamp, a rear supervisory lamp, and a link busy lamp. To the left of these keys are located certain common keys and lamps. These, together with other keys and lamps to the right of link circuits, are termed "common equipment." This common equipment is connected to each link talk key and automatically becomes a part of any link on which the talk key is operated. Operating practices provide for only one talk key being operated at one time. Equipment located in the turret of each operator's position consists of group identity and group busy lamps in addition to space for toll tickets. No equipment other than that indicated on this position key shelf and turret layout is mounted in the operator's position switchboard. All coils, capacitors, relays, and switches are located in a separate automatic equipment room to permit ease of maintenance and systematic routining without disturbing operators. Associated with each link is a line finder switch (shown in figure 3). In common with other switches of this type, the finder consists of three banks of contacts at the bottom, a vertically located shaft with spring "wipers" at the lower end, and electromagnets near the middle which can lift and rotate the shaft and wipers to any desired set of bank contacts. At the top are relays which control the electromagnets and other circuits. This finder provides for lines and trunks to be connected to its banks. The function of the finder is to hunt a line or trunk on which a call has been received, causing the switch to step up and across the bank to seize the calling line or trunk, the contacts of which were "marked." This finder connects directly to link talk keys by means of switching relays and to an outgoing preselector. The preselector is a two-motion switch very similar to the finder. Its operation, however, is under the control of key-sender equipment and control keys 0 to 9 pictured to the right in figure 2. The wipers of this switch respond in a vertical direction to the bank level corresponding to the key depressed by the operator. The rotary action is automatic. It passes over busy lines or trunks but stops and switches through on the first nonbusy line or trunk. Subsequent keys depressed on the key sender will operate other ranks of selectors similar to the preselector in order to obtain subline and trunk groups. With the aid of similar

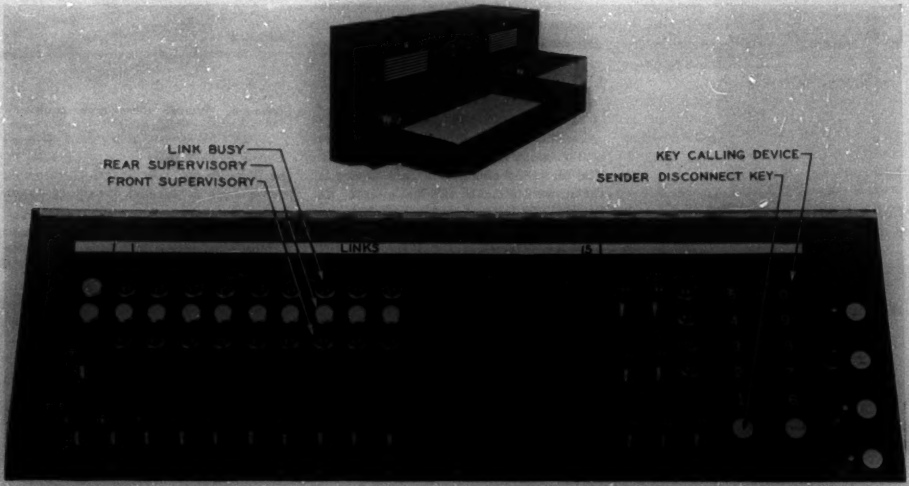


Figure 2. Typical key-shelf and turret arrangement

stepping switches in connecting toll centers, the local or answering toll operator is able to control the selection of lines, trunks, and subscriber stations without the assistance of an inward or through operator.

Relays associated with the positional keys, likewise with trunks and lines, are mounted in units and assembled on frameworks as shown in figure 4. Figure 5 shows four relay units with covers removed. These are completely wired self-contained units arranged for bolting to horizontal relay rack angles. Outgoing leads from each unit terminate on pin block terminals to facilitate production, installation, and maintenance. Key-sender equipment employs similar groups of relays for the storage and sending of digits registered through the operation of keys in the key sender. Rotary switches for the automatic association of an idle sender and storage relays with link keys are shown in figure 4 immediately above the relay groups.

Functions of the Toll Operator

The prime purpose of toll switching equipment is to provide a medium for toll operators to receive, extend, supervise, and time calls from local subscribers to subscribers served by distant toll centers. It is quite obvious that the type of equipment should be of such a design as will permit rapid and efficient service to the toll user with the minimum amount of operator effort in performing mere mechanical functions in setting up and supervising talking circuits. The method most commonly used in completing toll calls enables subscribers in local offices to reach the toll operator over a recording trunk from the local switchboard or in the case of automatic, by the subscriber dialing a long-distance code and terminating on a combined line and recording trunk within the toll board. Calls received over recording trunks are extended by the operator to toll lines connecting to distant toll points by means of cord circuits. These calls are extended with the aid of inward and through operators in other connecting toll centers and finally to the called subscriber while the originating or calling subscriber waits on the line.

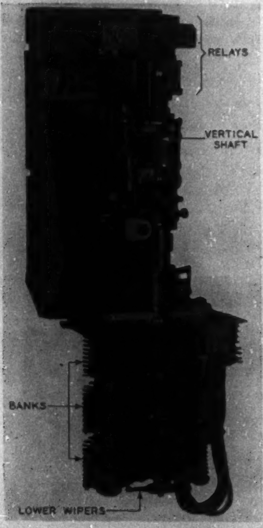


Figure 3. Finder switch

The procedure on the remote-control toll board enables subscriber to reach the toll board by means of a manual trunk in the case of manual exchanges or by dialing a long-distance code in the case of automatic offices. These calls instead of being answered by the use of plugs and cords, are automatically received on an idle operator's headset, at which time a "zip-zip" tone is received by the operator. Calls are extended by dialing toll group code numbers, causing the seizure of an idle toll line to the connecting toll center. With toll switching facilities in distant toll centers, the originating operator can readily complete calls by means of the regular key sender to subscribers in the distant exchanges without delay in waiting for assistance by inward and through operators.

Line and Trunk Finder and Link Circuit

Incoming lines and trunks terminate on the banks of finder switches, figure 3. The searching element of each finder is the answering end of the link circuit. It is connected by means of a switching relay to a talk and monitoring key and three supervisory lamps at one of the toll positions. One lamp serves as a link busy indication. The other two are answering and calling supervisory lamps as in standard cord board practice. A toll preselector is permanently associated with the calling end of each link circuit for extending calls into the trunking system. Each operator's position is equipped with several link circuits, the exact number depending upon the type of service to be handled. Present practice ordinarily provides for inward positions to be wired for 18 links, CLR (combined line recording) positions for 6 to 8 links, and delayed positions for 5 links.

Two general groups of finders are usually provided except for the smaller installations, one serving the inward positions and the other the CLR and delayed-call positions. Special groups of lines or trunks may be terminated at specialized positions by providing separate finder groups for the segregated lines or trunks or by arranging the finders associated with the links in the specialized positions to search over a selected group of finder bank contacts.

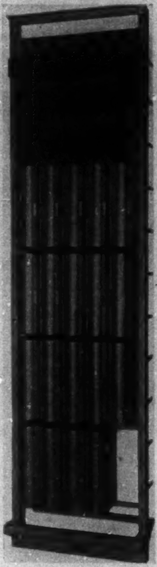


Figure 4. Typical relay rack equipment assembly

Operators indicate their preparedness to receive incoming calls by operating the talk key of a nonbusy link. Operation of a link talk key prepares the associated link circuit for receiving an incoming call and connects the link to the operator's position circuit. Incoming calls then are received automatically on the operator's headset. The presence of a call on the link is indicated by a double impulse of high-frequency tone ("zip-zip" tone) in the operator's receiver. The origin of incoming calls is indicated by the lighting of a line or trunk group identity lamp.

This identity lamp lights in the answering position only and remains lighted during the period the link talk key is operated.

Operation of the link monitoring key enables the operator to listen in on established connections without disturbing conversation. With two monitoring keys thrown, the operator can monitor on both connections. With one monitor and one talk key thrown, the operator may listen and talk on both connections, but can ring, dial, split, and release only on the link on which the talk key is operated. This feature permits the overlapping of certain operations on two connections in accordance with standard toll operating practices.

Supervisory relays are placed in the line and trunk circuits instead of the link circuits, permitting the same simplicity of link circuit design and equipment economies as are afforded by the latest type cord boards.

Position Circuit

The remote-control toll board uses a position circuit which includes common equipment such as transferring, splitting, ringing, dialing, and releasing keys and associated relays. Under normal talking conditions, the answering end of the link is connected to the calling end of the switching equipment, free from shunts or attachments, without looping through the toll switchboard. By operation of the talking key associated with the link circuit, the position circuit is connected between the answering and calling ends of the link and the operator's set is connected across the link.

With the talking key of any link circuit operated, the operator may:

(*a*). Dial on the answering or calling end of the link.

(*b*). Split the talking circuit between the answering and calling ends of the link.

(*c*). Release either or both ends of the link.

(*d*). Transfer "out of order" toll lines to the toll test board.

(*e*). Ring on the answering or calling end of the link.

On calls to local stations from inward positions, the ringing is automatic. On calls to local stations from CLR and delayed positions, the ringing is automatic only after the ringing key is operated to start the ringing. Thus, local lines may be seized and ringing delayed while the toll circuit is being built up.

A common splitting key is provided by means of which the operator may talk to the subscriber on either end of the link without the other subscriber overhearing. Release keys are provided by means of which the answering or calling ends of the link may be released separately or both ends may be released simultaneously. All position circuit keys function only when the talk key of a link circuit is operated.

Figure 5. Operators' position relays

Link and Sender Finder Equipment

Each operator's link and sender finder equipment consists of a relay group, which is part of the position circuit, and four 6-level 25-point rotary switches. Figure 6 shows a link and sender finder rotary switch group for four switchboard positions. Four rotary line switches are associated with each position. Two of the rotary switches provide two separate channels to the common group of senders and the other two extend the sender channels through to the particular links on which the operator throws her talk key. Two sender channels are provided so that the operator may overlap sending on two connections. Circuits are so arranged that the sender is locked to the link until all digits are sent out, even though the operator may restore the link talk key. Operation of a second link talk key will cause seizure of the second sender channel and permit the operator to set up another connection while the first sender is still functioning on the connection previously set up.

The rotary switch shown in figure 7 consists chiefly of two parts, a rotary mechanism and a bank assembly. The rotary mechanism is composed of a motor magnet with spring assembly, rotary pawl, ratchet wheel, and wiper assembly.

When the motor magnet is energized, its armature pulls up. Near the end of its stroke, the break contacts of its own interrupter springs open and breaks the circuit to the motor magnet. The motor magnet de-energizes and its armature falls back, stepping the wiper assembly around one contact. When the motor magnet restores, its interrupter springs again make contact, and if further rotation is required, the circuit to the motor magnet is again closed. The rotary pawl is attached to the armature. It is drawn away from the pawl stop when the armature pulls up, and under tension of a small spring attached to it, falls into the next tooth of the ratchet wheel. When the armature falls back, the rotary pawl is pushed in toward the armature stop by the force of the tension spring, and the ratchet wheel to which the wipers are attached is caused to rotate on its axis.

The ratchet and armature pawl arrangement prevents the ratchet wheel from making any movement except under control of the rotary armature.

The wipers are double-ended, making contact with either end, and are of special design to give flexibility and good contact.

The construction of the bank is such that one end or the other of the wipers will at all times be in engagement with a set of bank contacts, the second set of wipers coming into action when the first one leaves the last set of the bank contacts.

Commutator brushes project from the bank assembly and make continuous contact with the wipers.

Sender and link finder switches both use six-level banks and six pairs of wipers.

The operation of a link talk key marks one of the link finder bank contacts, and the wiper assembly will rotate until the wipers are opposite the marked bank con-

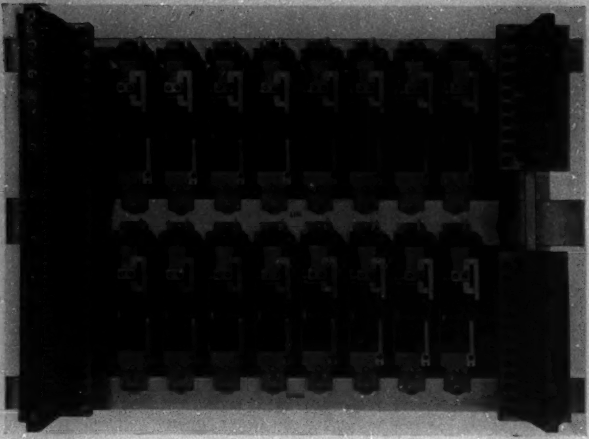


Figure 6. Link and sender finder switches

tact, thereby connecting the sender control relays with the particular link, the talk key of which has been thrown. Simultaneously the sender finder wipers rotate until they find a marked sender finder bank contact, thereby connecting an idle sender through the sender control equipment to the link circuit having the talk key thrown.

Common-Start Position-Distributing and Level-Distributing Equipment

Since separate line and trunk finder switch groups are furnished for each operator's position, common-start equipment is provided to limit the search for an incoming call to one finder switch only. Several operators may be waiting for calls at any given instant, therefore, position-distributing equipment is also provided to distribute incoming calls to the waiting operators in a definite sequence. A level-distributing circuit is also provided to prevent line and trunk finder switches from selecting waiting calls from the lower levels of their banks in preference to those on the upper levels. This circuit functions to distribute the incoming calls in sequence according to the bank levels on which they are received. The combined functioning of the start and distributing circuits provides effective teamwork by enforcing an even distribution of all incoming calls to those operators who are prepared to receive them, and by eliminating all wasted effort usually caused by several operators attempting to answer incoming signals that have several multiple appearances.

Digit Registers and Sender Equipment

A common group of register and sender units is provided. This group is accessible to all operators through the action of rotary finders, thus providing for their economical use since they are used for only a short period during each call. The registration equipment is reflexive in that an unlimited number of digits may be registered with a comparatively few storage groups. This is accomplished by using the digit registers over and over in their respective order after they perform their functions. The register and sender equipment is connected to the link until the operator depresses the sender disconnect key, thereby providing for any tandem dialing conditions that may be encountered in existing or ultimate networks. The sender releases digits within the storage sets without waiting for the complete registration of call numbers. The sender equipment consists of relay groups and is designed to mount on toll trunk relay racks.

Switching Layout

Schematic diagram, figure 8, shows the grouping of circuits with paths connected for a toll-to-toll call, using toll group call number 44. This figure also shows the paths to a local automatic station as on a toll-to-local call using local station number 6384.

Figure 9 shows a similar layout as required for a large multioffice system, with various types of toll line selectors to meet the conditions imposed by different sizes and classifications of toll line groups.

Through Call—Toll to Toll

On a typical through call, the distant operator rings on the toll line causing a ring-down relay to operate and place a marking potential on the associated finder bank contact. The finder switch associated with the operated talk key of a nonbusy inward operator's link automatically begins to search for the marked bank contact, stepping vertically to the bank level on which the marked line is located and horizontally until the marked bank contact is found.

The functioning of the common-start and position-distributing equipment permits only one finder to search for the incoming call. When the finder wipers reach the bank contacts of the calling line, the horizontal motion of the finder switch is stopped and relay operations extend the toll line to the operator's position equipment. Further relay action in the operator's position equipment causes a seizure indication in the form of a "zip-zip" tone to be impressed on the operator's headset, thereby informing her that a call has been received and is ready to be answered. In case no operator is waiting, the call will be held in abeyance and a common answering pilot lamp will light in all positions, as a signal that one or more calls are waiting for operator's service. After a predetermined time interval, a master lamp lights in the chief operator's desk indicating that a call is being delayed. This master lamp flashes after a further predetermined time to draw to the attention of the chief operator a need for closer supervision, or increased operating personnel to guard against delay in serving toll subscribers. Any operator becoming free will throw the talk key of a nonbusy link and the call will be automatically connected to her headset.

One set of bank contacts on the finder switches associates the incoming lines with the group indicating lamps at the toll switchboard. Relay operations in the finder cause the proper toll-group-identity lamp to light automatically, identifying the toll center from which the call originated. The group identity contacts on the finder banks are not multiplied between operators' finder groups; therefore, the identity lamp lights only in the position at which the call was received. Finder relay operations cause the link busy lamp to light when the calling line is seized. This busy lamp remains lighted for the duration of the call to provide for supervision and to guard against attempted reuse of the link until the call is released. As soon as the finder has completed its vertical stepping, circuit conditions are set up in the link and sender finder relays which cause the sender finder to search for an idle impulse sender and digit register group, and for the link finder switch to search for the particular link on which the operator has her talk key thrown.

When an idle sender is connected to the link, the sender supervisory lamp lights, indicating to the operator that an idle sender and digit register group is connected to the front end of the link in readiness for setting up, on the key calling device, the group code of the desired toll center. After acknowledging the call and receiving the order, the operator depresses the digit keys of the calling device in accordance with the number of the desired toll circuit group and depresses the sender disconnect key. Operation of the numeral calling device keys, which are connected to the digit register group through the link and sender relays, causes corresponding digits to be stored in the digit register relay groups in the same order that the keys were depressed.

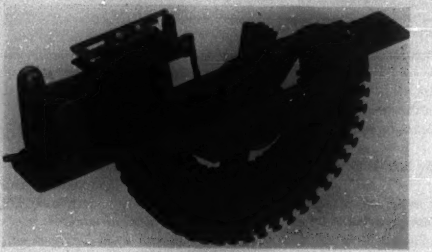


Figure 7. Rotary switch

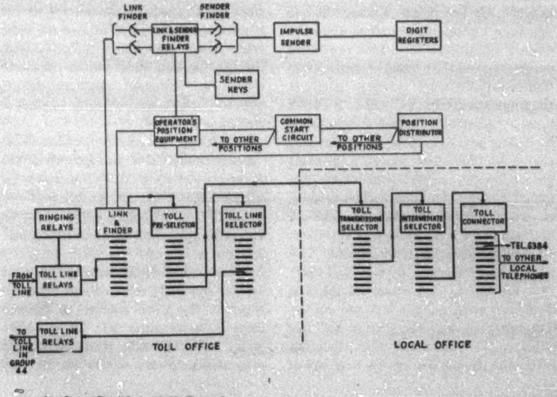
The impulse sender begins sending as soon as the first digit key is depressed and continues as long as any digits remain stored in the digit register relay groups. Preselectors and subsequent switches respond to impulses released by sender and digit register relay groups.

On the typical toll call shown in figure 8, the operator dials 44. The first digit 4 causes the preselector to step to its fourth level and automatically rotate and stop on a nonbusy toll line selector trunk. The second digit 4 causes the toll line selector to step to its fourth level and select an idle toll line.

Depressing the sender disconnect key serves as an indication to the sender equipment that no further digits will be registered and prepares the circuit for disconnect from the link after all stored digits have been sent out. When the switches have completed their functions, relay operations in the sender finder relay group extinguish the supervisory lamp and automatically disconnect the impulse sender from the link, thereby making it available to other operators.

When the sender supervisory lamp goes out, the operator rings on the toll line by throwing her "ring-front" key. Relay operations in the toll line relay group cause the proper ringing frequency to be impressed on the toll line. The terminating toll operator, on answering, may now talk to the originating operator with the remote control operator bridged across the line. At the start of toll conversation, the operator restores the link talk key causing a relay to restore in the link circuit. This relay disconnects the common position circuit from the link, making it available for use on other links.

Figure 8. Call through schematic toll-to-toll and toll-to-local calls

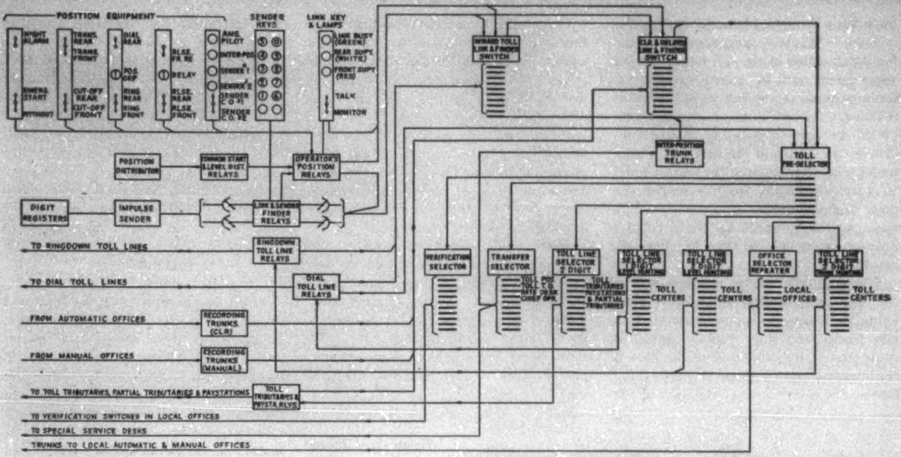


The toll group identity lamp also goes out when the talk key is restored.

When the originating operator rings off on the toll line, relays in both originating and terminating toll line relay groups operate, causing the white and red supervisory lamps of the link circuit to light. The operator throws the link talk key (or link monitoring key in case she is busy on another call) and challenges. With the talk key thrown, relay action in the link circuit reconnects the operator's position equipment to the link. The toll group identity lamp relights, identifying the originating office.

The operator rings off on the front end of the link with the "ring-front" key, giving the terminating operator disconnect supervision. With the talk key still operated, the operator clears the link and connecting circuits by operating a common link release or wipe-out key causing the supervisory lamps, link busy lamp, and toll group identity lamp to go out and the link and trunk circuits to restore to normal. If, upon clearing a call, the operator is ready to receive a new call, the link talk key is left operated; otherwise, it is restored to normal.

When all lines to a desired toll point are busy, a chain relay circuit is closed in the toll line relay groups causing a toll group busy lamp, located in the face panel, adjacent to the identity lamp, to light in all positions. The operator observing this busy lamp delays dialing to that particular toll group in accordance with standard operating practices. Absence of a lighted group busy lamp indicates that a toll circuit is available. Should


POSITION EQUIPMENT
NIGHT ALARM
EMERG. START
WITHOUT
TRANS. REAR
TRANS. FRONT
CUT-OFF REAR
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DIAL REAR
POS. GRP
RING REAR
RING FRONT
RLSE. FR. RE
DELAY
RLSE. REAR
RLSE. FRONT
ANS. PILOT
INTER POS
SENDER 1
SENDER 2
SENDER C.O. #1
SENDER C.O. #2
SENDER KEYS
LINK KEY & LAMPS
LINK BUSY (GREEN)
REAR SUPY. (WHITE)
FRONT SUPY. (RED)
TALK
MONITOR
POSITION DISTRIBUTOR
COMMON START & LEVEL DIST. RELAYS
OPERATOR'S POSITION RELAYS
DIGIT REGISTERS
IMPULSE SENDER
LINK & SENDER FINDER RELAYS
TO RINGDOWN TOLL LINES
RINGDOWN TOLL LINE RELAYS
TO DIAL TOLL LINES
DIAL TOLL LINE RELAYS
FROM AUTOMATIC OFFICES
RECORDING TRUNKS (CLR)
FROM MANUAL OFFICES
RECORDING TRUNKS (MANUAL)
TO TOLL TRIBUTARIES, PARTIAL TRIBUTARIES & PAYSTATIONS
TOLL TRIBUTARIES & PAYSTA. RLYS.
TO VERIFICATION SWITCHES IN LOCAL OFFICES
TO SPECIAL SERVICE DESKS
TRUNKS TO LOCAL AUTOMATIC & MANUAL OFFICES
INWARD TOLL LINK & FINDER SWITCH
CLR & DELAYED LINK & FINDER SWITCH
INTER-POSITION TRUNK RELAYS
TOLL PRE-SELECTOR
VERIFICATION SELECTOR
TRANSFER SELECTOR
TOLL POS.
TOLL T.D.
RATE DESK
CHIEF OPR.
TOLL LINE SELECTOR 2 DIGIT
TOLL TRIBUTARIES PAYSTATIONS & PARTIAL TRIBUTARIES
TOLL LINE SELECTOR 1 DIGIT LEVEL HUNTING
TOLL CENTERS
TOLL LINE SELECTOR 2 DIGIT LEVEL HUNTING
TOLL CENTERS
OFFICE SELECTOR REPEATER
LOCAL OFFICES
TOLL LINE SELECTOR 2 DIGIT TRUNK HUNTING
TOLL CENTERS

Figure 9. Typical toll-switching diagram

the operator fail to observe the busy indication, or should the toll group become busy during her dialing period, relay operations in the toll line selector cause the link supervisory lamp to flash at a rate of 120 impulses per minute. In this event, the front end of the link is released by operating the "release-front" key thereby restoring it to condition for redialing the toll point or for completion by dialing an alternate route.

In case "all paths busy" condition is encountered in the automatic switches prior to reaching the desired toll line group, the trunk switch will rotate to the 11th position of the busy bank level and operate its cam springs causing the link supervisory lamp to flash at a rate of 60 impulses per minute (to differentiate from the toll group busy). In case operating practices do not permit holding the toll line from the originating office when the desired toll group is busy for a prolonged period, the remote control operator, or in this case the through operator, may call the originating office as soon as a circuit becomes available. This is accomplished by dialing on the rear of a link. Throwing the "dial rear" key places a marking ground on the vertical bank of the line finder, causing the finder, when started, to cut in on its first bank level. The operation of any nonbusy link talk key sets its corresponding finder to operate. This finder, due to marking by the "dial rear" key, will cut in and rotate on the first level and seize an idle toll preselector. The operator dials the desired toll line group to the terminating office, then operates the ringing key on the rear end of the link and restores the "dial rear" key. When the distant operator answers, the operator dials the originating office on the front end of the link as previously described.

To permit dialing on the rear of a link, certain leads from the operator's position equipment are terminated on a set of bank contacts on the rotary switch of the link finder. When the "dial rear" key and a link talk key are operated, this set of bank contacts is marked and the link finder switch rotates to these position circuit contacts instead of searching for the link. Simultaneously, the sender finder rotary switch seizes an idle impulse sender and digit relays group. When these circuit functions are completed, the impulse sender is extended over the bank contacts of the sender finder switch, through the link finder switch banks, to the operator's position equipment; thence, over the finder associated with the link to the toll preselector.

Similar operations are performed at "delay" positions to care for call orders which are passed to "delay" operators for completion.

Inward Call—Toll to Local Manual

Inward calls to local manual subscribers are automatically received and acknowledged in the same manner as through calls. The operator sets up an office group selecting code plus the desired manual subscriber's number on the key sending set and depresses the sender disconnect key. Automatic switches cause the local number to be displayed at the manual-exchange call indicator position by way of a call indicator trunk. The link talk key may be restored immediately after the sender disconnect key is depressed. The sender supervisory lamp goes out when the automatic switches have completed their functions.

The call indicator operator plugs into the multiple jack of the called line, if idle, and ringing starts automatically. The front supervisory lamp of the link circuit lights when the line is taken up by the call indicator operator and is extinguished when the local subscriber answers. Conversation starts and the inward operator restores the link talk key, if not previously restored, extinguishing the toll group identity lamp. The operator may monitor by operating the link monitoring key.

Supervision is obtained by the local subscriber restoring the receiver which causes the front supervisory lamp to light and the distant operator ringing on the toll line lights the rear supervisory lamp. The operator throws the link talk key or link monitoring key (in case she is busy on another call) and challenges. With the talk key thrown, the toll group identity lamp relights, identifying the originating toll office.

To disconnect, the inward operator clears the link and connecting circuits by operating the link release or "wipe out" key causing the supervisory lamps, link busy, and toll group identity lamps to go out and the line equipment to restore to normal in readiness to receive a new call.

The call indicator trunk disconnect lamp lights, giving disconnect supervision to the call indicator operator.

When the manual-exchange subscriber's line is busy, the call indicator operator will press a busy key causing the link supervisory lamp to flash at 120 impulses per minute. In this event, the front end of the link is released by operating the "release-front" key, thereby preparing it to redial the manual exchange number. "All paths busy" as on automatic trunks is indicated by a 60-impulse-per-minute flashing link supervisory lamp.

Local Automatic to Ring-Down Toll (CLR)

A local automatic subscriber dialing the long-distance directory code is received by one of the waiting CLR operators who has a nonbusy link key thrown to the talk position. In case no operator is waiting for a call, the incoming call will be held in abeyance with the lighting of the common answering lamp in toll positions and the master lamp in the chief operator's desk as described for through calls. The first idle operator will throw the talk key of any nonbusy link. Seizure tone will be heard the instant the call is connected to her link circuit and the link busy lamp will light. The CLR trunk group identity lamp will also light, indicating the origin of the call. The sender supervisory lamp will light indicating that an idle sender is connected to the link.

After acknowledging the subscriber's order and making a toll ticket, the operator sets up the desired toll circuit on the key sending set and depresses the sender disconnect key. The sender lamp goes out when the automatic switches complete their functions, and the operator throws the "ring front" key to ring on the toll line. The called number is passed to the distant operator and conversation starts when the called party answers. The operator starts timing the ticket and restores the link talk key, extinguishing the CLR trunk group identity lamp. When the local party restores his receiver, the rear link supervisory lamp lights. The operator throws the link talk key (or link monitoring key in case of overlap operation when a talk key is operated on another link), challenges, and stamps the stop time on the ticket. The operator rings on the toll line with the "ring front" key giving the distant operator disconnect supervision. Disconnect operations and line and trunk busy indications are the same as previously described.

If the local party dialing the directory code is in a denied-toll group, the high-frequency seizure tone is followed by a splash of lower-frequency tone. This indicates to the operator that toll service shall be denied to the calling party. If the operator is in doubt as to hearing the denied-service tone, she may cause it to return by operating the "ring rear" key.

Local Manual to Toll, Utilizing Call-Indicator Equipment

The manual-exchange subscriber, removing his receiver, signals the *A* board operator and on a request for "long distance," the *A* operator plugs into an idle recording trunk jack. The CLR (combined line recording) operator receives and acknowledges the call in the same manner as previously described for local automatic-to-toll calls. In addition, since incoming calls from manual exchanges on the CLR method cannot be extended direct to a toll line due to lack of switchhook supervision from the calling party through the manual *A*-position cord circuit, it is necessary that means be provided to permit the CLR operator to reach the calling subscriber over an outgoing trunk from the toll board. The CLR operator, on receiving a call, instructs the calling subscriber to wait on the line and restores the talking key. An idle link key is then operated together with two positional keys termed "dial rear" and "without" keys. The operator sets up the manual office code on her key sender followed by the calling subscriber's number. The subscriber's number is displayed on the call indicator display panel in the usual way but, due to the circuit condition prepared through the action of the "without" key, the associated call-indicator supervisory trunk lamp flashes. This flashing lamp is an indication to the call indicator operator that the call is CLR from toll and that connection should be made to the called line without making the customary busy test. This "without" key is used only on CLR calls where it is intended to disregard busy. It is not used in the completion of inward or delayed calls. Upon reaching the calling subscriber over the outgoing trunk as described above, the calling party is transferred to a toll switching trunk thereby permitting the operator to receive switchhook supervision from the calling party. From this point, the call is handled in the same manner as previously described in connection with local automatic-to-toll calls.

Release of the CLR trunk on which the call was originally received is accomplished by operating the link talk key and "release-rear" key. The *A* operator may be recalled on this trunk any time prior to its release by operating the "ring rear" key intermittently. The *A* operator may recall the toll operator by plugging in and out of the jack.

Delayed Calls

Calls to certain towns, and calls which cannot be completed immediately by CLR operators, are handled on a delayed ticket basis. Tickets for such calls are passed from CLR positions to point-to-point operators.

The point-to-point operator throws the "dial rear" key and establishes connection with the calling subscriber on the rear end of the link by setting up, on her key calling device, the office code plus the subscriber's number. Upon her restoring the "dial rear" key, a sender is automatically connected to the front end of the link, and connection is established with the desired toll line by dialing the proper code. When the distant operator answers, the operator passes the called subscriber's numbers. When the called party answers, the operator rings the calling subscriber by throwing the "ring rear" key. Supervision, timing, disconnect, and busy indications are the same as previously described.

Direct Dialing to Distant Toll Offices

Calls to direct-dialing toll offices, whether originating at the local exchange or switching through it, are handled as explained under the respective preceding methods of operation with the following exceptions.

The local toll operator (inward, CLR, or delayed) in calling the distant exchange number sets up the distant office code followed by the subscriber's number. A flashing supervisory signal is received when the called line is busy or a steady supervisory signal when the called line is free. Ringing of the distant party starts automatically. If the call passes through intermediate automatic exchanges, the operator sets up the code of the intermediate office and awaits dial tone. As soon as dial tone is heard, she sets up the code of the terminating office and then the subscriber's number as previously described.

To reach points beyond direct dialing points, the local toll operator sets up the exchange code and rings. This signals the inward operator at the direct dialing point who acts as a through operator in the standard manner.

Calls Transferred From One Toll Position to Another

The nature of certain classes of calls requires that they be transferred to operators' positions other than those on which the calls are received. The receiving operator may transfer a call to any other position by dialing the required operator code on the front end of the link. This causes the call to be extended to an interposition trunk in the position to which the call should be transferred. An interposition trunk answering lamp will light when the trunk is seized as an indication to the operator that a transfer call is waiting. If the operator should be waiting for a call with a link talk key operated, the call will be received immediately on her headset.

Circuits are so arranged that interposition trunks are given preferential selection when the operator throws a link talk key. On inward and CLR positions, interposition calls are answered before any other waiting calls can be received. On delayed positions they are answered before the operator can originate further calls.

Discussion

B. C. Bellows (Bell Telephone Laboratories, Inc., New York, N. Y.): We are in full agreement with the idea implied in the paper that present toll switchboards should be readily adaptable to the future requirements which seem to indicate clearly a higher degree of mechanization in the toll switching facilities. This requirement has already manifested itself as illustrated by the fact that approximately ten per cent of the present Bell System operator-handled toll traffic is on a dialing basis not requiring the aid of inward or through operators for completion. While we are not entirely familiar with the conditions for which the remote-control cordless toll board was designed we believe it would be of interest to make a few comments on the paper presented, based on Bell System experience.

In so far as Bell System experience is concerned the type of toll switchboards provided today are similar to those provided in the "early days" in appearance only. The present Bell System modern outward toll boards while still of the cord and jack type, are substantially improved from a circuit and equipment standpoint. These improvements include such things as the automatic inclusion of repeater gain at switching centers where this is required, cord circuits arranged for common-battery supervision, dialing or key pulsing on both cords of a cord pair, improved transmission, improved signaling and key equipments, as well as many other miscellaneous improvements making for a more satisfactory operating arrangement.

As far as we can see, the introduction of the combined-line-recording method has had no bearing on the relative advantages of cord and cordless toll boards. Experience in the Bell System has demonstrated that the combined line and recording method requires slightly fewer operators than with previous methods. This is due to the fact that previous operating practices followed in the Bell System required toll operators to give the same undivided attention to the call in hand as is required with the combined-line-recording method, which procedure was justified because of the improved service and greater intertoll trunk efficiency that was thereby obtained.

Present Bell System cord boards are designed to permit dialing or key pulsing and common-battery supervision on both ends of the cord circuits, and are, therefore, readily adaptable to intertoll dialing requirements. There are, of course, inherent differences between the ring-down and intertoll dialing methods of operation. These fundamental differences would appear to be present regardless of the type of toll board employed. Bell System experience indicates that these different procedures do not unduly complicate the operator training and there has been no indication that an increase in operating errors is to be expected.

In the continuous effort that is being made to provide improved facilities, consideration has been given to cordless toll boards for Bell System use. This consideration has indicated that only in the handling of inward and through connections is the cordless toll board of substantially greater efficiency than cord boards. The possibilities for economies on outward boards would seem to lie in the elimination of cord handling and double answering. However, it has been our experience that on outward positions cord handling is so small a part of the operator's work time as to be hardly measurable and double answering occurs in any appreciable amount only when several operators are idle under which condition there is no noticeable effect on operating efficiency. To meet the recognized advantages in the handling of inward and through traffic, a cordless toll board employing crossbar switches is being made available.

Gilbert Sorber and **Arthur Bessey Smith:** We are glad for the remarks by B. C. Bellows, of the Bell Telephone Laboratories, and to note that he is in substantial agreement with us.

It is quite generally recognized that toll boards of today differ materially in circuit design from earlier types of toll boards. Even so, the continued use of plugs and jacks places a very definite limitation on the design of operators' working equipment. With the remote-control toll board it has been possible to design the operators' equipment to meet operators' requirements instead of being strongly influenced by the mechanical limitations of plugs and jacks.

We have not held that the introduction of the CLR (combined-line-recording) method has any direct bearing on the relative advantages of cord or remote-control types of toll boards. We do, however, point out that the type of toll board has a direct bearing on the grade of service to subscribers when the CLR method of operation is used. The remote-control toll board improves the grade of service to subscribers, because it enables the operator to give a greater amount of undivided time to the call in hand.

Inherent differences exist between the ring-down and intertoll dialing method of operation, but the design of the remote-control toll board provides improved facilities for meeting the changes in operating methods which result from these inherent differences.

The increased efficiency which results from the use of the remote-control toll board, when considered on a position basis only, is, we agree, most noticeable on inward traffic. Our experience with actual installations, however, indicates surprisingly enough that because of the greater number of positions involved, the over-all saving in outward traffic is even greater than on inward traffic.

Power Supply for Single-Phase Resistance Welders

R. H. WRIGHT
MEMBER AIEE

THE rapidly increasing use of a-c contact welders in metal fabricating plants imposes single-phase low-power-factor fluctuating loads on plant power systems. The loads may range from a few kilovolt-amperes for a few cycles per weld, as in the case of a small spot welder, up to several thousand kilovolt-amperes for five to ten seconds per weld in the case of a large butt welder. Where the plant is supplied from a small private generating station, single-phase welding loads not only cause troublesome voltage fluctuations but may damage the damper windings of the generators. If power is purchased from a utility, voltage fluctuations may still be present, not only in the plant but often in the entire adjacent area. To eliminate such disturbances it has been necessary in numerous instances to install motor generators to serve as phase converters to supply single-phase power. It is the purpose of this paper to discuss the more common applications for single-phase welding generators and the electrical problems involved in each type of application.

The principal applications for conversion equipment are in connection with butt, flash, spot, and seam welders, and tube welding machines.

Butt Welder

Among the earliest installations of single-phase generators for welding service were two motor-driven units for supplying butt welders used in fabricating automobile rear-axle housings. The original generators for this installation were old engine-type alternators which had been rewound for single-phase service. The normal capacity of the generators was small relative to the welding load and the machines had to be heavily over excited during the welding period. The main circuit between the generator and the welder was closed and opened for each weld. The generator windings vibrated excessively under load and if the main circuit was opened accidentally during a weld, high open-circuit voltages were developed; resulting in bus insulator flashovers and damage to the generator windings. Under such conditions an undue amount of maintenance was required. Operation was improved materially by changing to generator field control. Later modern equipment of greater capacity was installed which has given continuous service with only routine maintenance.

Figure 1 shows the type of weld made in such machines. The welding time varies from five to eight seconds. The generator output varies from 6,000 to 9,000 amperes at 350 to 400 volts, depending on the weight of the stock welded. In figure 2 are shown typical curves of voltage and current input to a rear-axle-housing butt welder. Two recent single-phase generators for this service are rated at 6,500 amperes, 440 volts, 60 cycles, 2,860 kva. Another generator used for the same purpose has a rating of 10,000/5,000 amperes, 220/440 volts, 2,200 kva, 60 cycles.

A schematic diagram of connections of a recent equipment is shown in figure 3. The generator is connected continuously to the primary of the welding transformer; circuit breakers in the main leads being used only for short-circuit protection. Complete control of the welding cycle is obtained by a field contactor and a field rheostat. No automatic voltage-regulating equipment is required as the load is quite stable during the weld. Only infrequent adjustments are required to compensate for changes in welding conditions.

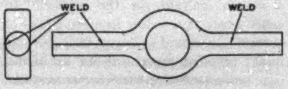

Figure 1. Type of butt welds made on automobile rear-axle housings

Generators for this class of service can be driven satisfactorily by synchronous motors. Where it has been desired to reduce the line peaks or reduce the rate of change of input, wound-rotor induction motors with flywheels have been used. A fixed slip resistance provides a satisfactory means of equalizing the peaks.

Flash Welders

One of the most common functions of the larger flash welders is to weld sheets or plates together at the edges. A large variety of products may have one or more welds made in this way. The two plates to be welded are clamped in the welding machine with the edges parallel. The edges are then brought together lightly and power is applied. Arcing and burning of the metal occurs uniformly throughout the length of the joint and the two pieces are heated rapidly for a short distance back from the line of contact. The two parts of the machine advance slowly together to compensate for the metal which is thrown off or consumed. At the end of the heating period the power is cut off and a quick acting cam forces the two edges together to make the weld.

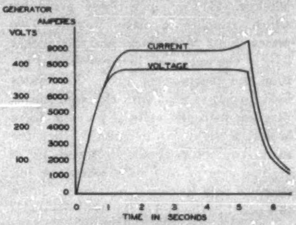

Figure 2. Typical single-phase load on a butt welder for automobile rear-axle housings

The time for making flash welds varies with the thickness of the metal and the amount of power which can be applied without the formation of blowholes. About 10 to 15 seconds is common for many applications involving heavier material. During the early part of the heating period, while irregularities in the edges of the stock are being burned off, the load is light. As the heating extends over the entire length of the weld the load rises rapidly to a maximum and fluctuates violently.

Power is usually controlled by a contactor in the primary circuit of the welder transformer. For many flash-welding operations it is not necessary to use a voltage regulator to maintain satisfactory generator voltage. In such cases generator field forcing is used. Between welds

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Paper number 39-66, recommended by the AIEE committee on electric welding and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 16, 1938; made available for preprinting December 22, 1938.

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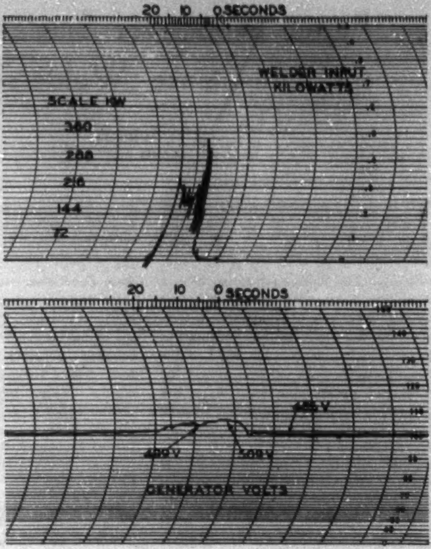

Figure 4. Kilowatts output and voltage of a single-phase generator operating in conjunction with a flash welder

Generator field forced during welding period

the generator excitation is only sufficient to give approximately full voltage at no load. A field-forcing contactor interlocked with the main welder contactor, shunts a portion of the generator field resistance at the start of the heating period. The voltage variation during the heating period is thus limited to less than five per cent. At the end of the heating period the field-forcing contactor opens and normal no-load excitation is restored. Where it is felt that closer regulation of voltage is required to give the desired uniformity of welds, a voltage regulator and quick-response excitation system can be used.

In figure 4 are shown graphic charts of the voltage and kilowatts output of a 500-kva 400-kw single-phase generator which supplies a flash welder. Two plates 0.107 inch thick and 80 inches long at the weld were being welded at the time the charts were made. Generator field forcing is used with this equipment. Starting with 485 volts at no load, there is first a slight dip in voltage when the main contactor closes, followed by a gradual rise to 509 volts when the full effect of field forcing is obtained. From this point to the end of the weld the maximum voltage variation is 3 per cent with load variations from approximately 35 per cent of generator capacity to approximately 80 per cent capacity.

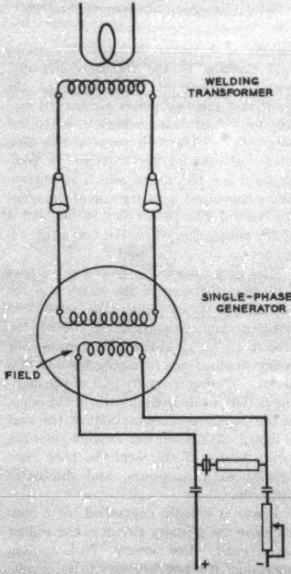

Figure 3. Schematic diagram of connections used for single-phase generators supplying large butt welders

The motor generator for which the curves are shown has a synchronous driving motor, so the load fluctuations are reflected directly to the power lines. Power is supplied by a small local central station. While the load peaks are quite evident on the station chart, no difficulty is experienced in maintaining a steady voltage on the system. In cases where the welder imposes peaks of several thousand kilovolt-amperes, it might be advantageous to use an induction motor and flywheel to iron out the violent fluctuations and limit the input to an average value. However, in the majority of applications, a synchronous driving motor will be quite satisfactory.

Spot and Seam Welders

Spot and seam welders usually operate in conjunction with timers which definitely fix the duration and energy input for each weld. Usually the time for each weld varies from one cycle to 12 or 15 cycles. With spot welders the welds are made at random as desired by the operator and the power impulses follow no definite pattern. The load on the circuit supplying a seam welder consists of a succession of short impulses repeated at regular intervals. Electronic timers are in quite general use for both spot and seam welders.

Because of the peculiar nature of the load, generators which supply single-phase power to spot and seam welders operate under conditions which are quite different from butt-welding and flash-welding service. In spot-welding service it is impracticable to use generator-field-forcing contactors because of the very short duration of the welding peaks. Also generator voltage regulators are of little or no value for the same reason. Single-phase generators for supplying spot welders are, therefore, designed so that the normal capacity is approximately equal to the welding peak loads. The transient voltage drop and the inherent voltage drop at full load are made to be approximately equal. The excitation is kept constant at a value which will give the desired voltage during the weld. In figure 5 are reproductions of oscillograms showing typical load conditions on a 700-kva single-phase generator which supplies a spot welder. The oscillograms show how the generator voltage drops instantly when load is applied and remains constant during the welding period.

In spot-welding service the welding time is only a very small percentage of the total time, so the root-mean-square loads

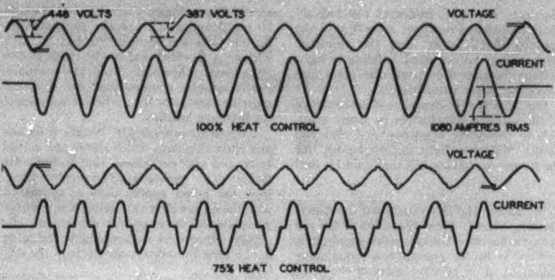

Figure 5. Oscillograms of typical spot-welding loads on a 700-kva 440-volt 30-per-cent-power-factor 60-cycle single-phase generator

Timing and energy input are controlled by an electronic timer

on the generator and its driving motor are very moderate. If a synchronous driving motor were used, the capacity would be determined largely by the magnitude of the peaks received from the generator. It may be possible for the frequency of the load pulsations to be the same as the natural period of oscillation of the set. Such a resonant condition may cause a synchronous driving motor to pull out of step unless special precautions are taken. For these reasons it is more satisfactory to use an induction motor to drive a generator used for spot-welding service. A squirrel-cage motor with a small amount of slip will utilize the flywheel effect of the set to equalize the peaks and insure stable operation. In extreme cases a small amount of additional flywheel effect can be built into the set. Figure 6 shows a 700-kva 0.30-power-factor single-phase spot-welding generator with a 150-horsepower squirrel-cage driving motor.

A motor generator which is to be used for supplying a seam welder will operate under steadier load conditions. A larger motor will be required to carry the higher average loads. Either a squirrel-cage motor or a synchronous motor will be satisfactory. In many cases it will be advantageous to use a voltage regulator with the generator to insure uniform welds. The regulator will maintain a definite average voltage which will be the average between the unregulated no-load and full-load voltages.

Tube Welders

Certain kinds of thin-walled tubing may be made by passing long coils of steel strip at constant speed through a forming and welding machine. Either alternating current or direct current at low voltage can be applied to the welding rolls. Where alternating current is used it may be advantageous to use a frequency higher than that of the regular supply circuit. A motor generator with a single-phase generator of the desired frequency is then required. Close voltage regulation is essential, so voltage regulators are used. Since the load is steady and practically continuous and speed variations should be minimized, a synchronous motor gives the most satisfactory drive.

Conclusion

The problem of power supply for high-capacity single-phase welding circuits has come into general prominence only recently. However, over a period of several years past, motor-driven single-phase generators have been installed for practically every type of welding service. Experience gained from these installations provides a basis for the correct application of conversion equipment for future requirements.

Figure 6. Single-phase 700-kva 0.30-power-factor welding generator with 150-horsepower squirrel-cage driving motor

It is desirable to make each installation as simple as possible and experience has shown that extremely simple apparatus gives excellent service for many applications. It is preferable to use generators with specially designed single-phase windings rather than to use one phase of a three-phase machine. Because of the vibration inherent in single-phase machines, it is highly desirable to avoid working welding generators at high overloads. Also the voltage can be controlled more readily when the peak loads are not greatly in excess of the normal capacity of the generator. The rated power factor of welding generators will range from 30 per cent for spot welders to 60–70 per cent for butt and flash welders.

There are some applications for which it is desirable to operate more than one welder from a single generator. Two or more spot welders with electronic timers can be operated from a single generator of minimum capacity if an electronic synchronizing device is used which permits only one welder to be energized at one time. Such an arrangement introduces no appreciable delay in production since each welder in turn is locked out only a fraction of a second. At least one installation is in successful operation where two welders are operated simultaneously from one generator. Each welder is controlled by a contactor and each main contactor is interlocked with a field-forcing contactor so that the generator field strength is approximately proportional to the load.

Discussion

B. M. Jones (Duquesne Light Company, Pittsburgh, Pa.): We, in Pittsburgh, have a great number of welder installations on our system, and we are getting more of them and they are becoming larger in size; and we believe that electric welding is going to be used more and more because it is an efficient, practical, convenient tool.

As you may know, a power company's concern over welders is the fear that the fluctuating load may cause a flicker in the lights of adjacent customers, which may be, and sometimes is, objectionable. Therefore, it is our job to "tool up" the system in the vicinity of the particular welder to prevent such fluctuating loads from affecting the lighting load to an extent that would be objectionable. Obviously, a slow reduction in voltage to quite an appreciable lowering of the normal voltage and then a gradual return to normal is not so perceptible and is very rarely objectionable, whereas an abrupt lowering of voltage, even to a much less degree than the slow reduction, is perceptible and would very likely become objectionable.

Another point the power company must investigate carefully is the amount of transformer capacity necessary to deliver suitable voltage to make a successful weld—in simple language, the regulation of the transformer. This, in most cases involving medium size and large welders, is a more important factor than the heating effect. A typical example encountered on our system recently was a large welder causing a 250-kva single-phase heating load and requiring a 667-kva transformer to hold the voltage sufficiently close to permit a successful weld. Naturally, this resulted in a larger investment. We have three or four such welders at different plants, and in each case have installed a 667-kva transformer for the 250-kva heating load.

In one of these installations, it developed that the three-phase power bank supplying the rest of the customer's plant had sufficient margin of capacity to carry this 250-kva heating load, but the voltage dips on the main bus with this welder connected thereto would have been of the order of 18 to 20 per cent, and it was felt that this would result in objectionable performance of the customer's motors, such as tripping them off, due to low voltage or sudden change in loads. It was therefore decided to install the 667-kva single-phase transformer for the welding load alone.

Other difficulties we have encountered have been with welders connected to our four-kv three-phase distribution system on the basis of the welding transformer characteristics as shown partially on the name plate, and then after the welder has been placed in service we find that the resulting swings are much greater. In several cases this differential has been so great that we were forced to provide a high tension line for the welder. Actually, in one specific case, we obtained from the welder manufacturer complete electrical characteristics of the transformer, *and then* the performance (by tests we made) indicated conclusively that the welders were not according to the manufacturer's letter, and the resultant large swings necessitated building a high-voltage line into this particular plant and providing larger transformers.

Our plea is that complete characteristics of the welding transformer be made available to the power company, and that you do not "hide your light under a bushel" for the power companies will surely find it out!

L. W. Clark (The Detroit Edison Company, Detroit, Mich.): Any discussion of the use of motor generator sets for serving resistance welders is hardly complete without some indication of the relative value of the three different types of sets in shielding the power-supply system from voltage disturbances. Whenever it becomes necessary to consider a motor generator set, it is usually because the welder, if served direct from the supply system, will cause a disturbing lamp flicker. It thus becomes necessary to install a set that will reduce this flicker to a value not objectionable.

The type of set which will accomplish this at the lowest first cost and operating expense should be selected. It is true that a flywheel induction set is the most effective in eliminating the voltage disturbance, but in most cases I believe it will be found that it does a much better job than is actually necessary. Likewise, the straight induction set without the flywheel is more effective than a synchronous set, but if the synchronous set accomplishes all that is necessary, its high operating power factor usually makes it the logical selection.

Most industrial plants, particularly those using large numbers of welders, need some form of power-factor correction to keep their overall plant power factor within reasonable and economical limits. Both the straight induction set and the flywheel set not only offer no improvement in this regard but actually reduce the plant power factor. The synchronous set, on the other hand, while doing a good job of flicker reduction also pays a return on its investment in the form of plant-power-factor improvement.

Some preliminary calculations on a proposed installation indicate that the introduction of a synchronous set will reduce the flicker caused by a welder in the ratio of between 4 and 8 to 1, the actual ratio depending upon the characteristics of the motor and also the ratio of resistance to reactance in the supply circuit. Assuming that a specific installation showed an improvement in the ratio of 6 to 1, the synchronous set would satisfactorily correct a flicker condition in which the welder, if served direct, dipped the voltage as much as 10 or 12 volts. The similar ratios for induction sets are between 10 and 30 to 1 without a flywheel and in the range above 30 to 1 with a flywheel. From this comparison it appears obvious that the situation must be extremely severe before a flywheel set becomes justified.

I would like to ask Mr. Wright if he has any operating data which shows the actual *before* and *after* conditions, that is, the volts flicker when serving a given welder direct from the power supply lines as compared with the flicker after the addition of a motor generator set between the welder and the supply. Such test data would be valuable in checking the calculated effects of the different sets.

R. H. Wright: Mr. Clark's position regarding the use of flywheels on welding motor generators is in general the same as that of the author. Flywheels are useful only for special applications. Two rather large welding sets with flywheels and induction-motor drive are operating in Detroit on purchased power. When the sets were built, the purchasers felt that flywheels should be incorporated so as to reduce the peaks if it ever became necessary to operate on the plant generating system. When operating on central-station power the reduction of load peaks is of little advantage and synchronous drives would be quite satisfactory and would give the advantages of a higher operating power factor.

Flywheel sets can be used to advantage when power is to be taken from a small d-c generating station. The flywheel will insure steadier speed than can be obtained with a d-c motor alone.

No tests have been made to indicate the improvement in voltage resulting from the transfer of a welder from the power lines to a motor generator.

The matter of momentary peak loads imposed by resistance welding machines, as discussed by Mr. Jones, is very important regardless of the source of power. For most types of welders the load is applied and released instantaneously and the power factor is low. This condition gives the maximum lamp flicker when the welder is connected directly to distribution lines. In calculating the effect of a welding load on the voltage regulation of a given circuit it is necessary to consider the duty cycle of the welder as well as the name-plate rating. Welders which are designed for intermittent duty may draw peaks of 300 per cent of the name-plate rating. Some indication on the name plate of the maximum peak capacity would be of great value.

Effect of Corona Discharge on Liquid Dielectrics

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THE proper functioning of a high percentage of electrical equipment depends on electrical insulating mediums, such as oils, under conditions known to include corona discharge. This is particularly true in the case of high-voltage cables of the "solid" type.

As early as 1917, Clark and Shanklin[1] published curves of resistivity versus voltage for paper-insulated cables at low temperatures, and attributed the erratic nature of these curves to gaseous ionization occurring in voids in the insulation. Less than two years later, Shanklin and Matson[2] discussed the ionization of occluded gases in high-voltage insulation quite extensively. In their paper, mention was made of some chemical analyses of the deteriorated material deposited on the walls of the gas spaces. A great deal of work followed in which various aspects of corona in voids in dielectrics were studied. In 1924, a paper was presented by Del Mar and Hanson[3] and much of the ensuing discussion of the paper concerned a wax-like substance, which they termed *X*, which had been found in oil-impregnated paper cables after they had been subjected to high electrical stresses. Some tests were described by E. R. Thomas[4] in which cable impregnating compounds had been subjected to corona discharge in the laboratory with the result that a yellow waxy substance with numerous void spaces was formed which was termed "Swiss cheese." Similar tests were later reported by Farmer[5] and by Willman.[6] It appeared evident then that the wax-like substance, *X*, was the result of a condensation of the oil molecules due to the action of the gaseous electric discharge. Schoepfle and Connell[7] undertook a careful review of the literature on this subject and concluded that the same reactions of hydrocarbons were produced by both the corona discharge and the alpha particle. Their studies of the effect of cathode rays on hydrocarbon oils showed that cathode rays also had to be classed in this category.

There was at that time considerable information available in the literature regarding the chemical changes brought about in hydrocarbons by electrical bombardment, but there was a distinct lack of knowledge with respect to the changes occurring in the electrical characteristics of hydrocarbons. In most of the investigations that had been conducted, a type of discharge different from corona discharge was used, or the hydrocarbons involved were gaseous at atmospheric conditions, or the effects studied were concerned with chemical changes only. In the present studies, which were started late in 1930, use was made of a corona discharge cell, which could be expected to simulate closely the discharges occurring in the highly stressed insulation of a-c equipment in commercial use. The materials studied were oils, such as liquid paraffin, cable oils, and hydrocarbons of various known types of molecular structures. The main purpose of the studies was to investigate what changes, if any, occur in the electrical characteristics of liquid dielectrics due to corona discharge. Changes in their chemical and physical characteristics were also investigated in the hope of uncovering any relations which might exist between these characteristics and their changes due to corona discharge.

TEST EQUIPMENT AND METHOD

Two test cells which were used in these studies are shown in figures 1 and 2. The cell shown in figure 1, designated as cell 1, was designed to incorporate the features of having the oil sample between layers of solid dielectrics while being subjected to corona discharge, and between metal electrodes while its electrical characteristics were being measured. This cell was also designed to be self-contained in order to eliminate possibilities of contamination of the sample during transfer from bombardment chamber to measuring chamber. The transfer of the sample was accomplished by means of automatic circulation of the oil, a feature used by Becker[8] in his studies on the transformation of hydrocarbons. Figure 1 shows a cross section of the cell as it appears during operation. With voltage applied between electrodes E_1 and E_2 and a Sprengel pump connected to stopcock *V*, bubbles form in the oil in discharge chamber *D*, and are drawn through connecting tube T_1 into measuring chamber *M*. Here the bubbles burst, liberating the gas to be pumped off and releasing the oil to mix with that in the measuring chamber from which it eventually returns to the bombardment chamber through tube T_2. The bombardment may be stopped at any time and measurements may be made to obtain the electrical characteristics of the oil.

Table I. Data on Test Cells

Cell 1: Measuring cell	
Outside diameter of active electrode (inches)	1.000
Length of active electrode (inches)	8.000
Inside diameter of high-voltage electrode (inches)	1.200
Thickness of test sample (inch)	0.100
Thickness of mica washers (inch)	0.030
Cell constant A/L (centimeters)	709
Capacity of cell with air dielectric (micro-microfarads)	63.5
Bombardment chamber	
Inside diameter of inner tube (millimeters)	21.4
Outside diameter of inner tube (millimeters)	25.4
Inside diameter of outer tube (millimeters)	30.4
Outside diameter of outer tube (millimeters)	34.4
Thickness of bombardment space (millimeters)	2.5
Length of bombardment electrode (inches)	8.000
Amount of oil necessary, per run (milliliters)	550
Cell 2: Measuring cell	
Diameter of active electrode (inches)	2.000
Width of guard ring (inch)	0.208
Spacing between active electrode and guard ring (inch)	0.003
Spacing between active and high voltage electrodes (inch)	0.010
Cell constant A/L (centimeters)	1596
Capacity of cell with air dielectric (micro-microfarads)	141
Amount of oil necessary to fill measuring cell (milliliters)	5
Bombardment chamber	
Size of gas accommodation bulb (liters)	2
Outside diameter of inner bulb of bombardment chamber (millimeters)	37
Inside diameter of outer bulb of bombardment chamber (millimeters)	42
Space between inner and outer bulb (millimeters)	2.5
Wall thickness of glass (millimeters)	2

Note: All metal parts of cells 1 and 2 heavily gold plated.

Paper number 39-32, recommended by the AIEE committee on research, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted May 26, 1938; made available for preprinting December 28, 1938.

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This work formed part of an investigation on the deterioration of high-voltage underground cable conducted by The Detroit Edison Company under the direction of the late Doctor C. F. Hirshfeld, chief of research. It was made possible only by the co-operation of the staff of the research department, and the aid and advice of many people not connected with the department. Thanks are offered to all, and in particular to Doctor C. S. Schoepfle, professor of organic chemistry at the University of Michigan, who served as a consultant, and to C. D. Robb and A. G. Fleiger of The Detroit Edison Company research department, who made most of the measurements and determinations involved.

1. For all numbered references, see list at end of paper.

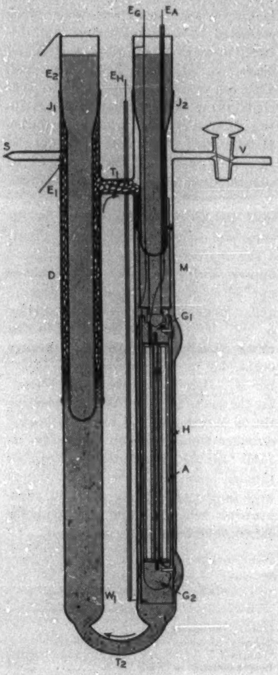


Figure 1. Cell 1 for corona discharge and for power-factor and conductivity determinations

A—Active electrode
D—Discharge chamber
E_1—Graphited bombardment electrode
E_2—Silvered bombardment electrode
E_A—Connection to active electrode
E_G—Connection to guard circuit
E_H—Connection to high-potential electrode
G_1, G_2—Parts of guard circuit
H—High potential electrode
J_1, J_2—Ground joints
M—Measuring chamber
S—Seal-off tube
T_1—Top connecting chamber
T_2—Bottom connecting tube
V—Stopcock
W_1—Support for measuring cell and connection to high-voltage electrode

Cell 1 was used in the preliminary tests on oils of which large amounts were available. Later, when hydrocarbons of which large amounts were not readily available were studied, the cell shown in figure 2, designated as cell 2, was used. Its main parts are the cell for electrical measurements *E*, the bombardment chamber *A*, and the gas accommodation bulb *B*. Incorporation of these three parts in one cell renders it self-contained to a high degree. The volume of the bombardment chamber is large compared with the volume of the oil sample; that is, all of the oil is in the bombardment chamber during the bombardment period, which renders superfluous any extra means for keeping the oil in circulation. The oil is transferred to the measuring cell by turning the whole cell upside down. The volume of the gas accommodation bulb is sufficient to avoid pressure changes of objectionable magnitude in the cell from gas produced during normal periods of bombardment. This makes unnecessary the removal of gas from the cell during the bombardment period and enables omission of liquid air traps necessary in the line to the Sprengel pump used with the previous cell. Pertinent dimensions and constants of both test cells are given in table I.

In the tests with cell 1, the samples were subjected to two bombardment periods, each of $6^1/_2$ hours duration. The gases evolved in the first one-half hour of each bombardment period were used to flush the entire vacuum system and then discarded; those evolved during the remaining six hours were pumped off continuously by means of a Sprengel pump and stored in a gas-collecting bottle. A Schering bridge was used for the power factor measurements which were made at 4,750 volts, 60 cycles (average stress of 47.5 volts per mil). In the tests with cell 2, the samples were subjected to one bombardment period of four hours duration. The evolved gases were accumulated in the cell and were pumped off after the bombardment period by means of a Toepler pump. A transformer bridge, similar to that described by Doyle and Salter,[9] was used for the power factor measurements which were made at 200 volts, 60 cycles (stress 20 volts per mil). In a number of special test runs, the test conditions were changed to suit the particular needs. Measurements of power factor, d-c resistance, and other electrical characteristics were made at various temperatures before bombardment and after each bombardment period. In some cases, attempts were made to measure the current in the corona discharge. Dielectric strength determinations were also made in a few instances on samples before and after bombardment. The amount of gas liberated during bombardment and the composition of the gas were determined in the majority of all tests. In a number of cases, determinations were made, before and after bombardment, of iodine number, hydrophil content, and viscosity.

COMPOUNDS TESTED

The materials studied were pure liquid paraffin oils, various cable oils, and ten hydrocarbons of known types of molecular structure. The cable oils and certain samples of the liquid paraffin were used as received, except for drying and degassing. The other compounds were procured as pure as possible, but some had to be treated chemically to purify them further; all were distilled in a good vacuum at low temperatures. Pertinent information regarding the samples is given in table II. After the samples were prepared for test, they were protected from coming into contact with the atmosphere by being transferred either directly into the highly evacuated test cell or into

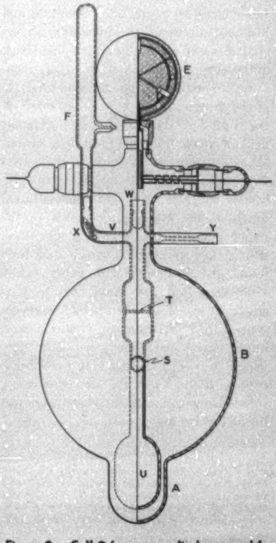


Figure 2. Cell 2 for corona discharge and for power-factor and conductivity determinations

A—Discharge chamber
B—Gas accommodation bulb
E—Cell for electrical measurements
F—Sample container
S—Main offtake tube
T—Sillimanite filter
U—Silvered bombardment electrode
V—Offtake for sample container
W—Offtake for removal of gas
X—Glass-enclosed magnetically operated hammer
Y—Seal-off tube

Table II. Oil Samples and Their Treatment Before Test

Samples				Treatment				
					Distillation			
Name of Compound	Designation	Formula or Base	Source	Initial	Kind of Still	Temperature	Pressure	Residue
n-hexadecane	*A*	$C_{16}H_{34}$	Eastman Kodak Co., Eastman grade	None	Hickman	Degassing coil at room temperature Distilling column at 80 deg C	Between 0.1 and 0.2 micron Hg	Very small percentage
n-hexadecene	*B*	$C_{16}H_{32}$	Synthesized from cetyl alcohol at University of Michigan	Treated with metallic sodium	Hickman	About same as n-hexadecane		
Synthetic oil number 4530*a* (hydrogenated 4530)	*C*	Mixture of saturated branched chain hydrocarbons	Synthesized by hydrogenation of 4530 and 4555 respectively at temperatures up to 200 deg C and pressures up to 200 atmospheres, Ni catalyst, at University of Wisconsin	None	Hickman	Removal during distillation of small amounts of methylcyclohexane introduced during hydrogenation process. Otherwise about the same as the corresponding unhydrogenated oils.		
Synthetic oil number 4555*a* (hydrogenated 4555)	*D*							
Synthetic oil number 4530	*E*	Mixture of unsaturated branched chain hydrocarbons. Molecular weight—610 for *E*; 944 for *F*	Standard Oil Co. (Indiana)	None	Hickman	Degassing coil below 125 deg C Distilling column about 185 deg C	0.01 micron Hg gas pressure	About 50 per cent of starting material
Synthetic oil number 4555	*F*					Degassing coil below 150 deg C Distilling column at 320 deg C		About 20 per cent of starting material
Decalin (decahydronaphthalene)	*G*	$C_{10}H_{18}$	Eastman Kodak Co., practical grade	Several days over sodium, then centrifuged and filtered	Conventional high-vaccum still	Distillation bulb at room temperature. Receiver at −80 deg C	0.01 micron Hg gas pressure	About 1 per cent of starting material
Turpentine polymer (synthetic)	*H*	Mixture of unsaturated cyclic hydrocarbons	Turpentine polymerized with $AlCl_3$	None	Hickman	Degassing coil at room temperature Distilling column at 100 deg C	Between 0.1 and 1.0 micron Hg	About 20 per cent very viscous residue
Alpha-methylnaphthalene	*I*	$C_{10}H_7CH_3$	Synthesized from alpha bromonaphthalene, magnesium and dimethylsulphate	Fractionally crystallized to remove impurity (naphthalene)	Conventional high-vacuum still. Sodium in distillation bulb	Distillation bulb at 70 deg C Receiver at −80 deg C	0.01 micron Hg gas pressure	Very small percentage
Tetralin (tetrahydronaphthalene)	*J*	$C_{10}H_{12}$	Eastman Kodak Co., practical grade	Distilled over sodium in low-vacuum still at elevated temperature	Conventional high-vacuum still. Sodium in distillation bulb	Distillation bulb at 40 deg C Receiver at −80 deg C	0.01 micron Hg gas pressure	Very small percentage
Liquid paraffin	*K*	Heavy, white mineral oil	Standard Oil Co. (Indiana)	None	Hickman	Degassing coil at 135 deg C Distilling column at 185 deg C	0.01 micron Hg gas pressure	About 20 per cent of starting material
Commercial cable compound	*L*	....	Cable manufacturer	Degassed and dried by spraying droplets of oil into high vacuum	None of the cable oils were subjected to distillation			
Oil number 107	*M*	Highly refined paraffin	Oils 107 to 113 supplied by Doctor Whitehead, The Johns Hopkins University					
Oil number 109	*N*	Highly refined naphthene						
Oil number 111	*O*	Paraffin blend						
Oil number 113	*P*	Refined naphthene						

highly evacuated sample containers provided with thin glass windows, which could be broken at the proper time by means of a glass-enclosed iron hammer, operated by a magnet (see *X*, figure 2).

Electrical Characteristics

POWER FACTOR

During the preliminary tests on a liquid paraffin and a cable compound, using cell 1, it was found that the power factor of the test samples increased considerably due to bombardment under corona discharge. This can be seen from figures 3 and 4, which show power factor-temperature characteristics of the oils before bombardment and after each of two $6^1/_2$-hour bombardment periods. In all of the preliminary tests, the samples were simply dried and degassed before test and were forced into an evacuated cell of the type shown in figure 1 by means of dry-nitrogen pressure. The nitrogen was removed before the start of the bombardment, which was carried out at a gas pressure of less than one millimeter of mercury. Nitrogen was not used in any of the other tests conducted with cell 1 or cell 2.

It was found that by distilling the bombarded liquid paraffin, a distillate is obtained, the power factor of which is of nearly the same value as that of the sample before bombardment. The constituents of the bombarded sample which are responsible for its high power factor remain in the residue. In these particular tests, distillation was carried out at a temperature of about 180 degrees centigrade in a high-vacuum still suggested by Hickman.[10] In another group of tests, the liquid paraffin was distilled in this manner before bombardment. A purer sample was thus obtained and it

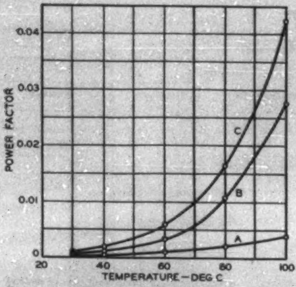


Figure 3. Power factor-temperature characteristics of liquid paraffin. Average of three tests conducted with cell 1

A—Before bombardment
B—After 6½ hours of bombardment
C—After 13 hours of bombardment

was found that the removal of the less volatile fraction and, possibly, of impurities resulted in lower initial power factor values and in a smaller magnitude of changes due to bombardment, but did not result in an elimination of these changes. The power factor-temperature characteristics of one of these samples at various stages of the test are shown in figure 5. In this test, an entirely closed glass system was used in order to prevent any possibility of contamination of the distilled liquid paraffin sample, and the sample was bombarded, the bombarded oil was distilled, and the distillate was rebombarded without opening the system. An automatic Sprengel pump, included in the system, served to store a part of the evolved gas in a gas-collecting bottle for subsequent analysis and also to recirculate the remainder of the gas through the oil in the bombardment chamber in order to support the movement of the oil from this part of the cell to the measuring chamber.

At the time when these studies were started, there was a widespread belief that, since most pure hydrocarbons were known to have excellent electrical characteristics, bombardment of a hydrocarbon sample would not result in changes of its electrical characteristics unless the bombardment occurred in the presence of oxygen or some other impurity. The test reported here showed, however, that despite the most rigid exclusion of contaminants, increases of power factor were obtained due to bombardment and that material of higher boiling point which was produced by the bombardment was responsible for this change. Since it could be concluded that the larger molecules formed in the discharge, by condensation or polymerization or both, were hydrocarbons which exerted a harmful effect on the power factor of the sample and since the structure of these larger molecules should depend on the structure of the starting material, a study of the effect of the molecular structure of a hydrocarbon on the power factor change due to bombardment was undertaken.

Cell 2, which was designed for this study, was first tried on samples of distilled liquid paraffin. In these tests as well as in all others conducted with cell 2, the gas pressure in the cell at the start of the bombardment period was a small fraction of one micron of mercury. Results obtained in one-hour and four-hour bombardment periods are shown in figure 6. It is apparent from a comparison of these results with those given in figure 5 that this cell is capable of producing results considerably faster than the cell shown in figure 1. Ten hydrocarbons of known type structure were studied with this cell, and the results of determinations of power factor-temperature characteristics are shown graphically in parts *A* to *J* of figure 7, each part depicting characteristics of the individual hydrocarbon obtaining before and after four hours of bombardment. Since the power factor values cover a range from 0.0001 to almost 1.0, a four-cycle logarith-

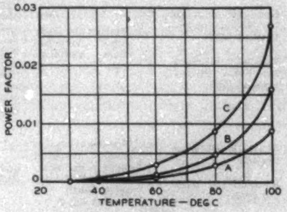


Figure 4. Power factor-temperature characteristics of a cable compound. Average of three tests conducted with cell 1

A—Before bombardment
B—After 6½ hours of bombardment
C—After 13 hours of bombardment

Table III. Conductivities of Various Compounds at Several Temperatures Before and After Bombardment

Compound	Conductivity*	Before Bombardment 100°C	80°C	60°C	50°C	40°C	25°C	After Bombardment 100°C	80°C	60°C	50°C	40°C	25°C
Hexadecane	A-C		0.066	0.033		0.013			5.04	3.40		1.78	
	D-C		0.0002	0.0001		0.0001			0.859	0.335		0.120	
	A-C/D-C		330	330		130			5.87	10.1		14.8	
Decalin	A-C				0.162	0.142	0.107			8.98	9.63	5.46	4.89
	D-C				0.0003	0.0002	0.0001			0.062	0.048	0.029	0.017
	A-C/D-C				540	710	1070			144.8	200.5	188.2	287.6
Turpentine polymer	A-C	1.29	0.43	0.11		0.030		4.44	1.93	1.48		0.19	
	D-C	0.884	0.36	0.072		0.017		2.39	0.684	0.166		0.031	
	A-C/D-C	1.46	1.29	1.53		1.76		1.86	2.82	8.91		6.14	
Alpha-methylnaphthalene	A-C			2.23	1.71	1.26	0.666			212.3	115.0	66.4	31.3
	D-C			0.124	0.097	0.066	0.033			44.2	21.9	12.4	4.57
	A-C/D-C			18.0	17.6	19.1	20.2			4.80	5.25	5.37	6.85
Number 107	A-C	0.344	0.123	0.054		0.016		8.44	3.13	0.772		0.133	
	D-C	0.066	0.025	0.008		0.004		0.624	0.177	0.062		0.023	
	A-C/D-C	5.22	4.92	6.75		4.00		13.5	17.7	12.5		5.78	
Number 109	A-C	0.526	0.161	0.074		0.030		4.29	1.71	0.460		0.090	
	D-C	0.449	0.096	0.036		0.013		0.503	0.127	0.037		0.010	
	A-C/D-C	1.17	1.68	2.05		2.31		8.53	13.5	12.4		9.00	
Number 111	A-C	4.38	2.41	1.04		0.365		5.19	2.52	0.960		0.255	
	D-C	0.216	0.068	0.023		0.006		0.211	0.065	0.026		0.008	
	A-C/D-C	20.3	35.4	45.2		60.8		24.6	38.8	36.9		31.9	
Number 113	A-C	3.22	1.58	0.664		0.219		8.96	4.69	1.97		0.674	
	D-C	1.42	0.784	0.377		0.135		0.462	0.129	0.042		0.017	
	A-C/D-C	2.28	2.02	1.76		1.62		19.4	36.4	47.0		39.7	

* Conductivities given in mhos × 10^{12}. A-c conductivity calculated from 60-cycle power factor values. D-c conductivity is one-minute conductivity.

mic scale was used in order to enable easy comparison of all ten compounds. These characteristics represent average values from two or more test runs except in the case of tetralin (*J*) in which only one test run is represented. This material, tetralin, attacked the glass of the bombardment chamber during bombardment, chipping out small pieces from the glass wall. Similar experiences with tetralin were reported by Becker.[8] The initial power factors of a few of the compounds, namely, turpentine polymer (*H*), alpha-methylnaphthalene (*I*), and tetralin (*J*), were higher than was expected. It should be pointed out, however, that it is very difficult to obtain these hydrocarbons in a very pure state. The increases in power factor were quite different for the various types of hydrocarbons. It will be noted that very large increases were obtained for the straight chain hydrocarbons, hexadecane (*A*) and hexadecene (*B*), for the saturated cyclic hydrocarbon, decalin (*G*), for the aromatic hydrocarbon, alpha-methylnaphthalene (*I*), and for the cyclic aromatic-aliphatic hydrocarbon, tetralin (*J*). The smallest power factor increases were obtained for saturated and unsaturated branched chain hydrocarbons, oils number 4555*a* (*D*), number 4530 (*E*), and number 4555 (*F*), and for the unsaturated cyclic hydrocarbon, turpentine polymer (*H*). The power factor increase for the hydrogenated oil number 4530*a* (*C*) was quite large, however. Considering both the initial power factor values and the increase of power factor values with bombardment, it is found that certain members of the branched chain hydrocarbons gave the best results. This statement is modified, however, in a later section of this paper under "Discussion." In addition to the oils of known type molecular structure, four oils representative of those offered to the cable trade were tested; for the latter, practically no information is available regarding their origins, refinement, or molecular structure. These four oils were chosen from a number of oils kindly supplied by Doctor Whitehead, who used them in other investigations.[11,12] The oils tested were selected to include some of high as well as some of low viscosity, and also to represent paraffin-base and naphthene-base types. The power factor-temperature characteristics of these oils before and after bombardment are shown in figure 8. It will be noticed that three of the four samples suffered quite a power factor increase due to bombardment. The fourth sample, oil number 111 (*O*), a low-viscosity paraffin-base product showed a very small change.

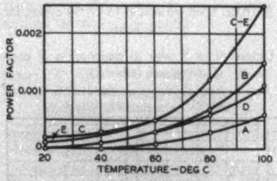


Figure 5. Power factor-temperature characteristics of liquid paraffin distilled before test. Test conducted with cell 1 in entirely closed glass system

A—Before bombardment
B—After 13 hours of bombardment
C—After 39 hours of bombardment
D—Distillate from *C*
E—Bombarded distillate (13 hours)

CONDUCTIVITY

In a number of test runs, determinations were made of one-minute d-c conductivity to learn how this characteristic of the oil was affected by corona discharge. Also, equivalent a-c conductivities were calculated from the power-factor values to enable comparison between a-c and d-c conductivites. A number of typical results are listed in table III.

It will be noted that in the case of the four hydrocarbons of known type molecular structure there were very large increases in d-c conductivity due to bombardment, whereas in the case of the cable compounds, changes were slight except for oil number 107 (*M*). The ratios of 60-cycle a-c conductivity to one-minute d-c conductivity cover a wide range for the first group and a much smaller range for the second group.

A few special test runs were conducted in which the changes in d-c and a-c conductivity as a function of time of direct-voltage application were studied. In one particular test, the sample consisted of the residue from bombarded decalin redispersed in the distillate, which had been distilled from the bombarded decalin earlier in the studies. The measurements were made at 50 degrees centigrade and approximately 20 volts per mil. First, a number of power factor measurements were made without intervening d-c measurements, then direct voltage was applied for one-half hour, and after this, power factor values were again measured at 15-minute intervals for some time without intervening d-c measurements. The results were used in plotting the curves of figure 9. During the one-half-hour d-c application, the d-c conductivity decreased quite rapidly at first and then more slowly; the power factor dropped from 0.1243 to 0.0380, and upon discontinuation of the direct voltage increased again at a rapidly diminishing rate. Similar observations, but involving much smaller changes, have been made by Whitehead and Shevki[13] on unbombarded cable impregnants.

BOMBARDMENT CURRENT

In these studies an attempt was made to have test conditions closely parallel those encountered in commercial service. For this reason the bombardment voltage, rather than the current, was maintained constant; and measurements of the current involved in the discharge were made by means of a vacuum-thermocouple microammeter connected in the high-voltage circuit. Considerable difficulty was experienced in these measurements, particularly when the range of the meter had to be increased by means of external multipliers, because of the high-frequency components in the discharge current. An all-wave radio receiver, covering a

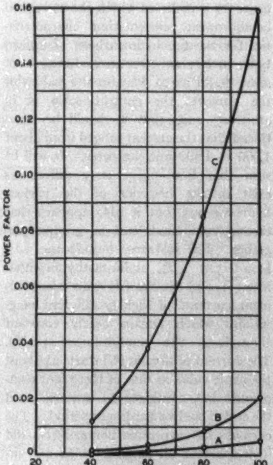


Figure 6. Power factor-temperature characteristics of liquid paraffin distilled before test. Tests conducted with cell 2

A—Before bombardment (average of seven tests)

B—After one hour of bombardment (average of four tests)

C—After four hours of bombardment (average of three tests)

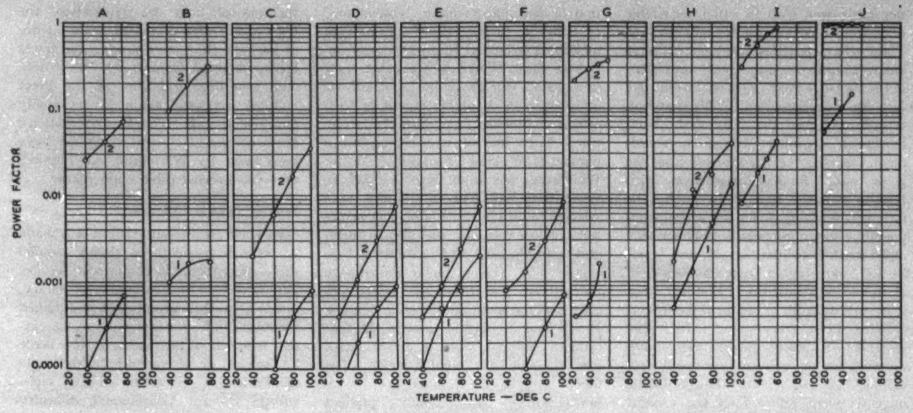


Figure 7 (above). Power factor-temperature characteristics of ten hydrocarbons of known type molecular structure

1—Before bombardment

2—After bombardment at 15 kv, 60 cycles, for four hours

Letters *A* to *J* refer to designation of compounds (table II)

range from 550 kilocycles to 61 megacycles, indicated frequencies up to 40 megacycles to be present in the discharge.

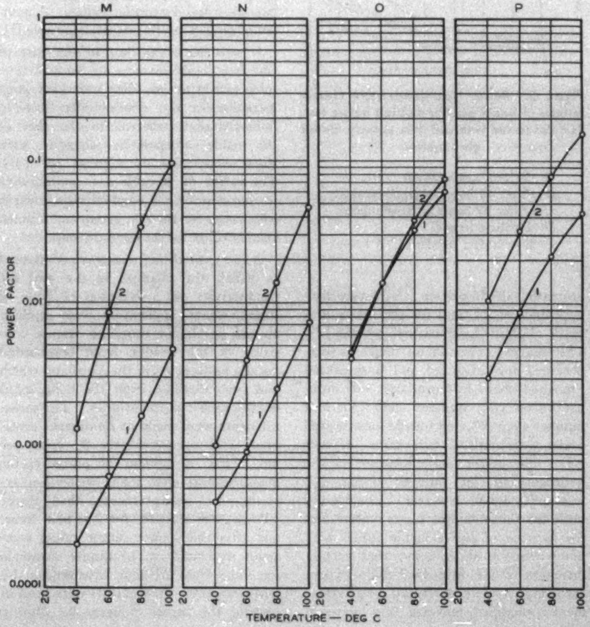
In order to show the type of results obtained by the current measurements, figure 10 is included which shows typical bombardment current-time characteristics for the ten hydrocarbons of known type molecular structure. Because of some doubt as to the absolute values of the current, the current scale is in arbitrary units, but it should be mentioned that the current ranged from about 1,000 to 4,600 microamperes. It will be seen that here again large differences exist in the behavior of the various hydrocarbons, but it also appears that certain compounds can be grouped together. For instance, hexadecane (*A*), hexadecene (*B*), alpha-methylnaphthalene (*I*), and tetralin (*J*) draw bombardment currents of slightly different magnitudes which remain nearly constant throughout the bombardment period. The current of decalin (*G*) starts at about the same value as that of these four compounds but continues to increase until the end of the bombardment period. The currents for turpentine polymer (*H*) and hydrogenated oil number 4555*a* (*D*) increase during the first 30 minutes of bombardment, then decrease to the end of the bombardment period; the current for the unsaturated oil number 4555 (*F*) is somewhat similar. Bombardment currents of the oil number 4530 (*E*) (unsaturated) and of the hydrogenated oil number 4530*a* (*C*) increase rapidly during the first few minutes, remain at the maximum for a short time, and then decrease. When the unsaturated oil

Figure 8 (below). Power factor-temperature characteristics of four cable compounds

1—Before bombardment

2—After bombardment at 15 kv, 60 cycles, for four hours

Letters *M* to *P* refer to designation of compounds (table II)

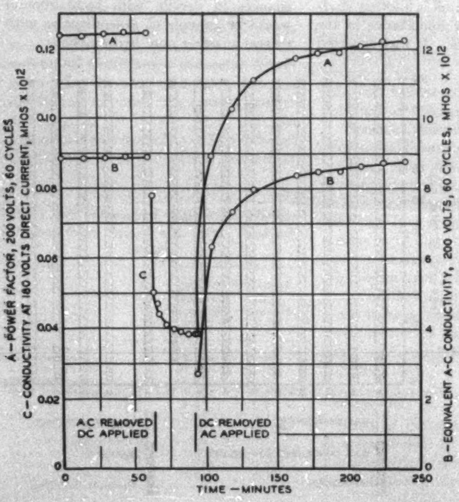


number 4530 (*E*) was bombarded, it was found that during part of the bombardment period, the current was larger than the range of the microammeter and multiplier (2,500 microamperes). During tests on the hydrogenated oil number 4530*a* (*C*) another multiplier was used, approximately doubling the previous range; the values measured under this condition were not directly comparable with those measured on the other nine compounds since the exact multiplication factor of the multiplier was not known because of the error due to high frequencies.

The bombardment currents encountered in studying the commercial cable compounds numbers 109 (*N*), 111 (*O*), and 113 (*P*) were similar to those of the group of compounds including (*A*), (*B*), (*I*), and (*J*) of figure 10. The current of compound number 107 (*M*) was also of the same magnitude as the above group of compounds during the first 30 to 45 minutes of the bombardment period; after this the current was larger than the range of the microammeter and remained so until the end of the period.

Figure 9. Effect of one-half-hour direct-voltage application on power factor and conductivity of bombarded decalin

Measurements:
Temperature, 50 degrees centigrade
Alternating voltage, 200 volts, 60 cycles
Direct voltage, 180 volts
Electrode spacing, 0.010 inch



Dielectric Constant and Dielectric Strength

The dielectric constants of the oil samples at various temperatures before and after bombardment were obtained for a large number of the samples tested. There was practically no difference in the first three significant figures of the corresponding values of the dielectric constants before and after bombardment. These values were calculated from capacity determinations which were incidental to the power-factor measurements in the case of the cell containing oil, and which were obtained for the empty cell by means of a capacity bridge. The dielectric strength of a number of oil samples bombarded in cell 1 was determined before and after bombardment, using test cups described in the ASTM Standard Method *D*117–36. In practically all cases, the dielectric strength of a sample after bombardment was lower than that before. For instance, the average dielectric strength of samples from three test runs was 44.5 kv before and 30.5 kv after bombardment for the liquid paraffin, and 33.3 kv and 27.1 kv, respectively, for a commercial cable compound.

Other Characteristics

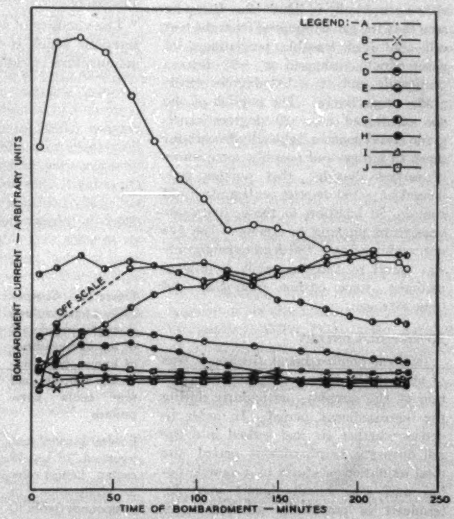
Gas Evolution and Gas Composition

The evolution of gas from an oil under corona discharge is of considerable importance in cable practice. Some have held that a large gas evolution would be beneficial because it would tend to increase the gas pressure in cable voids and would eventually bring about pressure conditions under which the discharge could not maintain itself. Others have reasoned that the gas evolution should be a minimum in order to prevent the gas from forming paths between existing voids and thereby hastening eventual breakdown. Whatever the particular requirements, it was considered important to know the gas evolution of various types of oils and the composition of the gases evolved.

The amounts of gas evolved from ten hydrocarbons of known type molecular structure and from four commercial cable compounds are shown graphically in figure 11. It is evident that the aromatic compound and unsaturated compounds of other types of molecular structure gave off smaller amounts of gas than did the saturated compounds. It is also evident that of the four cable compounds, those of paraffinic base gave off more gas than did those of naphthenic base, and that the more highly refined oils gave off more gas than did the less refined oils. In

Figure 10. Bombardment-current versus time-of-bombardment characteristics of ten hydrocarbons of known type molecular structure

Letters *A* to *J* refer to designation of compounds (table II)



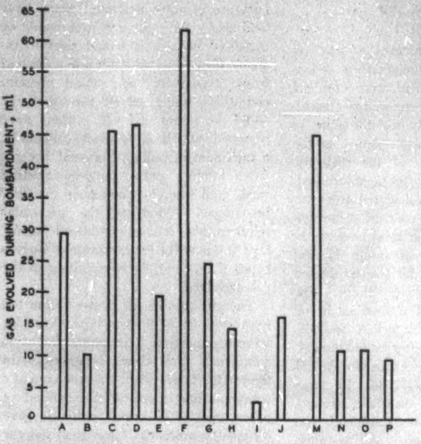


Figure 11. Gas evolved during bombardment of ten hydrocarbons of known type molecular structure and four cable compounds

Bombardment of four hours at 15 kv, 60 cycles. Letters refer to designation of compounds (table II)

this connection, it should be mentioned that the degree of refining is also an indication of the degree of saturation, since the refining process tends to eliminate unsaturates. Similar results were obtained by Schoepfle and Connell[7] and by Schoepfle and Fellows[14] by using cathode-ray bombardment, and more recently by Nederbragt[15] and by Berberich[16] by means of corona discharge.

The compositions of the evolved gases were determined by means of a method described by Fleiger.[17] The results are shown graphically in figure 12. It can be seen that the gas as removed from the test cell contained sizeable percentages of gases which condensed at −80 degrees centigrade and at −180 degrees centigrade, respectively. The portion of the gas condensed at −80 degrees centigrade could contain lighter hydrocarbons down to butane and possibly some traces of carbon dioxide; that portion condensed at −180 degrees centigrade could contain, in addition to these, hydrocarbons from propane to ethane. The gas not condensed at −180 degrees centigrade was found to contain mainly hydrogen, methane, some carbon monoxide and some nitrogen.

HYDROPHIL CONTENT

In these bombardment studies, it was of utmost importance to prevent oxidation of the samples, particularly during the bombardment period. In order to detect whether air had leaked into the cell during a bombardment period, the total oxidation products in a sample before and after bombardment were determined in practically all test runs. The hydrophil test as described by Wyatt, Spring, and Fellows[18] was used for this purpose. Results of hydrophil determinations made during the tests on the ten hydrocarbons of known type molecular structure are given in table IV, and it is evident from the results that there were no significant increases in hydrophil content. It should be pointed out, in fact, that decreases in hydrophil content were observed for two of the compounds tested.

VISCOSITY

The viscosity of an oil is another characteristic which is of importance in the manufacture of cables. It was expected that this characteristic would be affected by corona discharge and measurements of viscosity were made in the case of a few representative compounds. The results are shown in parts *A* to *J* of figure 13; the two curves of each part represent viscosity-temperature characteristics before and after bombardment. It is quite evident that there are three groups of compounds with respect to the magnitude of the viscosities. In the lowest viscosity group are hexadecane (*A*), hexadecene (*B*), decalin (*G*), alpha-methylnaphthalene (*I*), and tetralin (*J*). In the medium viscosity group are the unsaturated and the hydrogenated oils number 4530 (*E*) and number 4530*a* (*C*), and also the turpentine polymer (*H*). In the highest viscosity group are the unsaturated and the hydrogenated oils number 4555 (*F*) and number 4555*a* (*D*). It is of interest to note that despite these differences in viscosity, nearly all compounds suffered a substantial increase in viscosity when subjected to corona discharge. The only exceptions were hexadecane (*A*) and decalin (*G*). Increases in viscosity were also obtained in the tests in which cable compounds were used.

MISCELLANEOUS

A number of determinations other than those already mentioned were made on some of the compounds, particularly on decalin. This compound was singled out because it was particularly suitable for certain tests, and because it was expected that most of the findings regarding changes in decalin with bombardment would be capable of generalization with respect to other pure hydrocarbons.

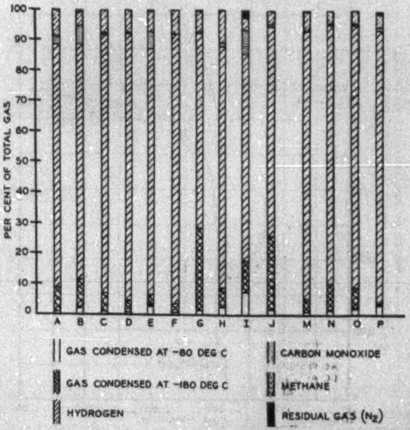


Figure 12. Composition of gas evolved during bombardment of ten hydrocarbons of known type molecular structure and four cable compounds

Bombardment of four hours at 15 kv, 60 cycles. Letters refer to designation of compounds (table II)

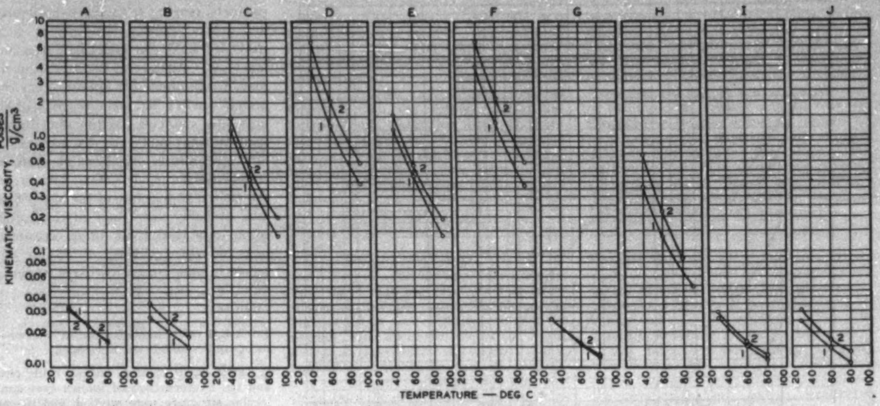


Figure 13. Viscosity-temperature characteristics of ten hydrocarbons of known type molecular structure

1—Before bombardment
2—After bombardment at 15 kv, 60 cycles, for four hours
Letters *A* to *J* refer to designation of compounds (table II)

Iodine-number determinations were made in order to investigate changes in the degree of saturation due to corona discharge. It was found that the iodine number of decalin increased considerably with bombardment. In the preliminary tests of the studies conducted with cell 1 on a cable compound and on pure liquid paraffin, it was observed that the iodine and sulphuric-acid tests indicated an increase in the content of unsaturated components as a result of corona discharge.

Since it was found that the bombarded hydrocarbons contained small amounts of compounds of high boiling points, and since the viscosity of the samples increased due to corona discharge, attempts were made to determine the molecular weight of these larger molecular aggregates. In the case of decalin, vacuum distillation at room temperature and at 100 degrees centigrade, and selective solvation were resorted to. It was found that nearly all of the sample distilled at room temperature in a good vacuum; this material was composed primarily of decalin (molecular weight 138), but also contained some unsaturated products. The part of the residue which distilled at 100 degrees centigrade had an average molecular weight of 198. The material remaining after this distillation was a viscous amber oil of an average molecular weight of 429. It is probable, however, that this residue consisted of a series of polymers of different molecular weights. A portion of this residue was dissolved in benzene and precipitated with isopropyl alcohol. After several such treatments, a white powder was obtained which had an average molecular weight of 1,600. Although it is probable that some of the residue is of a still higher order of polymerization, it is noteworthy that most of the polymerized product which is formed from decalin by bombardment and which seems to be responsible for the increase in power factor of decalin with bombardment is of so relatively low an order of polymerization.

Table IV. Hydrophil Values of Various Hydrocarbons Before and After Bombardment

	Per Cent Hydrophils	
	Before Bombardment	After Bombardment
Hexadecane (*A*)	0.006	0.031
Hexadecene (*B*)	0.033	0.20
4530 (hydrogenated) (*C*)	2.0	0.96
4555 (hydrogenated) (*D*)	0.005	0.021
4530 (*E*)	3.9	1.9
4555 (*F*)	0.11	0.14
Decalin (*G*)	0.002	0.025
Turpentine polymer (*H*)	0.16	0.17
Alpha-methylnaphthalene (*I*)	0.000	0.036
Tetralin (*J*)	0.019	0.026
Liquid paraffin (*K*)	0.007	0.15

DISCUSSION

Some of the characteristics of the oil samples which have been investigated are closely interrelated. An example of this is found in the effect of viscosity on the variation of power factor and conductivity of oils at different temperatures, and this effect cannot be ignored when oils of different viscosities are being compared. It is generally recognized that both power factor and conductivity depend not only on the number of ions present, but also on their mobility, which in turn is affected by the viscosity of the sample. In figure 14 are shown power factor-kinematic viscosity characteristics of a number of samples before and after bombardment. It can be seen that in most cases these characteristics are nearly straight lines. With very few exceptions, the slope (power factor to viscosity) of these lines is in the neighborhood of −1, indicating that the power factor values are nearly inversely proportional to the kinematic viscosity; that is, the change in power factor with temperature can be practically accounted for by the corresponding change in viscosity.

Having thus added to the power factor-temperature characteristics the more fundamental relation of power factor versus viscosity, a better criterion is available for comparing various oils with regard to their ability to withstand corona bombardment as judged by power factor change. It can now be seen that a group of six oils, namely, hydrogenated oil number 4530*a* (C_1), liquid paraffin (K_1), oil number 4530 (E_1), oil number 4555 (F_1), turpentine polymer (H_1), and the hydrogenated oil number 4555*a* (D_1) rate nearly the same before bombardment. After bombardment, however, the turpentine polymer (H_2) and the oil number 4530 (E_2) are considerably better than the remaining four oils of the group mentioned. Similar attempts

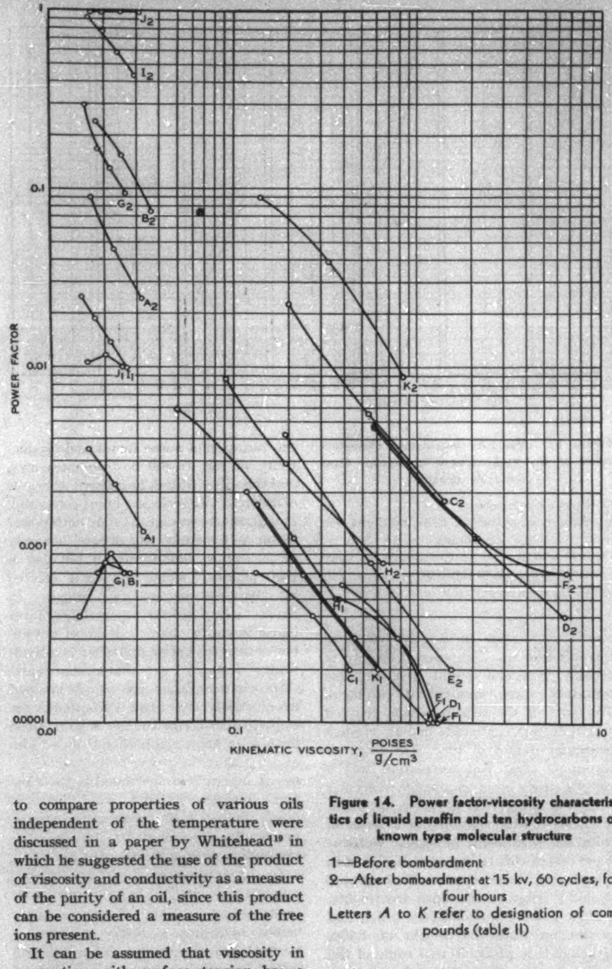


Figure 14. Power factor-viscosity characteristics of liquid paraffin and ten hydrocarbons of known type molecular structure

1—Before bombardment

2—After bombardment at 15 kv, 60 cycles, for four hours

Letters *A* to *K* refer to designation of compounds (table II)

to compare properties of various oils independent of the temperature were discussed in a paper by Whitehead[19] in which he suggested the use of the product of viscosity and conductivity as a measure of the purity of an oil, since this product can be considered a measure of the free ions present.

It can be assumed that viscosity in connection with surface tension has a decided influence on the bombardment current and the gas evolution of an oil under corona discharge, by affecting the size and number of gas bubbles in the oil in which the bombardment takes place. It is also recognized that the molecular structure of the hydrocarbon has a considerable influence on the amount of gas evolved. Discussing the generation and absorption of gas in insulating oils under the influence of an electric discharge, Nederbragt[15] suggested the addition of ten per cent or less of aromatics to cable oils in order to decrease their gas-producing tendency. In view of the results of the present studies it appears important, however, to ascertain to what extent this addition affects the other characteristics of the oil in relation to corona discharge. It would be of importance, for instance, to know the effect of this admixture on the power factor change of the oil with bombardment.

Under the influence of corona discharge, a great number of reactions take place in a hydrocarbon oil, the reaction products ranging from gases to liquids and solids. Similar observations were made by Lind and Glockler,[20] Harkins and Gans,[21] Austin and Black,[22] Sommerman,[23] and others. It appears from the present studies that some reaction products are changing continually during bombardment and even for some time after the sample has been exposed to corona discharge, depending largely on the molecular structure of the oils. This fact, probably more than anything else, seems to be responsible for an occasional lack of reproducibility of results observed in these corona-discharge studies.

Summary

1. A test cell suitable for use in bombardment studies on oils was developed.

2. Pure liquid paraffin, various cable oils, and ten hydrocarbons of known type molecular structure were studied, and it was found that a number of their characteristics are changed by corona discharge.

3. The power factor of all samples increased with bombardment.

4. The one-minute d-c conductivity of bombarded samples was in most instances considerably lower than the a-c conductivity calculated from power-factor values.

5. Application of d-c potential to bombarded decalin for extended periods showed that the power factor and conductivity of the samples were thereby decreased considerably, the improvement being maintained only during and shortly after application of the potential.

6. The material responsible for the changes in power factor and conductivity, and produced during the bombardment, constitutes an extremely small portion of the bombarded oil. It consists of material which remains as residue during distillation of the bombarded oils at the temperatures and pressures used in these studies.

7. Currents resulting from application of identical a-c bombardment voltages to the cell were widely different for the various oils.

8. The gas evolution of the various oils under bombardment was widely different, generally more gas being obtained from the saturated than from the unsaturated compounds.

9. In practically all cases an increase in viscosity resulted from the bombardment of the oils.

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Discussion

L. J. Berberich (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): As the authors have pointed out, cable engineers have recognized for some 20 years that corona discharge in cable voids affects oil-impregnated paper insulation. However, except for the formation of a waxy product which came to be known as "*X* wax" or "cable cheese," the nature of the reaction was not at all understood for a considerable length of time. It remained for the chemists, and logically so because this is largely a chemical problem, to throw important light on the behavior of hydrocarbons when subjected to gaseous electric discharge. The paper by Schoepfle and Connell published in 1929 (reference 7 of the paper) was the first, I believe, to show conclusively that the saturated highly refined petroleum oils are more strongly affected by cathode rays as well as corona discharge than the lightly refined, unsaturated oils. This has been of great practical significance and was in general agreement with the results of an investigation on a series of pure hydrocarbons given in a companion paper (Schoepfle and Fellows, *Industrial and Engineering Chemistry,* volume 23, 1931). Apparently these two papers inspired much of the work that followed.

More recently, Nederbragt (reference 15) and I (reference 16), working independently, have found that the addition of aromatic compounds to cable oils resulted in marked lowering of the gas evolution. Before the present paper appeared, the origin of the increase in power factor and dielectric loss in bombarded oils was completely obscure. The authors have presented excellent evidence that the loss is caused by the high-molecular-weight polymerization products formed. This and the demonstration that these products are charged represent an important addition to our knowledge of this phenomenon.

Most of the investigators of this problem have used the volume of gas evolved as criterion of stability. The authors have used this as well as power factor change. There seems to be only a very approximate relationship between them. An important exception is found in alpha-methylnaphthalene which gave off the least gas yet showed a significant increase in power factor. I have observed in my work in a qualitative way that aromatic compounds, of which alpha-methylnaphthalene is an example, give off little gas, but may produce considerable quantities of high-molecular-weight products. Since the high-molecular-weight products have been shown to be the cause of the power factor increase, perhaps this is the reason why aromatic compounds do not appear to be very stable from the power-factor standpoint. I would appreciate the comments of the authors upon this point and would like to ask if any of the solid products isolated from a bombarded compound were added to an unbombarded oil or compound in order to determine the effect on power factor.

The statement in the paper that viscosity in connection with surface tension influences the gas evolution is in agreement with my experience. The action of the discharge can take place only at the gas-oil interface. Viscosity and surface tension determine the size of the bubbles formed and hence the area of the effective corona. For this reason it is difficult to compare materials of widely different viscosities. The surface-tension variations are usually small. This work perhaps would have been more valuable if compounds and oils of the same viscosity had been chosen. The difficulty in doing this, however, is appreciated.

Charles F. Hill (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): A rather interesting phase of the results reported by Sticher and Thomas is found in table III and involves the ratios of a-c to d-c conductivities as affected by bombardment of oil. High initial ratios decrease appreciably while low ratios increase with bombardment. In most cases the conductivities increased appreciably but one case involves no change in conductivity (number 111 oil) and in the last case of the table for number 113 oil, the d-c conductivity decreased by ten times. It would appear that these data indicate possibilities of investigating the nature of the electric carriers; that is, are they polar or electrolytic, etc. It occurs to me that the d-c carriers might be isolated by electrical conduction through porous membranes.

Herman Halperin (Commonwealth Edison Company, Chicago, Ill.): This paper refers to the effects of corona discharge such as takes place frequently in solid-type high-voltage cables. The measure of this discharge is known as ionization factor, that is, the increase in power factor in going from a given low stress to a given high stress, the latter being considerably above the operating stresses. In some accelerated aging tests in Chicago on three-conductor 13-kv belted-type cable it was found for some cables that, whereas the ionization factor at room temperature after the cable had been heated to 100 or 115 degrees centigrade was 0.010 or 0.015, the ionization factor after several subsequent heating cycles to a more normal value, that is, 60 degrees centigrade, was less than 0.005 in many cases. In other words, the distribution of compound seemed to change, with the result that there was considerable recuperation.

When it became advisable in this country about 18 years ago to change from rosin-impregnated cables to cables impregnated with lower-loss compounds, in many cases the utilities did not have to wait long to learn the quality of the cable because there were frequent failures. Examination of these failures showed some wax formation and other signs of deterioration in many cases. Since that time the quality of cable insulation has improved very greatly. In the case of cable made, however, between 1926 and 1930 we have found wax formation, and sometimes carbonization, in cable removed from normal service. This cable, however, has had an extremely low rate of failures attributable to the quality of the insulation. The question then arises: What is the life and reliability in service to be for such cable made in the past 12 years where some deterioration has already developed?

It is interesting to note that the so-called cable compound performed outstandingly well in the bombardment tests.

Figure 12 indicates that in some cases five or ten per cent of the gas is carbon monoxide. In view of the precautions taken by the authors to prevent the presence of oxygen in their test apparatus, the presence of carbon monoxide is inexplicable.

F. M. Clark (General Electric Company, Pittsfield, Mass.): The paper by Sticher and Thomas is to be welcomed as demonstrating once more the great dependence of dielectric engineering on the molecular and chemical phenomena. One can no more speak of the dielectric properties of liquid insulation without clearly describing the fundamental chemical characteristics of the liquid under discussion. In this respect it is unfortunate that a clearer description of the cable compounds used in the paper is not given. The reference to Doctor Whitehead's work helps but little for the compounds are not clearly described in the reference. Aside from the viscosity, and the other oil properties usually given, such as flash, fire, and pour points, the oils are merely described as highly refined or refined paraffin, naphthenic, or paraffin blend oil. In view of the loose usage of these terms during the past few years, this terminology yields but little information. Whitehead, however, does clearly define the oils involved as specially prepared for the work described and not identical with the oils used in commercial cable operation. The high pour-point values for the thin low-viscosity cable oils given by Whitehead (oils number 111 and number 113) clearly indicate a fundamental difference between those oils and those generally used commercially.

The work in my own laboratory is in general agreement with the results of the present paper, as are the generally accepted ideas of the art. Bombardment of saturated paraffinic hydrocarbons evolves more gas than the bombardment of the aromatic and unsaturated hydrocarbons. The more volatile constituents of the bombarded material are characterized by better power factor properties than the nonvolatile residue. In most instances with which I am acquainted the effect is chiefly a pyrogenous decomposition. The more volatile components of the bombarded material are the saturated hydrocarbons of good dielectric properties. The less volatile or nonvolatile are the cracked and polymerized residues. Tests with which I am acquainted tend to indicate that the final product of the bombardment, the *X* wax, is of good dielectric properties. It is the intermediate series of products in various stages of polymerization, especially of unsaturation, which contribute to the higher dielectric losses reported by the authors.

It must be remembered that except in a very general sense, there is no one chemical description that applies to the pyrolysis of organic compounds as varying in structure as those of this paper. In a general sense, the pyrolysis products are unsaturated and tend toward a deeper color than that of the parent molecule. This deepening in color normally is associated with changes in the structure of the hydrocarbons present which also are accompanied by higher dielectric loss. The authors do not describe color changes but presumably they were present with an efficient and decomposition-free distillation of the bombarded product, my own experience has been that the color-producing molecules accumulate in the still residue and contribute to its higher dielectric losses as compared to the colorless distillate. I request from the authors a statement as to the color of their bombarded products and ask whether any refining or decolorization treatment was applied to remove these colored products. If so, what effect on the viscosity and dielectric loss was observed by this decolorization or refining treatment?

The relation between viscosity and power factor as described in figure 14 is of interest and doubtless of significance within a single chemical class of pure organic compounds. The danger lies possibly in the effort to extend such an observation to include all classes of hydrocarbons and hydrocarbon derivatives. Such a step must be made with exceedingly great care.

The authors have indicated that the greatest amount of gas comes from the bombardment of saturated hydrocarbons. Whether this is desirable or undesirable may be a debatable question, as pointed out by the authors, but it is true nevertheless that the saturated water-repelling hydrocarbons are in practical experience generally looked upon as being more dangerous or at least more difficult to handle successfully than the cyclic type of hydrocarbon. But the authors show that in general the cyclic type of hydrocarbon tends to give the higher dielectric loss in the bombarded sample. This is only a general observation, however, for from figure 7 it is to be observed that the turpentine polymer gives a power-factor characteristic of the same order as that observed for the hydrocarbons. This again brings out the important fact that is repeatedly observed in chemico-dielectric studies, it is not safe to make broad generalizations at our present state of knowledge. The chemical structure and peculiar chemical properties of individual compounds within the general classification usually are of an importance which cannot be ignored.

It is true as clearly pointed out by the authors that oxidation effects appear to be eliminated from the results of the present studies. However, bombardment effects produce products of a different chemical type from the parent substance. Aromatic and saturated molecules crack under bombardment and produce unsaturated compounds chiefly of the straight carbon chain type. Such compounds are easily susceptible to change in the dielectric field. Among such changes is a marked susceptibility to oxidation. The use of such products in continued commercial service constitutes a hazard which because of their concentration in localized areas may equal the hazard generally associated with the use of an improperly refined mineral oil. The degree of chemical unsaturation produced as a function of the molecular type bombarded and the chemical and dielectric stability of the bombarded products offer a fertile field for further investigation.

The present paper is confined to a study of the behavior of insulating liquids under bombardment. It has always seemed to me that a problem of equal importance is the determination and comparison of the threshold point at which the gassing of insulating liquids under voltage really begins.

I ask whether the authors have made any attempt to determine the resistance of the various liquids to a chemical change under voltage which results in gas evolution leading to the bombardment effects described in the paper.

Hubert H. Race (General Electric Company, Schenectady, N. Y.): First, it seems to me that the authors should be commended for the careful physical and chemical techniques employed in this work.

Second, I should like to point out a physical measurement which can be used to arrange different liquids in order according to their chemical structure. This property is "specific dispersion" and has the great advantage of being a quick and inexpensive measure of chemical structure in terms of differences in the proportion of aromatic ring and saturated compounds in a liquid hydrocarbon. The specific dispersions of five of the pure chemicals measured by Sticher and Thomas are given by Von Fuchs and Anderson (*Industrial and Engineering Chemistry*, volume 29, 1937, page 319). For these materials I have plotted the correlation between rates of gas evolution and specific dispersion as shown in figure 1. In general, liquids with higher specific dispersions show lower rates of gas evolution. Unpublished data on "stamp capacitor" life tests assembled by H. W. Bousman of our general engineering laboratory indicate a similar correlation such that in general, liquids in the same viscosity range having higher specific dispersions show longer life. It is also significant to point out that only one material (*I*) showed a lower rate of gas evolution than did oil (*P*), which is the nearest in structure to the oil which has had widest use in oil-filled cable in this country.

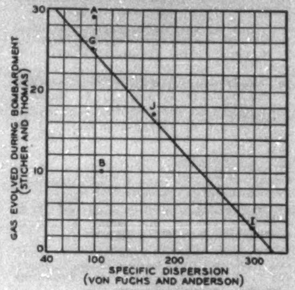

Figure 1. Correlation between specific dispersion and rate of gas evolution under corona bombardment

It is suggested, therefore, that further studies along this line include measurements of "specific dispersion."

Wm. A. Del Mar (Phelps Dodge Copper Products Corporation, Yonkers, N. Y.): Our laboratories have been making tests of oil stability with a modification of the bombardment cell described by G. W. Nederbragt in *Journal* IEE (London) volume 79, 1936, pages 282–90.

The test is made by placing a sample of oil in an evacuated cell and producing, by means of a 60-cycle potential, a glow discharge in the gas remaining over the surface of the oil. The amount of gas evolved, the change in power factor, and the change in resistivity are noted, the stability of the oil being gauged by these three characteristics.

A dimensional drawing of the cell used for this test is shown in figure 2 of this discussion. It consists of a glass cylinder 50 millimeters external diameter and 250 millimeters external length, with hemispherical ends. The thickness of the glass is 1.6 millimeters. It is equipped with external electrodes in the form of tin-foil belts 95 millimeters wide and 28 millimeters apart. A funnel and stopcock provide means for introducing a 20-gram sample of oil. A mercury manometer measures the gas evolution by indicating the change of pressure in the cell. Two such cells are used, in order that each oil may be tested, if desired, in comparison with some other oil.

The electrical circuits are shown in figure 3 which shows two cells, each fed by a 15,000-volt 60-cycle gas-tube sign transformer. A milliammeter is in each cell circuit. An adjustable-ratio autotransformer is used to control the voltage fed to each step-up transformer, so as to enable equal currents to be passed through the two cells.

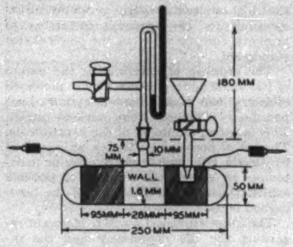

Figure 2. Oil-stability test cell

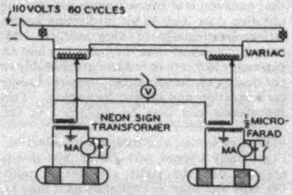

Figure 3. Electrical circuit of oil-stability test set

A diagram of the oil-conditioning equipment is shown in figure 4. Oil is degasified in the cell by dropping slowly into a vacuum of less than 0.1 millimeter mercury from the funnel. The vacuum regulator is then set at an arbitrary value, usually 1 millimeter and the oil left to "condition" overnight at this pressure. Gas, usually CO_2, is admitted through an adjustable capillary leak and dried by passing through a P_2O_5 moisture trap.

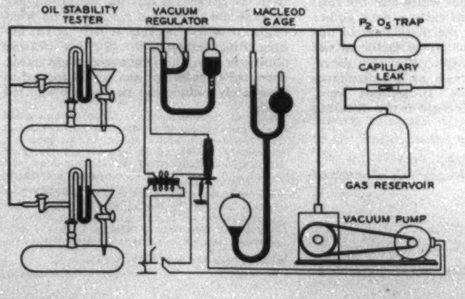

Figure 4. Vacuum system of oil-stability test set

The following precautions have been found necessary to insure reproducible results in comparing oils:

1. The cells and their electrical equipment must be identical.

2. Both the volume and surface area of the oil samples in the cells must be identical.

3. The starting vacuum must be the same.

4. Stopcock leakage must be carefully eliminated preferably by the use of a glycerin base lubricant, insoluble in oil.

5. Milliammeters must be checked carefully before each test. The rectifier type of milliammeter does not retain its calibration in this service, due probably to high-frequency transients. Some help is obtained by shunting each ammeter permanently by a 0.5-microfarad capacitor and by employing a short-circuiting switch by means of which the milliammeter may be kept out of circuit except when readings are being taken. Calibration may easily be made by means of a 100,000-ohm series resistor and a known alternating voltage.

We have found that at 1.0 millimeter pressure approximately 12,000 volts are required to produce a current of 0.8 milliampere through the cell. Viscous oils (solid-type cable oils) seem to require about eight per cent lower voltage to maintain this current than the less viscous oils used in oil-filled cable.

The test, carried out with the above precautions, gives results which are reproducible within about plus or minus five per cent. Blanks, that is, without voltage, show that no change of power factor, within the limits of accuracy of the Schering bridge, occurs as the result of transferring the oil between the Nederbragt bombardment cell and the Berberich measuring cell.

The cell used by Sticher and Thomas has the advantage of permitting continuous curves of gas evolution, power factor, and resistivity to be made, whereas the Nederbragt cell is limited to continuous curves of gas evolution and merely "before and after" reading of power factor and resistivity.

Some of the conclusions reached are as follows:

1. This test produces two types of deterioration of oil. One type is evidenced by increase in gas pressure above the specimen, believed to be caused by partial decomposition of the oil. The other type is evidenced by an increase in power factor and decrease in resistivity of the oil. These two types of deterioration do not necessarily go together. On the contrary, if we except blends containing certain grades of wood rosin, it is usual for oils having the better gassing characteristics to, have inferior power factor and resistivity characteristics, and vice versa. Highly refined water-white oil was poor by both criteria.

2. Oils of high viscosity (solid-type cable oils), in general, showed greatest deterioration by gassing. Oils of low viscosity (oil-filled-cable oils) produced less gas but showed greater power factor and resistivity changes.

3. We confirm Nederbragt's results on the value of added aromatics in reducing gas evolution, but find the practical application difficult.

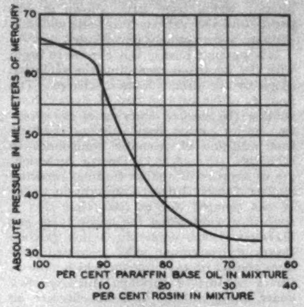

Figure 5. Oil-stability tests-gassing under glow discharge

Run of 1,400 minutes

Temperature = 30 degrees centigrade

Initial pressure = 1.0 millimeter of carbon dioxide

Sixty-cycle current = 0.80 milliampere

4. The substitution of CO_2 for air as the medium in the discharge cell has little or no effect on the results.

5. Viscous oils as used in the solid-type cables are intermediate in gassing between water-white oil and oil-filled-cable oil.

6. Power factor increases occur without increase of hydrophil number.

7. There is little difference in gassing stability between rosin-free paraffin base and rosin-free naphthene base oils, as used in solid-type cables.

8. Wood rosin added to paraffin base oil adds greatly to the gassing stability, as shown in figure 5 of this discussion.

9. The effect of wood rosin on power factor stability is less easy to evaluate because there are four effects to consider, as follows:

(*a*). Effect of wood rosin on magnitude of power factor.

(*b*). Effect of wood rosin on power-factor temperature relation.

(*c*). Effect of wood rosin on each of above after bombardment in the Nederbragt cell.

(*d*). Effect of degree of type or purity of wood rosin on each of above three.

It is impossible in the space of this discussion to report each of these four items, except in the briefest way.

In general, wood rosin increases oil power factor, but whereas in some grades the effect is very great, in others it is almost negligible.

Some grades of rosin increase and some decrease the rise of power factor with temperature.

Some grades of rosin increase and some decrease the effect of bombardment on power factor.

The test has been exceedingly useful in enabling improved grades of rosin to be selected.

We hope to be able to give more specific details of the various rosins at some later date. Acknowledgments are due to J. H. Palmer and E. T. Merrell for their assistance in the preparation of this material.

Joseph Sticher and D. E. F. Thomas: The authors are grateful to the discussers for the interest which they have shown in this paper, and the various questions and suggestions are genuinely appreciated.

It is perhaps best in this closure to begin with the questions regarding the evolution of gas by oils under corona discharge. The findings of Nederbragt and of Berberich regarding the marked lowering of gas evolution under corona bombardment resulting from addition of aromatic compounds to cable oils, referred to by Doctor Berberich, are in support of similar findings reported by The Detroit Edison Company in 1929.[1] It was pointed out at that time that a method of controlling the quantity of gas liberated within cables in which gaseous electric discharge can occur suggested itself from results of experiments which showed that a mixture of paraffins and aromatics under bombardment does not liberate as much gas as would be released by the paraffins alone.

Regarding the relationship between power factor increase and gas evolution of oils under corona bombardment which was touched upon by Doctor Berberich, it can be stated that a review of the information available in our files did not reveal such a relationship. This statement is essentially in agreement with the conclusion drawn by Doctor Berberich.

The question by Mr. Halperin regarding the presence, in some cases, of five or ten per cent of carbon monoxide in the gas obtained from bombardment, as shown by figure 12, is very much appreciated since it provides an opportunity to include here a bit of information which might well have been given in the paper. It will be noted, by referring to figure 11, that in all cases in which the reported percentage of carbon monoxide was large, the total amounts of gases evolved were relatively small.

It should be explained that there is a possibility that the extremely small amount of gas reported to be carbon monoxide may have been largely hydrogen which was not removed by the palladium tube diffusion method used to determine the amount of hydrogen in the gas sample. In the gas analysis method used in these tests and described fully in reference 17 of the paper, the gas which is not removed by the palladium tube is subsequently passed into a quartz tube containing cupric oxide at 250 degrees centigrade. This step in the procedure determines the amount of carbon monoxide in the gas sample. It is apparent that any traces of hydrogen not removed by the palladium tube will be burned in the cupric-oxide tube at 250 degrees centigrade and will be reported as carbon monoxide. It will be appreciated that in analyses of very small gas samples a considerable percentage error is usually encountered.

The correlation between specific dispersion and rate of gas evolution under corona bombardment which is shown in figure 1 of the discussion by Doctor Race is extremely interesting. It would seem that, if this correlation could be substantiated by a great number of results from bombardment tests on pure hydrocarbons, such a relation should prove very helpful in the selection of oils for use in the manufacture of oil-impregnated paper-insulated cables.

The tests of oil stability which Mr. Del Mar reported are of considerable interest, particularly those in which the effects of rosin, added to oils, are discussed. With regard to bombarded oils, we have found that they exhibit an increased affinity for oxygen, and for this reason, as well as to prevent other possibilities of contamination, we considered it important in our tests to transfer test samples from the bombardment chamber to the power-factor cell, and vice versa, under vacuum. As a further precaution against possible contamination we refrained from the use of a mercury manometer on the bombardment chamber.

A number of the questions raised in the discussion were concerned with the material of higher molecular weight formed under corona discharge of oils and with the nature of the increased dielectric loss. A considerable amount of experimental work on this phase of the problem has been conducted by The Detroit Edison Company. The inclusion of the results of that work was considered as being beyond the scope of the present paper; these results were therefore scheduled to be the subject of a separate paper in the near future. The questions which have been raised, however, can best be answered as follows.

In a number of cases, bombarded oils were distilled in order to concentrate the polymerized materials. In answer to Doctor Clark's question regarding color changes, it can be stated that in the cases in which water-white oils had been bombarded, the bombarded oils exhibited a more or less yellow tinge. Upon distillation, the distillate was again water white and the residue exhibited in some cases a dark amber color. In all cases, the distillate had approximately the same power factor as the oil before bombardment. Viscosity determinations were not made on the distillates from bombarded oils. Indications were, however, that in other respects this distillate was closely the same as the material before test, and it can be expected that this is also true for the viscosity.

The solid products (white powder) isolated from a bombarded compound, which Doctor Berberich inquired about, were added to unbombarded compound of the same kind from which the solid material was obtained. It was found that a very pronounced increase of power factor values resulted from this addition. While tests of this nature were not made on aromatic compounds, there seems to be good reason to concur in Doctor Berberich's suspicion that high-molecular-weight products are responsible for the apparent instability of aromatic compounds from the power-factor standpoint.

The problem of determining the nature of the material responsible for the increased dielectric loss of bombarded hydrocarbons has been attacked from various angles. This included a number of d-c experiments. The use of porous membranes, as suggested by Doctor Hill, should prove of value in attempts to reduce the number of possible explanations for the behavior of the bombarded liquids.

The question raised by Mr. Halperin regarding the life expectancy and the reliability in service of cable in which some deterioration has already developed is of considerable interest. A definite answer to that question is of course impossible, but the question does lead to an interesting speculation, particularly in view of Mr. Halperin's statement that the cable has had an extremely low rate of failures attributable to the quality of the insulation, in spite of some carbonization and of wax formation. This speculation can best be explained in the following manner.

Wax in cables, resulting from ionization, is generally viewed with alarm, but this is possibly not the right attitude to take. Perhaps wax may be desirable, as it may be looked upon as the result of a natural self-defense of a cable against ionization occurring or continuing within the cable. If wax is readily formed, a protective coating will be produced over all surfaces of the voids in a short time. This coating decreases the volume of the voids, and with subsequent loading cycles additional polymerization products will be formed which add to the amount of wax in the voids. In this manner, ionization and resulting waxing can eventually lead to a reduced size of the affected voids which will not allow any further ionization to occur. Whether or not a cable can save itself in this manner from the ravages of internal ionization may be expected to depend essentially on what might be called "waxing ability" of the impregnating compound. If this waxing ability is low, the insulation might break down before sufficient wax can be formed to adequately reduce the size of the affected voids.

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[1] Hirshfeld, Meyer, and Connell, Minutes of the Forty-Fifth Annual Meeting of the Association of Edison Illuminating Companies, 1929, page 464.

Sensitivity of the Four-Arm Bridge

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MEMBER AIEE
(Deceased)

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THE rapid increase in the use of a-c bridges for the measurement of electrical and nonelectrical quantities makes desirable a critical analysis of the performance characteristics of various types of bridge structures. The literature is replete with descriptions of multitudinous bridges and there is a great mass of analytical treatment and experimental data available concerning bridges designed for special purposes. The selection of a particular type of bridge is dictated by many conditions, the more important of which are:

(*a*). Sensitivity requirements

(*b*). Accuracy requirements

(*c*). Magnitude and characteristics of quantity to be measured

(*d*). Voltage and current requirements

(*e*). Frequency

(*f*). Availability of bridge components

Cases frequently arise when the last three considerations offer no obstacles and the selection of the bridge on the basis of the first three considerations is far from apparent. This is also true in the design of the so-called "universal" bridges which are employed for the measurement of several quantities over a wide range of values. Hence, it is recognized that different quantities and different ranges are most expeditiously measured by different bridges, and switching arrangements are incorporated for the transition from one type to another. An important question requiring a definite answer in this connection is, at what value of the unknown shall one type of bridge be switched into another type? This often involves considerable computation over a wide range of values and ratios before a satisfactory solution is obtained. A typical example of the amount of work necessary for a bridge design was presented to the Institute by Edwards and Herrington.[1]

The paucity of material treating sensitivity as related to the kind and magnitude of quantity being measured is surprising. Fischer[2,3] has given a general treatment of the problem, but his results are not in a form conducive to convenient comparison of various types of bridges. The sensitivity studies of Schering[4] apply only to the Schering bridge. Several years ago the first of the authors[5] presented a method whereby the locus of potential difference across the galvanometer terminals could be conveniently plotted in the form of a circle or in more complicated cases as the sum of several circles. A subsequent publication[6] demonstrated the applicability of the same method to the determination of galvanometer current.

The present paper treats the general problem of the unbalanced voltage appearing across the galvanometer terminals of any four-arm network as a function of the impedances of the arms in such a way as to show directly the effect of a slight change off the balance point for any of the bridge arms. The determination of sensitivity in connection with the open-circuit potential difference across the galvanometer terminals will be considered in this presentation. This problem is of greater interest than that of galvanometer current in view of the wide application of amplifiers operating into null detectors. Furthermore, the addition of a fifth arm involving galvanometer impedance brings in another variable which has no inherent relationship to the bridge structure and to a certain extent complicates comparison of various types of bridges.

General Four-Arm Bridge

The general structure of a four-arm bridge network is given in figure 1. The impedance arms, Z_1, Z_2, Z_3, Z_4, are assumed to be linear and bilateral. No mutual reactions between arms are considered to exist. The applied voltage E to the bridge is assumed to be constant. The unbalanced voltage across the galvanometer terminals is e; this voltage is given by:

$$e = E\,\frac{Z_1Z_3 - Z_2Z_4}{(Z_1 + Z_2)(Z_3 + Z_4)} \tag{1}$$

It is more convenient to deal with the unbalanced voltage e' produced by unit potential across the bridge or

$$e' = \frac{e}{E} = \frac{Z_1Z_3 - Z_2Z_4}{(Z_1 + Z_2)(Z_3 + Z_4)} \tag{2}$$

The sensitivity of a bridge structure with reference to any particular arm is the unbalanced voltage produced across the galvanometer terminals when the balancing arm is altered slightly from the true balance position. Any arm may be chosen as the balancing arm, say Z_3.

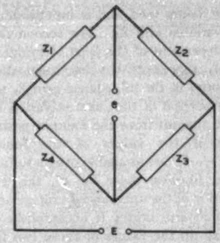

Figure 1. The general four-arm bridge

Let Z_3 be the correct value of this arm for balance, and let it change by a small amount to $Z_3 + dZ_3$. The change in the denominator on account of a slight variation of Z_3 is negligible because it enters as a small part of a sum. Hence:

$$e' = \frac{Z_1 \cdot dZ_3}{(Z_1 + Z_2)(Z_3 + Z_4)} \tag{3}$$

For a fair comparison of sensitivity of various bridges the slight unbalance in Z_3 should be expressed as a fraction of Z_3. Let this fractional change be denoted by

$$\sigma' = \frac{dZ_3}{Z_3} \tag{4}$$

To render the equation more tractable let the complex ratio of Z_1 to Z_2 be A.

$$A = \frac{Z_1}{Z_2} \tag{5}$$

Then at balance:

$$\frac{Z_1}{Z_2} = \frac{Z_4}{Z_3} = A \tag{6}$$

Introducing these substitutions the unbalanced voltage is:

$$e' = \frac{A}{(1 + A)^2} \cdot \sigma' \tag{7}$$

The convenience of this expression lies in the fact that it consists of two independent factors. The first factor $A/(1 + A)^2$ may be called the "bridge factor" in that it depends on the type of

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Paper number 39-23, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 19, 1938; made available for preprinting December 14, 1938.

A. C. SELETZKY was associate professor of electrical engineering at Case School of Applied Science, Cleveland, Ohio, at the time of his death, May 11, 1938; L. A. ZURCHER is instructor in electrical engineering at the University of Kansas, Lawrence.

[1] For all numbered references, see list at end of paper.

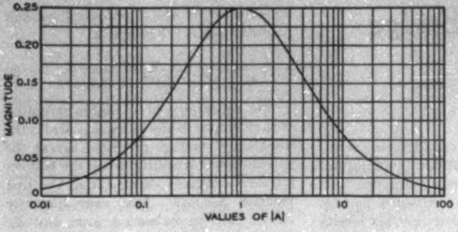


Figure 2. Magnitude of bridge factor

Curve of $A/(1+A)^2$ for $\theta = 0$ degrees $A = Z_1/Z_2 = |A| \underline{/\theta}$

bridge being used and is independent of the variable arm Z_3. The second factor is independent of the type of bridge and merely represents the fractional change produced in the impedance arm Z_3 when some portion of that arm is varied by a slight amount from the balance position. Thus, if the values of the function $A/(1+A)^2$ are available over a sufficient range, likewise those for σ', the determination of the unbalanced voltage across any four-arm bridge is accomplished by looking up the values of these two functions and taking their product.

The Function σ'

The function σ' which represents the fractional variation of impedance in the arm which is upset slightly from the balance position depends upon the manner in which this unbalance is produced. The more important cases arising in practice are:

(*a*). Series impedance arm
  1. Resistance and inductance
  2. Resistance and capacitance

(*b*). Parallel impedance arm
  1. Resistance and capacitance

In all these cases the ultimate variable to be considered is the variation of either in-phase or quadrature component. Hence, the function σ' will be studied in terms of a variable σ which will be $(dR)/(R)$, $(dL)/(L)$, or $(dC)/(C)$. In this way it is possible to obtain the fractional impedance variation σ' as a function of a slight change in any one component of a bridge arm. To increase the generality of the results, Q will be used to represent the ratio of reactance to resistance of the impedance arm. The results for these combinations are given in table I. The form for σ' in each case when the resistance is varied is:

$$\sigma' = \frac{\frac{dR}{R}}{1 \pm jQ} = \frac{\sigma}{1 \pm jQ} = \sigma \cdot \cos \theta \underline{/\mp\theta} \quad (8)$$

where θ is the phase angle of the impedance arm. The maximum value of σ' occurs when $\theta = 0$, $Q = 0$; where the impedance is a pure resistance.

Likewise, the form for σ' in each case where the reactive component is varied is:

$$\sigma' = \frac{\pm\sigma}{1 \pm \frac{j}{Q}} = \pm\sigma \cdot \sin \theta \underline{/90^\circ - \theta} \quad (9)$$

Here the maximum value of σ' occurs when $\theta = \pm 90$ degrees, $Q = \infty$, where the impedance is a pure reactance.

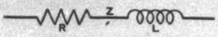
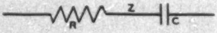
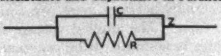
Obviously, the maximum value of σ' occurs where the direction of the change in impedance coincides with the impedance vector and this can only occur for pure reactance and for pure resistance arms. Inasmuch as the fractional upset off balance σ in the variable impedance arm expressed as a fraction of portion of the arm in which it occurs is usually taken at a small convenient value such as 0.001 or 0.0001, the determination of σ' is accomplished by merely multiplying the sine or cosine of the impedance phase angle by this fractional upset, σ.

The Function $A/(1 + A)^2$

"RESONANT" TYPE BRIDGE

The type of bridge is characterized by the "bridge factor" $A/(1+A)^2$. Inasmuch as A is a complex ratio of two passive impedances, its magnitude can vary from zero to infinity and its argument from $\mp$180 degrees to $\pm$180 degrees. These limiting values of angle are obtained when the impedances are pure inductive and pure capacitive reactances respectively. Thus, if the bridge arms are alternately composed of inductive and capacitive impedances, the sensitivity of the bridge approaches infinity as

Table I. The Function $\sigma' = dZ/Z$ for Various Impedances

Resistance and Inductance in Series		
 Variation of R, $\sigma = \frac{dR}{R}$	$Z = R + jwL$	$Q = wL/R = \tan\theta$ Variation of L, $\sigma = \frac{dL}{L}$
$\sigma' = \frac{\sigma}{1 + jQ} = \sigma \cos\theta \underline{/-\theta}$		$\sigma' = \frac{\sigma}{1 - j/Q} = \sigma \sin\theta \underline{/90^\circ - \theta}$
σ' increases as Q decreases		σ' increases as Q increases
$\sigma'_{\max} = \sigma = \frac{dR}{R}$ at $Q = 0$		$\sigma'_{\max} = \sigma = \frac{dL}{L}$ at $Q = \infty$
Resistance and Capacitance in Series		
 Variation of R, $\sigma = \frac{dR}{R}$	$Z = R - j/wC$	$Q = 1/wCR = -\tan\theta$ Variation of C, $\sigma = \frac{dC}{C}$
$\sigma' = \frac{\sigma}{1 - jQ} = \sigma \cos\theta \underline{/\theta}$		$\sigma' = \frac{-\sigma}{1 + j/Q} = \sigma \sin\theta \underline{/90^\circ + \theta}$
σ' increases as Q decreases		σ' increases as Q increases
$\sigma'_{\max} = \sigma = \frac{dR}{R}$ at $Q = 0$		$\sigma'_{\max} = -\sigma = -\frac{dC}{C}$ at $Q = \infty$
Resistance and Capacitance in Parallel		
 Variation of R, $\sigma = \frac{dR}{R}$	$Z = R/(1 + jwCR)$	$Q = wCR = -\tan\theta$ Variation of C, $\sigma = \frac{dC}{C}$
$\sigma' = \frac{\sigma}{1 + jQ} = \sigma \cos\theta \underline{/-\theta}$		$\sigma' = \frac{-\sigma}{1 - j/Q} = -\sigma \sin\theta \underline{/90^\circ - \theta}$
σ' increases as Q decreases		σ' increases as Q increases
$\sigma'_{\max} = \sigma = \frac{dR}{R}$ at $Q = 0$		$\sigma'_{\max} = -\sigma = -\frac{dC}{C}$ at $Q = \infty$

the arms are made more and more reactive and equal in value.

Let

$$A = A_d + jA_q \tag{10}$$

Then:

$$\frac{A}{(1+A)^2} = \frac{A_d + jA_q}{(1 + A_d + jA_q)^2} \tag{11}$$

The function equals infinity when $A_d = -1$ and $A_q = 0$ which is the condition described above. For lack of better name this type of bridge network may be called the "resonant" type and is characterized by having no finite limit to its sensitivity other than the physical limitations of obtaining purely reactive arms. The application of the resonant type of bridge has been described by Grover,[7] and is the only four-arm network which allows the ratio A to have a negative real component.

"Wheatstone" and "Quadrature" Types of Bridges

The question of sensitivity may be further simplified by studying the limits of the function $A/(1+A)^2$ as A_d and A_q approach zero separately.

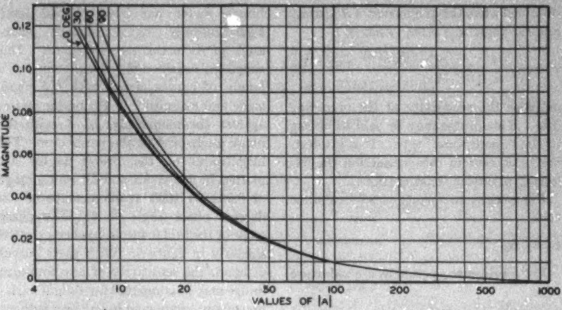


Figure 3B. Magnitude of bridge factor

Curves of $A/(1+A)^2$ for various values of θ
$A = Z_1/Z_2 = |A| \angle \theta$

When the ratio A is of such character that its quadrature component A_q may approach or become zero, the bridge may be said to belong to the "Wheatstone" type.

$$A = \frac{Z_1}{Z_2} = \frac{|Z_1| \angle \theta_{Z_1}}{|Z_2| \angle \theta_{Z_2}} = \frac{|Z_1|}{|Z_2|} \angle \theta_{Z_1} - \theta_{Z_2} \tag{12}$$

where θ_{Z_1}, and θ_{Z_2}, are the phase angles of the respective impedance arms. For A to be real

$$\theta_{Z_1} - \theta_{Z_2} = 0 \text{ degrees} \tag{13}$$

The maximum sensitivity for the real type is obtained when $Z_1 = Z_2 = Z_3 = Z_4$, and the maximum sensitivity becomes

$$e' = \frac{1}{(1+1)^2} \cdot \sigma' = \frac{\sigma'}{4} \tag{14}$$

This is the maximum sensitivity of the Wheatstone bridge, in terms of open-circuit potential difference across galvanometer terminals, with constant applied voltage, and is one of the cases analyzed by Fischer.[8] The fractional unbalance factor in the case of the pure resistance bridge is $\sigma' = (dR)/(R)$ and the maximum sensitivity when all four arms are equal is $(dR)/(4R)$ volts per volt of applied voltage. It is for this reason that when a bridge is operated in such a manner that the ratio A is real, it may be said to belong to the Wheatstone type.

When the ratio A is such that its real component may approach or become zero, the phase-angle relationship of adjoining impedances must be such as to allow approach to, or attainment of, the limiting value

$$\theta_{Z_1} - \theta_{Z_2} = \pm 90 \text{ degrees}$$

in this case the maximum sensitivity is

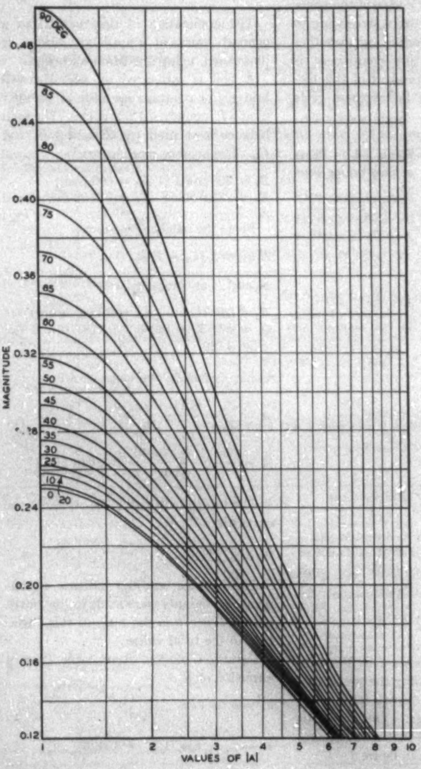


Figure 3A. Magnitude of bridge factor

Curves of $A/(1+A)^2$ for various values of θ
$A = Z_1/Z_2 = |A| \angle \theta$

obtained when the magnitude of A is unity and the angle of A is 90. That is,

$$e' = \frac{j\sigma'}{(1 + j1)^2} = \frac{\sigma'}{2} \qquad (15)$$

or twice the sensitivity of the preceding case. This bridge may be said to belong to the "quadrature" type. It is interesting to observe in this connection the following practical considerations.

For a bridge to operate as a Wheatstone type

$$\theta_{z_1} = \theta_{z_2}$$

For a bridge to operate as a quadrature type

$$\theta_{z_1} - \theta_{z_2} = 90 \text{ degrees}$$

Obviously, it is possible to have the same bridge satisfy both relationships if the arms are varied over a sufficiently wide range. For example, let Z_1 be a pure capacitance and Z_2 be a capacitance and resistance in series. Then as the resistance of Z_2 is reduced, θ_{z_2} approaches θ_{z_1} or 90 degrees and the bridge approaches the Wheatstone type. On the other hand, as the resistance in Z_2 is increased θ_{z_2} approaches 0 degrees and the bridge approaches the quadrature type. This is one of the reasons why a particular bridge is more sensitive for the measurement of an impedance with a high Q and some other type of bridge is more sensitive for the measurement of an impedance with a low Q.

The "bridge factor" $A/(1 + A)^2$ is a function of both the magnitude and phase angle of the complex ratio of impedance arms A. It is a symmetrical function on either side of $A = 1$, as may be seen from figure 2, where the magnitude of the bridge factor has been plotted semilogarithmically for a constant phase-angle of A, in this case 0 degrees. This simplifies considerably the plotting of a family of curves giving both the phase angle and magnitude of the bridge factor, against the magnitudes of A with the phase angle of A as the parameter. The magnitudes of the bridge factor are plotted in figures 3A and 3B over a range of 0.001 to 1000 at increments of ten degrees phase angle of A. The phase angle of the bridge factor has been plotted similarly in figure 4. With these curves at hand, the sensitivity of any four-arm bridge may be computed for a slight variation of any component in any arm without resorting to computations involving complex quantities. Given the values of the impedance arms, it is necessary only to divide Z_1 by Z_2 or Z_4 by Z_3 to obtain A. The bridge factor is thus determined from the curves. Given the fractional upset in the variable arms, the factor σ' is obtained from the sine or cosine of the phase angle of the variable arms, according to equations 8 and 9 depending upon whether the resistive or reactive component is being changed. The product of this factor σ' and the bridge factor then, gives the sensitivity in open-circuit volts across the galvanometer terminals for unit voltage applied to the bridge.

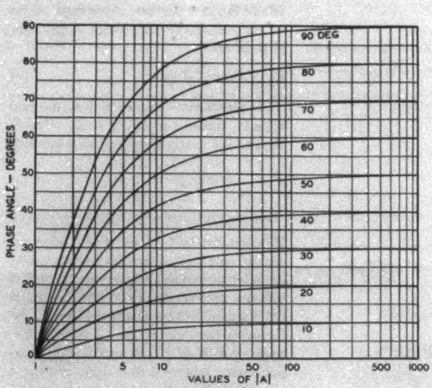


Figure 4. Phase angle of bridge factor

Curve of $\underline{/A/(1+A)^2}$ for various values of θ

$A = Z_1/Z_2 = |A| \underline{/\theta}$

Illustrative Analyses

The following analyses are to illustrate the method of determining the sensitivity of a four-arm bridge network for a given set of balance readings. The subscript notation corresponds to that of the general four-arm bridge shown in figure 1.

First, a pure resistance or Wheatstone bridge is used to measure an unknown resistance, R_1. The balance readings are:

$R_2 = 300$ ohms
$R_3 = 733.8$ ohms
$R_4 = 600$ ohms

From the balance equation:

$R_1R_3 = R_2R_4$
$R_1 = (300)(600)/733.8 = 245.3$ ohms and
$A = Z_4/Z_3 = 600/733.8$

The magnitude of the bridge factor is the same for $1/A$ as for A. Therefore:

$1/A = 733.8/600 = 1.223$

The bridge factor from the curves in figure 3A is 0.2475.

$\sigma' = \sigma = 0.1/733.8 = 0.000136$

Therefore:

$e' = (0.000136)(0.2475) = 33.7$ microvolts per volt

The application of this method to a general four-arm impedance bridge is illustrated, using the Maxwell bridge.

A coil is measured on the Maxwell bridge (for diagram see table II, number 6) at a frequency of 1,000 cycles. The final balance is secured by adjusting R_3 and C_3. The balance readings are:

$R_2 = 300$ ohms $\quad R_4 = 600$ ohms
$R_3 = 733.8$ ohms $\quad C_3 = 0.6598$ microfarad

From the balance equation

$$R_1R_3 = L_1/C_3 = R_2R_4$$

R_1 and L_1 as computed are:

$R_1 = 245.3 \qquad L_1 = 0.1187$ henrys
$Q = wL_1/R_1 = (6283.2)(0.1187)/245.3$
$= 3.040$

Using Z_4 and Z_3, the value of A is:

$$A = \frac{Z_4}{Z_3} = \frac{R_4(1 + jwC_3R_3)}{R_3}$$

$$A = \frac{600\ (1 + j\,3.040)}{733.8} = 2.616\ \underline{/71.8^\circ}$$

The bridge factor $A/(1 + A)^2$ from the curves is

$0.276\ \underline{/-35.6^\circ}$

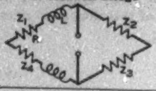
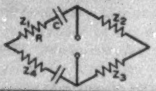
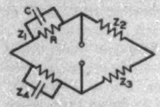
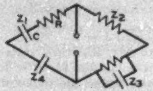
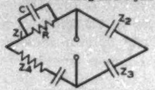
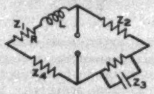
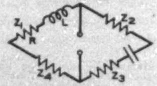
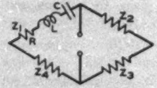
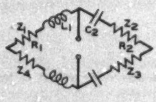
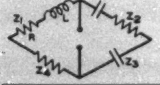
The balance readings indicate adjustments for balance were made in the fourth place. Then σ is the ratio of this variation to the total value.

Variation of R_3: from table I for a variation of R

$\sigma' = \sigma \cos\theta\ \underline{/\theta}$

$$\sigma = \frac{0.1}{733.8} \cos 71.8^\circ = 0.31233$$

Table II. Types of Bridges

1. The Maxwell Bridge		$\theta_{Z_2} = 0°$ Quadrature Type High Q $WL \gg R$ $\theta_{Z_1} \cong 90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 90$ deg Wheatstone Type Low Q $WL \ll R$ $\theta_{Z_1} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
2. The Wien Bridge		$\theta_{Z_2} = 0°$ Quadrature Type High Q $1/WC \gg R$ $\theta_{Z_1} \cong -90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong -90$ deg Wheatstone Type Low Q $1/WC \ll R$ $\theta_{Z_1} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
3. The Parallel Resistance Bridge		$\theta_{Z_2} = 0°$ Quadrature Type High Q $1/WC \ll R$ $\theta_{Z_1} \cong -90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong -90$ deg Wheatstone Type Low Q $1/WC \gg R$ $\theta_{Z_1} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
4. The Schering Bridge		$\theta_{Z_4} = -90$ deg $\theta_{Z_2} = 0$ deg Quadrature Type High Q $1/WC \gg R$ $\theta_{Z_1} \cong -90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong -90$ deg Wheatstone Type Low Q $1/WC \ll R$ $\theta_{Z_1} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
5. The Fleming and Dyke Bridge		$\theta_{Z_2} = -90$ deg $\theta_{Z_3} = -90$ deg Quadrature Type Low Q $1/WC \ll R$ $\theta_{Z_1} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 90$ deg Wheatstone Type High Q $1/WC \gg R$ $\theta_{Z_1} \cong -90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
6. The Maxwell Bridge		$\theta_{Z_2} = 0$ deg $\theta_{Z_4} = 0$ deg Quadrature Type High Q $WL \gg R$ $\theta_{Z_1} \cong 90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 90$ deg Wheatstone Type Low Q $WL \ll R$ $\theta_{Z_1} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
7. The Hay Bridge		$\theta_{Z_2} = 0$ deg $\theta_{Z_4} = 0$ deg Quadrature Type High Q $WL \gg R$ $\theta_{Z_1} \cong 90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 90$ deg Wheatstone Type Low Q $WL \ll R$ $\theta_{Z_1} \cong 90$ deg $\theta_{Z_1} - \theta_{Z_3} \cong 0$ deg
8. The Resonance Bridge		$\theta_{Z_1} = 0$ deg $\theta_{Z_2} = 0$ deg $\theta_{Z_3} = 0$ deg $\theta_{Z_4} = 0$ deg Wheatstone Type Only
9. The Grover Bridge		Any Value of Q $\theta_1 - \theta_2 = \theta_4 - \theta_3$ Resonant Type Kind of Sensitivity at $Z_1 = Z_4 \cong \|Z\| \underline{/+90 \text{ deg}}$ and $Z_2 = Z_3 \cong \|Z\| \underline{/-90 \text{ deg}}$ is infinity Wheatstone Type $WL_1 \ll R_1$ $\theta_{Z_1} \cong 0$ deg $1/WC_2 \gg R_2$ $\theta_{Z_2} \cong 0$ deg $\theta_{Z_1} - \theta_{Z_2} = 0$ deg Quadrature Type For $\theta_{Z_1} - \theta_{Z_2} = \pm 90$ deg
10. The Owen Bridge		$\theta_{Z_3} = -90$ deg $\theta_{Z_4} = 0$ deg $\theta_{Z_4} - \theta_{Z_3} = 90$ deg Quadrature Type Only

and

$$\sigma' = \frac{0.1}{733.8}(0.31233)\underline{/-71.8°}$$
$$= 0.000044\underline{/-71.8°}$$

Therefore,

$$e' = \frac{A \cdot \sigma'}{(1 + A)^2}$$
$$= (0.276)(0.000044)\underline{/-35.6° - 71.8°}$$
$$= 12.1 \text{ microvolts/volt } \underline{/-107.4°}$$

Variation of C_3: From table I for a variation of C

$$\sigma' = -\sigma \sin\theta \underline{/90° - \theta}$$

$$\sigma = \frac{0.0001}{0.6597} \sin 71.8° = 0.94997$$

and

$$\sigma' = \frac{0.0001}{0.6597}(0.94997)\underline{/90° - 71.8°}$$
$$= 0.000144\underline{/198.2°}$$

Therefore,

$$e' = (0.276)(0.000144)\underline{/198.2° - 35.6°}$$
$$= 39.7 \text{ microvolts/volt } \underline{/162.6°}$$

It is to be remembered that this analysis is illustrative only. To make a thorough comparison of bridges and methods of balancing as applied to a given engineering problem, the factors mentioned in the opening paragraph must be considered.

THE METHOD APPLIED TO TEN TYPES OF BRIDGES

In table II is given a summary of the sensitivity analyses of ten types of four-arm bridges. Numbers 1, 2, 3, 4, 6, and 7 will approach either the quadrature type or the Wheatstone type for values of high Q or low Q respectively. Number 5 has the same type sensitivity except for high Q it is of the Wheatstone type and for low Q it is of the quadrature type. Number 8 is of the Wheatstone type only and number 10 is of the quadrature type only. Number 9, the Grover bridge, has theoretically an infinite range of sensitivity; not only may it become of the resonance type, but it may also, under certain conditions, take on the other two types of sensitivity.

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Discussion

W. Richter (A. O. Smith Corporation, Milwaukee, Wis.): This paper is an excellent contribution to our knowledge on a-c bridges and should prove very valuable to all those workers that are employing these. If the desired accuracy for the unknown is given, the tables published in the paper permit the quick determination of the unbalanced voltage due to a small change of any of the balancing resistors. A knowledge of the magnitude of this voltage on the other hand permits the proper choice of the null-detector, and the design of an amplifier, if needed. Knowing the input voltage for this amplifier will save time and effort, usually wasted in trying to make it more sensitive than required for the problem on hand.

The paper treats only the problem of finding the open-circuit voltage of the bridge, that is, it assumes that a detector with infinite or at least very high impedance is employed. This assumption can safely be made for a-c bridges, since the usual detectors of former years, such as phones and a-c galvanometers, are now generally replaced by vacuum tubes, or at least, a vacuum tube is placed between the bridge and the detector, thus imposing a negligible load on the bridge. For d-c bridges this does not hold true however, since a simple and reliable amplifier for d-c voltages of the order of a few microvolts has not appeared yet.

Between equations 13 and 14 the statement is made that for maximum sensitivity the four impedances should be alike. This statement, carried from one textbook to the next, is incorrect even for the case where the detector has finite impedance, but its incorrectness is particularly easily seen in the case treated in the paper under discussion. Equation 7 gives the unbalanced voltage e' in terms of A and σ'. A is the ratio Z_1/Z_2 which for balance must equal Z_4/Z_3, while σ' is the fractional change dZ_3/Z_3.

The expression (7) for e' becomes a maximum for $A = 1$, that is, $Z_1 = Z_2$ and $Z_4 = Z_3$, but evidently there is no need for Z_1 to be equal Z_4 and Z_2 to be equal Z_3, since in the equation 7 appears only A, but not the actual values of the four arms. Even without reference to the equation 7 it is easily recognized that in case of infinite detector impedance the potentials of the two points to which the detector is connected are only functions of the impedance ratios of the two branches, that is, functions of Z_1/Z_2 and Z_4/Z_3, but are independent of the actual values of these impedances.

The paper does not assign any symbol to the angle of the complex ratio A; in the second example, the angle of 71.8 degrees is as well the angle of A as of σ'. This might prove somewhat confusing to the person reading the paper for the first time. It might have been helpful to point out that this coincidence is due to the fact that in this particular example of the Maxwell bridge the angle of A happens to be equal but of opposite sign to the angle of Z_3 because the phase angle of Z_4 is zero.

L. A. Zurcher: W. Richter's discussion is a valuable contribution to this paper. Mr. Richter is right that the statement "The maximum sensitivity for the real type is obtained when $Z_1 = Z_2 = Z_3 = Z_4$," made in the paper between equations 13 and 14, is incorrect. Oliver Heaviside in his "Electrical Papers," volume 1, pages 3–12, derived equations which apply to the present discussion. He specified the detector resistance by e and the battery resistance by f. Corresponding to the positions of Z_1, Z_2, Z_3, Z_4 in this paper he had resistances of a, c, b, and d. Heaviside's statement from page 4 is "and if only d, e, and f are given, then for any value we give to c, there is a pair of values of a and b which constitutes the best arrangement for that value of c; and there will be a particular value of c which, with the corresponding values of a and b, will be the best arrangement for the given values of d, e, and f." The result of this analysis is given on page 7 and is

$$c = \sqrt{df\frac{d+e}{d+f}} \tag{9}$$

$$a = \sqrt{ef} \tag{10}$$

$$b = \sqrt{de\frac{d+f}{d+e}} \tag{11}$$

Applying this to the present case where e is large and f small compared to a, b, c, and d we find that

$$c = \sqrt{df\frac{d+e}{d+f}} = \sqrt{ef}$$

$$a = \sqrt{ef}$$

$$b = \sqrt{de\frac{d+f}{d+e}} = d$$

or for this case $c = a$ and $b = d$.

This corresponds to Mr. Richter's statement that e becomes a maximum for $A = 1$.

Further for the case where the detector has infinite resistance and the battery finite resistance the ratio of a to c is

$$\frac{a}{c} = \sqrt{\frac{ef}{df\dfrac{d+e}{d+f}}} = \sqrt{\frac{d+f}{d+e}\,\frac{e}{d}}$$

$$= \sqrt{\frac{d+f}{\left(1+\dfrac{d}{e}\right)d}} = \sqrt{1+\frac{f}{d}} = \frac{b}{d}$$

That is if the battery supplying voltage to the bridge contains an internal resistance, the condition for maximum sensitivity is that

$$\frac{a}{c} = \frac{b}{d} = \sqrt{1+\frac{f}{d}}$$

and also a and c should be as large as possible.

B. Hague in his "Alternating Current Bridge Methods," page 63, shows that Heaviside's equations apply to the a-c case

Demand-Meter Time Periods

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THE "demand meter" has been used for approximately 40 years. The function of this device is to measure the "maximum demand" of the load taken by any user of electric service. This quantity, in turn, is used by most utilities in their rates for such service to their larger customers.

Unfortunately, the time period over which this "demand" is measured has never been standardized. A one-minute "demand" is the minimum time the writer ever heard of actually being used and one hour is the maximum. Modern practice has crystallized generally on two time periods, 15 minutes and 30 minutes.

When the demand meter used is of the "block interval" type, which measures the arithmetic average of the load used over the specified time period (15 minutes or 30 minutes as the case may be), there is no difficulty in getting a definite and concrete idea of just what is meant by "maximum demand." However, the Code for Electricity Meters, which is sponsored among others by the Edison Electric Institute, recognizes not only the "block interval" type but also puts its stamp of approval on the type wherein time appears as an exponential function instead of an arithmetic function as it does in the "block interval" type. When time appears as an exponential function, the meaning of "maximum demand" is not so apparent as when it appears as an arithmetic function. It is the object of this brief discussion to point out the differences in indication of "maximum demand" when using these two types of demand meters, that is, an arithmetic average as compared to a logarithmic average. The authors also make a plea for standardization of the time period of all maximum demand measurements.

To quote from the Code for Electricity Meters:

> CLASS II. INTEGRATED-DEMAND METERS
>
> 16. An integrated-demand meter consists of a device in a combination with an integrating meter whereby the energy consumption as measured by the meter is registered from time to time in such a way that the maximum demand may be determined from the record. Two variants are recognized: . . . .
>
> CLASS III. LAGGED-DEMAND METERS
>
> 17. These are instruments so constructed as to require a certain time interval for the indication to reach the point corresponding to the value of the load. Two variants are recognized:
>
> (*a*). Those in which the speed of the indicator in moving up its scale under constant load, is constant, or at any load, is proportional to the load.
>
> (*b*). Those in which the speed diminishes with the time of the deflection. (The demand interval of class III*b* meters is ordinarily considered to be the time required for the instrument to indicate 90 per cent of the full value of a steady load which is thrown suddenly on it.)

It should be particularly noted that the character of the quantity indicated by any of the "Class II. Integrated Demand Meters" or the "Class III—(*a*)" meters is quite different from that indicated by the "Class III—(*b*)" meters. To express in mathematical terms the indications of first two of the above types, it may be said:

meter indication = average load over the specified time (1)

The Code for Electricity Meters recognizes that this quantity differs from the quantity indicated by the class III*b* meters as is shown by the following quotation from the code:

> PRACTICAL INTERPRETATION OF MAXIMUM DEMAND
>
> 19. In commercial practice the maximum demand of an installation or system is given by the record or indication of a demand meter of acceptable type, which is correctly installed, properly adjusted, and none of the errors of which exceeds the limits of commercial tolerance.

In figure 1, the area *mmnn* shows the extreme limits of the indications of demand meters of class II type when there is applied to such a meter any given amount of energy (kilowatt-hours) of a time duration of less than three meter periods. The heavy line *mm* represents the indication that will obtain when using the class III*a* type in measuring short-time-duration loads. In neither of these cases does the meter distinguish between loads whose time of duration is less than one meter period. For instance, a 100-kw load for 30 minutes will give an indication no different from a 200-kw load for 15 minutes, a 600-kw load for 5 minutes, or a 3,000-kw load for one minute. The arithmetic average of all these over a 30-minute period is exactly the same.

When we come to consider the indications given by the class III*b* meters, (an exponential time function meter) we find quite a different situation. The indication of this type meter when responding to a load having a given amount of energy in a given time may be expressed mathematically as follows:

$$\text{meter indication} = \frac{\text{watt-minutes}}{t}\left(1 - \epsilon^{-Kt}\right) \qquad (2)$$

where

ϵ = base of Napierian logarithms
t = time (minutes) of load application
K = an adjustable constant

In figure 1, curves *A*, *B*, *C*, *D*, and *E* represent the indications of a class III*b* demand meter when a given amount of energy (kilowatt-hours) is applied during any time period from zero to three times the time period of the meter. These five curves are simply a plot of meter indication against time as given by equation 2. These five curves differ only in the value of *K*. If *K* = infinity, it is obvious that equation 2 reduces to indication = watts (curve *A*). If *K* = 4.605, we have curve *B;* the indication will reach 99 per cent in one time period. If *K* =

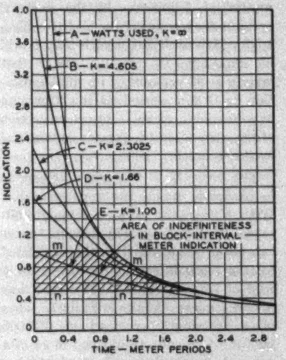

Figure 1

Paper number 39-72, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 10, 1938; made available for preprinting January 6, 1939.

P. M. LINCOLN is president of Therm-Electric Meters Company, Inc., Ithaca, N. Y., and former director of the school of electrical engineering, Cornell University, Ithaca; R. R. SPROLE is associated with the Therm-Electric Meter Company, Inc.

Mr. Sprole is responsible for the appendix.

2.3026, we have curve *C;* the indication reaches 90 per cent in one time period. If $K = 1.66$, we have curve *D;* the indication reaches 81 per cent in one time period. If $K = 1$, we have curve *E;* the indication reaches 63.2 per cent in one time period.

The value of the constant K is adjustable (by the maker *or* user) to any value desired within reason. The Code for Electricity Meters states that "ordinarily" the time period of class III*b* meters shall be taken as the time to reach 90 per cent of final value. This would mean curve *C* in figure 1. The authors would suggest that the word "ordinarily" be omitted from this section of the code. As the code stands at present, the meter user would be justified in assuming that he is at liberty to use some value of K other than 2.3026 which gives the 90 per cent registration at full time. The writer happens to know that in some cases a value of $K = 4.605$ has been used. As clearly indicated in figure 1, this value of K is apt to give an excessive overregistration when the time of load application is less than about 99 per cent of one meter period. In thermal demand meters or in any other type wherein time appears as an exponential function, the time period of the meter is a matter of definition. In the authors' opinion, the wording of the Code for Electricity Meters should be such that it will be impossible to install a meter that arrives at 90 per cent of its final indication in 15 minutes and call it a 30-minute or a 60-minute meter. Proper protection of the users of electric service demands at least this much.

The value $K = 1$ (curve *E*, figure 1) is obviously much too small a value. With $K = 1$, the value of demand indicated by the class III*b* demand meter can never be greater than that indicated by the block interval type—even for a load of 1,000 times for 0.001 of the time period—unless "peak splitting" takes place. Obviously, a load of 100,000 kw for 1.8 seconds should pay more of a demand charge than a load of 100 kw for 30 minutes, although the energy contained in both these blocks of load is exactly the same. Since in the class III*b* demand meter, the time period is a matter of definition, it seems to the authors that the code should leave no doubt concerning the percentage of final reading to be taken as the nominal time period of such a meter. In other words, the definition should be definite.

The authors have included curve *D* ($K = 1.66$) for the purpose of raising a question concerning ammeter versus wattmeter performance. Class III*b* demand meters may be made available in ammeters as well as wattmeters. The time to reach 90 per cent of final reading has nearly always been taken as the nominal time period of the ammeter as well as the wattmeter. It should be noted, however, that the ammeter has a scale of squares while the wattmeter scale is uniform. When an ammeter has reached 90 per cent of its final reading, it has reached only 81 per cent (90 per cent squared) of its final angular deflection. Therefore, if we stick to 90 per cent of final reading as the proper definition of time period for both ammeters and wattmeters, it means that a given structure cannot be used for both instruments. For instance, a structure which, when used as an ammeter will arrive at 90 per cent of its final reading in 30 minutes, will, when used as a wattmeter, require approximately 42 minutes to arrive at 90 per cent of its final reading. This is not a serious matter since adjustment of the time period is not difficult. But if it is considered desirable that a given structure may be available for use either as an ammeter or wattmeter without change, the definition of what constitutes the time period of either the wattmeter or the ammeter must be changed; either the wattmeter time period must be taken as the time to arrive at 81 per cent of final reading (curve *D*-figure 1) or the ammeter time period must be taken as the time to arrive at 95 per cent of its final reading.

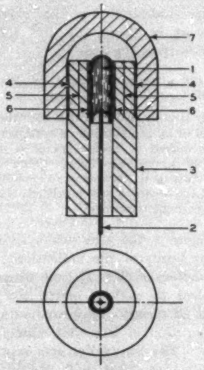

Figure 2

So long as "maximum demand" is measured by class II or class III*a* demand meters, there will always be an urge on the part of the utility to adhere to a comparatively short time period for demand measurement. Many of the modern uses of electric service lead to large loads of a relatively short time duration—welding for instance. If a customer has a load of any character which has a high value for a time period of say five or ten minutes, the utility naturally wishes to obtain compensation for the duty of supplying these high loads for relatively short time periods. If the utility has a choice between a 15-minute and a 30-minute demand period, the tendency will be to adopt the lower time period because of the desire to secure adequate compensation for supplying these short-time high-peak loads. If the demand measurements are made by means of the class III*b* type of demand meter, this tendency to choose the short time periods for demand measurement does not exist—or at least not to the same extent. The class III*b* demand meter automatically recognizes the time of load duration. This is clearly shown by curves *A*, *B*, *C*, *D*, and *E* of figure 1. These curves all show that the shorter the time during which a given amount of energy is used the higher will be the demand reading.

Some experimental work which one of the authors has been carrying on during the last ten years may be of interest to the users of demand meters and aid them to settle this question of the proper time period to adopt as standard for modern demand meters. Figure 2 shows diagrammatically the setup with which he has been experimenting. In figure 2, 1 is a reservoir containing an expansible liquid; 2 is a capillary tube which conveys this liquid to an indicating mechanism (not shown); 3 is a mass of insulating material (the experimenter is using bakelite), in one end of which are embedded resistances 4, 5, and 6; 7 is a mass of material, preferably metal, which is used for the purpose of storing the heat that is liberated in the resistances 4, 5, or 6. If the heat generated in the resistances 4, 5, or 6 at a given constant rate is diffused instantly throughout the mass being heated, it can be proved mathematically that temperature rise of the entire mass follows an exponential law. That is

$$T = T_1 (1 - \epsilon^{-Kt}) \qquad (3)$$

where

T = the temperature rise of the mass at any time

T_1 = the final temperature rise of the mass after the application of the constant heat rate for an infinite time

e = base of Napierian logarithms
t = time
K = a constant

However, it is well known that heat does not and cannot diffuse instantly throughout any mass of matter that is being heated. Another phenomenon—diffusivity—enters the picture. If the diffusivity of the materials making up the entire mass being heated is known, the rate of temperature rise of any particular part of the mass being heated may be calculated mathematically (see appendix). However, the mathematical solution so arrived at is rather complicated and the authors prefer to discuss results in words rather than in mathematical formulas.

It is obvious that if the location of the heater is that shown as 4, figure 2, the heat generated therein is more quickly conducted to the mass 7 than to the heat-sensitive member 1. Such a location of the heater results in a meter performance similar to that shown as curve *A*, figure 3. In fact, curve *A*, figure 3 is the actual curve of performance of a thermal wattmeter first produced in 1917. (For complete description, see "The Character of the Thermal Storage Demand Meter," AIEE Transactions, volume 37, 1918, pages 189–210.) The heater location of the wattmeter described in the reference simulated that of 4 in figure 2 herein. The maximum deviation from the theoretical response, assuming instantaneous diffusion, is nearly 30 per cent. If now the heater location be made that shown diagrammatically as 6 in figure 2 herein, it is obvious that the heat-sensitive member 1 will be affected in advance of the remainder of the total mass being heated. Location 6 in figure 2 herein results in a meter performance shown as curve *B*, figure 3. In this curve, the deviation from theoretical response, assuming instantaneous diffusion, is about the same as in curve *A*, figure 3, but now the deviation is above that for instantaneous heat diffusion instead of below, as it is in curve *A*, figure 3. If the heater location is that shown as 5, figure 2, the diffusion to 1 and 7 is very nearly at the same rate and the resulting performance is close to the theoretical—curve *C*, figure 3. By interposing additional thermal resistance between the heater and the mass 7, curve *B*, figure 3, may be caused to rise still further during the early moments of load application if desired.

The authors would like to point out that the actual curve of the heating of generators, motors, transformers, and other electrical equipment under actual loading conditions is more nearly like curve *B* of figure 3, than any of the other curves of this figure. The phenomenon of diffusivity occurs in actual electrical equipment even to a greater extent than in the structure shown in figure 2. The windings of any electrical equipment under load will heat up in advance of the equipment as a whole. Heat is diffused from the windings to the other parts only after a lapse of time. Diffusion follows a law of nature which cannot be repealed. So long as we must necessarily depart from the assumption of instantaneous diffusion, the authors would suggest that the departure be in the direction that we find in actual electrical equipment, namely, curve *B* of figure 3.

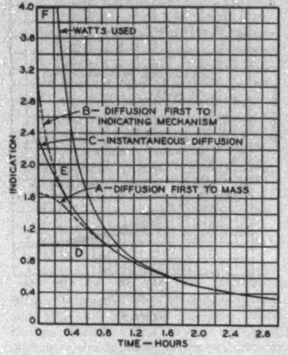

Figure 3

In view of the modified law of response that may be secured by the means described above, the authors would like to suggest the advisability of modifying the existing Code for Electricity Meters so that it calls for a definite time period for all demand meters. The relative demand indications as given by a thermal demand wattmeter as described above and a standard class II or class III*a* demand meter of one-hour time period may be seen from an inspection of figure 3. The horizontal line *D* of figure 3 gives the indication of a one-hour class II or class III*a* demand meter, provided it does not "split the peak." This compared with curve *B* gives the relative indications of the two types. During the first instant of load application, the class III*b* type wattmeter will register at a rate about three times that of class II or class III*a*. The rate of registration of the former remains above the latter for approximately 53 minutes. After the lapse of 53 minutes the class II or class III*a* type meter indicates a higher value than the class III*b*; provided there is no "peak splitting," the maximum excess of the former above the latter is ten per cent after exactly one hour of load application. After one hour, the difference decreases and after about 2$^1/_2$ hours, the two types will read the same values. However, the above comparison is of little practical value since the number of demand meters of the class II or class III*a* type in actual use having a one-hour time period is practically nil.

A much more important comparison is one between a one-hour class III*b* type and a 30-minute class II or class III*a* type. This is given in figure 3 by comparing curve *B* with the line *E*. Comparing these, it will be noted that during the first instant of load application, the former registers at a rate approximately 50 per cent higher than the latter and remains higher during the first 11 minutes of load application. From 11 minutes to 30 minutes of load application, the class II or class III*a* will indicate considerably higher values than the class III*b*—assuming no "peak splitting"—the maximum excess of the former over the latter occurring after 30 minutes and amounting to approximately 47 per cent. After 30 minutes, the indications of the two types approach each other rapidly and become the same after about 2$^1/_2$ hours of load application. It is impossible to say which type would indicate the higher value. This will depend entirely upon the character of the load during maximum and upon whether "peak splitting" occurs. It is the authors' opinion that in general the two types will not be far apart in their indications.

The line *F*, figure 3, compared to the curve *B* shows the relative indications of a 15-minute class II or class III*a* to a one-hour class III*b* demand meter. In this case, the former will always read higher than the latter, unless the time of load application is exceedingly short and "peak splitting" is at a maximum—a practically impossible combination. However, the authors consider that 15 minutes is too short a time period for demand measurement, no matter what type of demand meter is used. Even with this time period, it will be noted that after 2$^1/_2$ hours of load application the two types indicate the same value.

The authors would urge that the utilities give serious consideration to adopting uniformity of practice in the time period over which maximum demand shall be measured. They hope that the foregoing analysis will be of service to them in the adoption of such a uniformity of practice.

It was not until after the manuscript for the foregoing paper on "Demand Meter Time Periods" had been submitted that the writer learned that a committee under the chairmanship of Doctor J. Franklin Meyer of the Bureau of Standards had been appointed to revise the Code for Electricity Meters. If the authors had known at the time of preparing the manuscript that the Code for Electricity Meters was coming up for revision, their recommendation would have been definitely to fix the time period of the thermal demand wattmeter at the time required to arrive at 81 per cent of final indications (K = 1.66). The reason for this recommendation is that it reduces to probably a minimum the discrepancy between the indications of the two types of demand wattmeters.

Commenting further, it is so obvious as to need no further discussions that the two types of demand wattmeters should indicate the same values as nearly as possible. The differing fundamental principles underlying the two types make it impossible that the two types should *always* indicate the same values. Figure 4 shows a comparison between the two types when K = 1.66 as well as when K = 1.151. Judging from this comparison, it is the authors' opinion that the value K = 1.66 will *probably* give about as small a discrepancy between the indications of the two types as any value that could be chosen.

Figure 4 shows also the results of some tests recently made on a thermal wattmeter. The heavy solid line marked K = 1.66 represents the performance that would obtain if the diffusion of heat could be made instantaneous. The short dotted line represents the performance with diffusion to the mass at a normal rate. The long dotted line represents the performance that has been obtained by a design that involves a considerable reduction in the rate of diffusion to the mass. This long dotted line by no means shows the maximum departure from the solid line that can be obtained by reducing the rate of diffusion to the mass still further.

It is obvious that a meter performance which follows the long dotted line would overregister on short-time high-peak loads—such as a spot welder. The rate of heat diffusion cannot be made infinite. Diffusion cannot be made instantaneous, but it can be controlled by proper design. The authors consider it to be a part of the duty of Doctor Meyer's committee to fix the maximum amount by which the dotted-line performance may be permitted to depart from the solid-line performance.

The advantage of being able to use a given structure either as an ammeter or wattmeter without change is so obvious as to need no further comments.

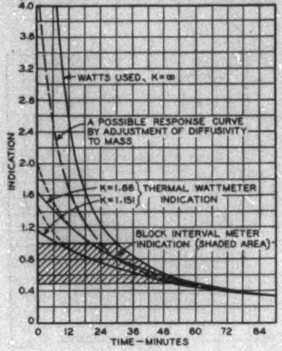

Figure 4

If the same percentage of final reading is used to define the time periods of both ammeters and wattmeters, where time appears as an exponential function, as has been the general practice of the past, it means essentially that there is discrimination amounting to approximately 40 per cent of the ammeter time period between electric - service users whose maximum demand is measured by the thermal ammeter and those whose maximum demand is measured by the thermal wattmeter. The thermal demand ammeter antedates the thermal demand wattmeter by many years (Wright system, Brighton, England, about 1896). Wright adopted 90 per cent of final indication as his definition of time period for his demand ammeter. In the authors' opinion, this was a remarkably astute choice since it gives about as small a discrepancy between the "arithmetic" and the "logarithmic" averages as any value that could be chosen.

With the advent of the thermal demand wattmeter in 1917, the authors see no justification for continuing the discrimination between the ammeter user and the wattmeter user that was brought about by adopting the same percentage of final reading for both instruments.

In view of the foregoing discussion, therefore, the authors respectfully submit the following recommendations for the consideration of Doctor Meyer's committee:

1. The adoption of the time to arrive at 81 per cent of its final reading as the nominal time period of the thermal demand wattmeter.
2. The adoption of the time to arrive at 90 per cent of its final reading as the nominal time period of the thermal demand ammeter.
3. That 30 minutes shall be adopted as the time period for all demand meters, thermal and otherwise.
4. That Doctor Meyer's committee shall fix the maximum allowable departure to be permitted between the actual curve of response of thermal demand meters and the curve of response which would obtain under the assumption of instantaneous diffusion of heat and that the value so fixed shall become a part of the revised code.

Appendix

The equation of temperature rise versus time (equation 3) given in the paper assumes instantaneous diffusion of heat throughout the mass. As pointed out, this assumption introduces an erroneous concept in regard to the heating and cooling of mass. However, the mathematical expression so obtained can conveniently be handled in calculating approximate values.

In an effort to broaden this concept of temperature rise throughout a mass by introducing the diffusivity factor, reference is made to Ingersoll and Zobel in their book, "Mathematical Theory of Heat Conduction," and also the text, "Heat Transmission" by William H. McAdams. Both texts refer to Fourier's expression of thermal conductivity and derive from it the partial rate of temperature rise in a given direction as:

$$\frac{\partial\theta}{\partial t} = \frac{K}{\rho c_p}\left(\frac{\partial^2\theta}{\partial Z^2}\right) \qquad (4)$$

Applying equation 4 to a slab of mass of thickness $2R$ having negligible surface resistance, McAdams shows the temperature rise as a function of time to be:

$$\theta = \Theta\left(1 - \frac{4}{\pi}\sum_{a=1}^{a=\infty}\frac{\epsilon^{-a^2\alpha t}}{a}\cdot\sin a\beta\right) \qquad (5)$$

The boundary condition of equation 5 is the same as that for the expression given in equation 3, that is, $\theta = \Theta$ at $s = 0$ and $s = 2R$; $\theta = \theta_1$ at $t = 0$; and $\theta = \Theta$ at $t = \infty$.

Equation 4 may be expressed in the form of a general solution for the particular problem given by McAdams as:

$$\theta = \frac{\alpha}{\pi}\sum_{a=1}^{a=\infty} a\sin a\beta\cdot\int_0^t f(x)\epsilon^{-(t-x)a^2\alpha\left(\frac{1}{\pi}\right)^2} \qquad (6)$$

It is to be noted in equations 5 and 6 that for a given mass, the value of β will depend upon the location of that particular point within the mass where the temperature rise is to be measured. Therefore, there exists for each point within the mass, a separate and slightly different exponential expression.

The mathematical series of equations 5 and 6 converges rapidly, and for large values of t, equation 5, approaches the expression given in the paper. The diffusion factor becomes less dominant in the solution as t increases, being negligible for values of t when θ is as small as 70 per cent Θ.

Thermal demand metering has in general been accepted as being a logical method for the measurement of demand due to the fact that the indication of these meters, and the heating of electrical equipment are both a function of the fundamental laws of heating and cooling. However, to set forth a specific temperature response curve for thermal meters to follow based upon the heating and cooling curves of electrical equipment would be quite impractical as a variety of thermal curves would result for different equipment, and in addition a wide family of curves would be obtained for any given equipment depending upon the point within the equipment where temperature was being measured.

It is important, however, that all thermal meters of a given classification follow the same exponential curve. Otherwise, thermal meters built by various manufacturers will not agree in their indication on rapid load variations, although it is true that they will register the same on a continuous applied constant load regardless of the exponential curve followed by the individual meters.

In taking steps to standardize on an exponential curve, the paper points out that in referring to the heating and cooling of electrical equipment, one can be guided by the fact that in general the conductors buried in the surrounding insulation have a more rapid temperature variation than the surrounding mass upon load variations. This statement bears considerable weight in deciding whether or not a thermal meter should have an initial rapid, or slow rise in indication upon load application. The authors refer to the exponential equation as a reference datum and to this extent the equation becomes quite useful.

Whatever the standardized exponential curve shall be, it inevitably must be one arbitrarily deduced and biased to some extent by the range over which present thermal-meter manufacturers can control their response. With the variety of exponential curves resulting from the most simple application of Fourier's equation, as illustrated in this appendix, there certainly must exist a curve that can be made satisfactory to all concerned and still be classified as an exponential curve.

θ = temperature at time t

θ_1 = temperature at time $t = 0$

t = time

K = conductivity

ρ = density

c_p = specific heat

Z = distance along Z axis

Θ = ultimate temperature of mass arrived at when $t = \infty$ under conditions of constant heat application

a = odd integers

$$\alpha = \left(\frac{\pi}{2}\right)^2 \left(\frac{K}{\rho c_p}\right) \left(\frac{1}{R}\right)^2$$

$$\beta = \left(\frac{\pi}{2}\right) \left(\frac{s}{R}\right)$$

R = main thermal path or distance from the source of heat to the midplane, the midplane being the plane across which no heat passes

s = mean thermal path or distance from source of heat to point at which temperature is being measured. In the thermal meter the source of heat is considered to be the resistors, and temperature is measured within the liquid of the reservoirs

$f(x)$ = function of heat applied to mass corresponding to time x

ϵ = 2.718

Discussion

Sidney Withington (New York, New Haven, and Hartford Railroad Company, New Haven, Conn.): This interesting paper illustrates clearly many of the problems in design and utilization of demand meters, and is a valuable addition to the literature on the subject.

While the recommendation of the 30-minute time period for all demand meters is of interest, and in some respects logical, it is suggested that in certain services at least, this period is shorter than is desirable, in view of the characteristics of the services involved and the design characteristics of the supply and utilization equipment, and for this reason one-hour periods would be in many ways more logical than shorter periods, as standard. In this connection, the one-hour integrated demand period might be correlated with the clock-hour in the interest of simplification.

H. M. Witherow (General Electric Company, Lynn, Mass.): We agree with Dr. Lincoln as to the desirability of standardizing time interval and characteristics of demand meters. Such standardization would ultimately benefit both the utilities and the manufacturers. However, in arriving at standardized characteristics there are many factors which must be studied and given their proper value. Characteristics should be fixed only after a thorough study of these factors has been made and conclusions discussed with manufacturers, and users of demand meters.

In most discussions regarding the agreement or lack of agreement between block interval and logarithmic demand meters a great deal of emphasis is placed upon the difference in registration on loads of short duration. These differences have been given much too much importance since any demand meter measures only approximately the many variables entering into the cost to serve. In considering standardization, short period characteristics should be considered of secondary importance.

Welding loads which call for large amounts of power for intervals of only a few seconds cannot be properly measured by any of the ordinary demand devices. Billing for this type of load which will reflect the cost to serve will ultimately be made on some other basis or at least on some measurement which is suitable for this type of load and which is quite different from the ordinary demand measurement.

The intermittent loads usually found which may have peaks of a few minutes duration might be considered a real problem if one central station served one customer. Where one central station and distribution system serves many customers these more usual short-time peaks have little or no effect on the system since the individual peaks are small compared to the capacity of the system and time diversity of the many customers tends to minimize their effect. Demand measurement may be considered as a method of reducing the combined sustained peaks rather than a means of penalizing the individual customer who may have a high peak of short duration. Many rate engineers now think of demand measurement in terms of promotional rates rather than in terms of penalty rates, and it is entirely proper that demand charges should be so considered.

In considering standardization of characteristics, most importance should be given to reasonable agreement on the more usual types of loads where peaks are normally sustained for longer periods. From the standpoint of promotional rates the time interval should be made as long as is consistent with the short-time rating of generating and distribution equipment.

There is little justification for a difference in the characteristics of logarithmic wattmeters and ammeters. The only reason for such a difference is convenience to the manufacturer, which usually has not been considered a sound basis of definition. If some certain characteristic can be defined as most suitable for measurement of demand in watts, this same characteristic should be just as desirable for the measurement of demand in amperes, volt-amperes, or any other unit.

In fixing the time interval for the characteristic of logarithmic meters, practical as well as theoretical considerations should be properly evaluated. For example, the time required for testing demand meters is quite important to the larger utilities. Within the past few years very satisfactory means have been developed for testing block-interval mechanisms whereby a complete check can be made within a few minutes regardless of the rated interval of the device. To my knowledge, no comparable method is available for testing thermal meters and if the ideal logarithmic meter is one reaching 81 per cent of its final indication in 30 minutes, the time required for test would be greatly increased over that required for testing similar meters now on the market. It is probable that such a meter would require about two hours to reach its final indication on steady load and an equal period would be required for it to cool off in order to check a second point.

Whenever logarithmic demand meters are discussed it seems to be taken for granted that one should point out that block-interval meters may split peaks of short duration and thus arrive at different indications for equivalent loads, depending upon where the fixed interval begins. This is a characteristic of the block-interval meter which is well recognized and which may cause some variation in reading on certain types of loads, but which, in general has little significance in demand measurement. In papers in which this possible variability is given prominence, the inference is that the logarithmic meter is perfect in this respect and that it will always give the same indication for equivalent loads. This is true if we assume that these equivalent loads are always applied in the same order. For example, let us assume that a customer has just two loads, one of which is three times the other, and that once in each 30-minute period they are applied in sequence for some short interval such as 5 minutes each. If the larger load always follows the smaller load then the logarithmic meter will repeat. However, if we reverse the sequence, quite different results are obtained. Recent tests on such a load showed a difference of over 20 per cent in the indication of a logarithmic meter, merely by reversing the order in which the loads were applied. Practically, this is just as important and has just as little significance as the split peaks of block interval meters. I would like to suggest that in future discussions these characteristics be given a place comparable to their significance so that the real point of the discussion will not be lost.

Albert J. Allen (Consolidated Edison Company of New York, Inc., New York): I want to congratulate Doctor Lincoln for his clear exposition of the manner in which the load-time curve of thermal-demand meters may be controlled.

In recommending a particular load-time curve as a single standard for thermal-demand meters, however, I believe that a greater step has been taken than is justified by the evidence at hand. In any event, the inclusion of this single standard, in the Code for Electricity Meters would set up restrictive clauses of a nature that have always been, and should remain, foreign to the main purpose of the code.

In my opinion, the code should remain broad enough to cover all acceptable types of demand meters, and as long as any such type is clearly described, and made subject to a sufficient number of tests to determine that representative samples of that type will perform in accordance with its stated characteristics, the purpose of the code is accomplished.

Any further step is now, and should remain, in the hands of the rate engineer. It is for him to decide which among the various types of demand measurement available best represents the cost of service. The code can be of great value in outlining what kinds of demand meters are available for use and in setting up tests to determine which manufacturer's types best perform in accordance with stated characteristics, but it would do a disservice if it restricted the use of any type of demand meter to a particular demand interval, without regard to the characteristics of the system on which it is to be used, or the load which it is to measure.

The difference in load measurement between lagged-demand meters as a class and integrated-demand meters as a class, both of which would remain as acceptable types, is much more pronounced than the difference between two lagged-demand meters having different characteristic curves, one reaching 81 per cent and the other 90 per cent of final indication in a demand interval. This, I believe, weakens the case for selecting either of these particular load-time curves as a single standard for lagged-demand meters.

P. M. Lincoln: Mr. Withington's suggestion for adopting one hour instead of 30 minutes as the standard time period for demand measurement is, in the writer's opinion, a step in the right direction. However, the writer believes that such a move is premature. The first step is the elimination of the 15-minute time period now quite widely used. If and when all demand periods have become 30 minutes, the next logical step would be to increase the time period for demand measurement to one hour.

The writer believes that the utilities would hesitate to take such a step so long as "block-interval" meters are used for demand measurement. In many of the modern uses of electric service, the average of the maximum 15-minute or 30-minute period within the maximum one hour would be materially higher than the average for the whole hour. Such a step would therefore mean essentially a reduction in rates. This is a question for each utility to consider on its own merits. The thermal demand wattmeter would recognize the higher rate of use of service during fractions of the maximum hour and with this method of measuring maximum demand, the writer sees no objection to the use of a one-hour time period.

Mr. Witherow deplores the emphasis placed on the difference in indication between the "block-interval" and the "logarithmic" types of demand meters. In view of the differing fundamental principles underlying the two types, it is not difficult for a utility to explain a difference in the indication of the two types to a user of service. Mr. Witherow does not suggest any logical explanation that the utility can give to a service user who finds a difference in indication of 5 per cent to 15 per cent (a possible 50 per cent) in the indications of two identical "block-interval" meters measuring the *same* load. Nor does he suggest any means by which the utilities may meet the situation when knowledge of the inherent inaccuracy of the "block-interval" demand meter has become generally known to the users of electric service.

In discussing the difference in registration on short-time loads between the "block-interval" and the thermal types of demand meters, Mr. Witherow states, "These differences have been given much too much importance since any demand meter measures only approximately the many variables entering into the cost to serve." In this, the writer disagrees with Mr. Witherow. The demand measurement is intended to assess the service user an amount which will reimburse the utility for its fixed charges (interest, depreciation, taxes, and insurance) and these in turn amount to approximately one-half of the total costs of rendering electric service. In the writer's opinion, it is impossible to give "too much importance" to obtaining an *accurate* measure of demand.

Mr. Witherow also intimates that billing for short-time loads—such as a spot welder—should be made "on some other basis or at least on some measurement which is suitable for this type of load and which is quite different from the ordinary demand measurement. In using the term "ordinary demand measurement," Mr. Witherow undoubtedly has in mind the block-interval demand meter. The writer would remind Mr. Witherow that the thermal-demand meter is not an "ordinary demand measurement." The thermal-demand meter recognizes the heating effect of any load, no matter what is its value or its time of duration. If all loads were steady, demand measurement would be simple; any sort of a wattmeter would effect an accurate measurement. It is only when load peaks occur that demand measurement calls for something more. That "something more" is the thermal-demand meter which the writer is advocating.

The writer notes with satisfaction Mr. Witherow's comment that "There is little justification for a difference in the characteristics of logarithmic wattmeters and ammeters." Presumably, therefore, he approves the writer's recommendation that the demand wattmeter's time period shall be defined as the *square* of the percentage of final value of the demand ammeter time period.

In commenting on the time required to test thermal demand meters as compared to the "block-interval" type, Mr. Witherow makes a real point. The writer has considered this difficulty at some length. In his opinion, the methods of testing this type meter should be somewhat modified to meet the very point that Mr. Witherow has raised. The writer would suggest that the utilities have on hand at all times a reasonable stock of thermal demand meters known to be correct. If suspicion arises concerning the accuracy of any meter in service, one of these test meters may be placed in series with it. Within two hours, at the utmost, the test meter should be reading correctly the load passing. If the tester cannot wait the necessary time, he may return the next day or the observations may be taken on the occasion of the inspector's next regular visit. If the two meters read the same values—no matter what that value—the subscriber's meter is correct; if there is a difference, the subscriber's meter may readily be adjusted to be correct.

And while we are on the subject of testing, the writer would like to point out that the Code for Electricity Meters recommends the periodic testing of all class I and class II demand meters at intervals of one year. For class III*b* demand meters, the recommended test period is "not to exceed five years." It also may be of interest to observe that in Canada where the thermal demand meter has been standard since 1923, these meters are sealed by the Canadian Government for a period of six years. The simplicity of the thermal demand meter and the absence of any complicated mechanism have proved this practice to be satisfactory in Canada.

Subharmonics in Circuits Containing Iron-Cored Inductors—II

IRVEN TRAVIS
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Synopsis: This paper is an extension of the work reported in a paper of the same title by the writer and C. N. Weygandt.[1] The production of subharmonic oscillations in a series circuit consisting of a capacitor and an iron-cored inductor is studied by the method of matching boundary conditions. The calculations are carried out by means of a new mechanical calculating device, and are verified by differential-analyzer solutions.

A sufficient, but not necessary, criterion for stability is deduced. Some consideration is given to the effect of resistance in the circuit.

I. Introduction

IT IS assumed that the reader is familiar with reference 1. It will be recalled that the method of matching boundary conditions was used in that paper. The method was idealized to a considerable extent in order that certain mathematical difficulties might be avoided. This resulted in an analysis which was convenient in those cases in which it applied, but which was of very limited scope. The idealizing assumptions were:

1. The winding resistance of the inductor was taken equal to zero.
2. All hysteresis effects in the inductor core were neglected.
3. Eddy currents in the inductor core were neglected.
4. It was assumed that the saturation curve of the iron could be represented by the three straight lines shown in figure 1.
5. The analysis was restricted to those cases in which the effect of capacitance *during operation in the saturated region* could be neglected.

Of these assumptions the fifth imposed the most serious limitation. The values of parameters ordinarily encountered in series capacitor installations in transmission systems, and in many other practical cases, fall outside the range over which this assumption is valid. The assumption 4 represents the actual phenomenon with satisfactory accuracy; the assumption that the current is zero over the unsaturated region introduces no great error inasmuch as the unsaturated inductance is usually about 40 times the saturated inductance. The first three assumptions remove all dissipation from the circuit. Although this may affect the stability of various modes of oscillation, it is felt that the effect upon wave form is slight. In this paper assumptions 1 to 4 are retained but the effect of capacitance in the saturated region is considered.

II. Excursion in the Saturated Region

Since the analysis is to include the effect of capacitance during operation in the saturated region, the equation which holds in that region is

$$L\frac{d^2q}{dt^2} + \frac{q}{C} = E\sin(t + \beta_k)^* \qquad (1)$$

subject to the boundary conditions

$$t = 0 \qquad \frac{dq}{dt} = 0 \qquad q = Q_k \qquad (2)$$

In equations 1 and 2, the symbols have the same significance as in reference 1.

The solutions of equation 1 for charge and current, after inserting boundary conditions, are:

$$q = \left(Q_k + \frac{CE\sin\beta_k}{LC-1}\right)\cos\frac{t}{\sqrt{LC}} + \frac{CE\sqrt{LC}\cos\beta_k}{LC-1}\sin\frac{t}{\sqrt{LC}} - \frac{CE}{LC-1}\sin(t+\beta_k) \qquad (3)$$

$$i = \frac{CE\cos\beta_k}{LC-1}\cos\frac{t}{\sqrt{LC}} - \frac{1}{\sqrt{LC}}\left(Q_k + \frac{CE\sin\beta_k}{LC-1}\right)\sin\frac{t}{\sqrt{LC}} - \frac{CE}{LC-1}\cos(t+\beta_k) \qquad (4)$$

These equations hold throughout opera-

Paper number 39-14, recommended by the AIEE committee on basic sciences, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted May 23, 1938; made available for preprinting December 5, 1938.

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The author wishes to express his appreciation to Cornelius Weygandt for valuable suggestions, to M. E. Nelson for his excellent work in constructing the calculating mechanism, and in particular to Doctor J. A. Shohat of the mathematics department, University of Pennsylvania, for his aid in connection with appendix III.

[1] For all numbered references see list at end of paper.

*Time is measured in radians of the impressed sinusoid.

The writer disagrees completely with Mr. Witherow's contention that two loads each of five minutes duration, one three times the other in value, first in one sequence and then in the other, are "equivalent" loads. They most decidedly are *not* "equivalent" loads. The best possible means of proving this lack of equivalence is to examine the temperature rise of the equipment that carries such loads. The rising sequence will invariably cause a higher temperature rise in the equipment carrying such loads than will the falling sequence. The writer considers it to be one of the outstanding merits of the thermal demand wattmeter that it thus recognizes the lack of equivalence of two such loads as Mr. Witherow has described. The thermal demand meter recognizes this lack of equivalence by the same fundamental law of nature as does the equipment that carries the load.

Commenting now on Mr. Allen's contribution the writer quite agrees with Mr. Allen when he says, "The difference in load measurement between lagged-demand meters as a class and integrated-demand meters as a class, both of which would remain as acceptable types, is much more pronounced than the difference between two lagged-demand meters having different characteristic curves, one reaching 81 per cent and the other 90 per cent of final indication in the demand interval."

However, Mr. Allen seems to have missed one of the main points in the writer's paper. The writer considers it of the utmost importance that the Code for Electricity Meters shall recognize the inherent difference between the thermal demand wattmeter and the thermal demand ammeter. To recognize this inherent difference, the time period of the wattmeter *must* be defined as the square of the time for the thermal demand ammeter to arrive at any given percentage of final value. The fundamental laws of nature dictate at least this much. The exact percentage of final value by which to define time and just what this time shall be are matters which are in the hands of the lately appointed committee to revise the Code for Electricity Meters. But whatever time is adopted or whatever percentage is adopted, the percentage of the wattmeter time period *must* be the *square* of the ammeter time period.

The writer still adheres to his recommendation that 30 minutes be adopted as the time period for all demand measurements. Comparisons between the rates of various utilities are thereby made much simpler and confusion will be avoided. However, the writer does not consider this to be an *essential*.

tion in the saturated region, that is, until the flux linkage in the reactor has been reduced again to the value it had when operation started in the saturated region.

The current at a transition from one region of operation to the next is always zero, hence the conditions which obtain at the instant t_k, at which equations 3 and 4 cease to hold, are:

$$Q_{k+1} = \left(Q_k + \frac{CE \sin \beta_k}{LC - 1} \right) \cos \frac{t_k}{\sqrt{LC}} + \frac{CE\sqrt{LC} \cos \beta_k}{LC - 1} \sin \frac{t_k}{\sqrt{LC}} - \frac{CE}{LC - 1} \sin (t_k + \beta_k) \quad (5)$$

$$0 = \cos \beta_k \cos \frac{t_k}{\sqrt{LC}} - \frac{1}{\sqrt{LC}} \times \left(\frac{LC - 1}{CE} Q_k + \sin \beta_k \right) \sin \frac{t_k}{\sqrt{LC}} - \cos (t_k + \beta_k) \quad (6)$$

in which Q_{k+1} is the charge at the end of operation in the saturated region. Q_{k+1} and t_k are the values which must be used as boundary conditions in the solution of the differential equation for the next traverse of the unsaturated region. The time t_k must be determined from equation 6; then Q_{k+1} may be determined from equation 5. Note that t_k is the smallest angle greater than zero which satisfies equation 6.

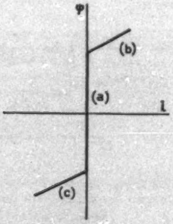

Figure 1. Idealized saturation curve

Since the current is always zero at a transition point, the charge and the phase angle of the electromotive force at transition are sufficient boundary conditions. If the saturated region is entered with a charge Q_k and an electromotive force having the phase angle β_k, then the saturated region will be left when the charge has changed to Q_{k+1} and the phase angle has become $\beta_{k+1} = \beta_k + t_k$. Let us associate corresponding charges and phase angles to form complex numbers. Denote $Q_{k+1} \angle \beta_{k+1}$ by $\mathbf{Q}_{k+1}$ and denote $Q_k \angle \beta_k$ by $\mathbf{Q}_k$. Then the effect on the boundary conditions of an excursion in the saturated region may be described by the equation:*

$$\mathbf{Q}_{k+1} = f_s(\mathbf{Q}_k) \quad (7)$$

where f_s denotes the functional relationship given by equations 5 and 6. Figure 2 shows examples of contours in the $\mathbf{Q}_k$ plane and the corresponding contours in the $\mathbf{Q}_{k+1}$ plane. A discussion of these contours and the nature of the function f_s is given in appendix I.

III. Excursion in the Unsaturated Region

From reference 1, equation 21, the flux linkages during operation in the unsaturated region are given by

$$\psi = -E \cos t - \frac{Q_0}{C} t + \psi_0 + E \cos T_0 + \frac{Q_0 T_0}{C} \quad (8)$$

in which ψ_0 is $\pm\psi_s$, T_0 is the angle of the electromotive force at the instant of entering the region, and Q_0 is the charge at entering. Denote by Q_m and β_m the charge and angle at entering the unsaturated region, then the charge and angle at leaving are Q_{m+1} and β_{m+1}. These values are given by

$$Q_{m+1} = Q_m \quad (9)$$

$$\cos \beta_{m+1} + \frac{Q_m}{CE} \beta_{m+1} = \cos \beta_m + \frac{Q_m}{CE} \beta_m + x \quad (10)$$

where x has the value $+2\psi_s/E$ when the transition is from the upper to the lower saturated region, the value $-2\psi_s/E$ when the transition is from the lower to the upper saturated region, and the value 0 when ψ re-enters the region from which it came.

The effect on the boundary conditions of an excursion in the unsaturated region may be described by the equation

$$\mathbf{Q}_{m+1} = f_u(\mathbf{Q}_m) \quad (11)$$

in which the complex numbers represent charge and phase angle of electromotive force entering and leaving the region, and f_u denotes the functional relationship given by (9) and (10). The footnote discussing equation 7 applies equally well to equation 11.

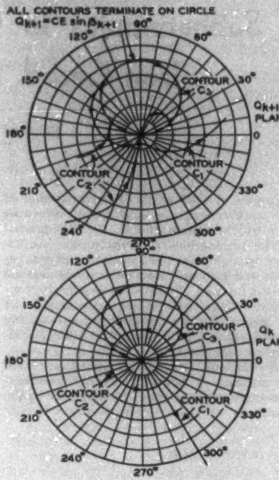

Figure 2. Examples of contours

IV. Recurrent Boundary Conditions

A subharmonic exists when the boundary conditions after a given succession of traverses of the ψ versus i characteristic are found to be substantially recurrent. The term *substantially* is used because it is believed that, except in unusual circumstances, an oscillation which over short intervals seems to be recurrent actually has superimposed upon it a perturbation. The superimposed perturbation may be of much lower frequency than the approximately recurrent phenomenon, or indeed may not be periodic at all.

Disregarding minor deviations due to the above mentioned perturbation, we may express the condition for a subharmonic oscillation mathematically by the equation

$$\mathbf{Q} = f_s f_u \ldots f_s f_u f_s f_u \mathbf{Q} \quad (12)$$

that is, if, by operating successively and alternately with the functions f_u and f_s, a given complex number transforms into itself, then that complex number gives the charge and phase angle of electromotive force for a subharmonic oscillation.

The operation indicated in equation 12 cannot be carried out analytically since

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*There is one point in connection with this method of expressing the problem which requires amplification. The charge Q_k may be negative. Since the modulus of a complex number cannot be negative, the question arises as to whether or not it is permissible to represent such a negative sign by a phase angle of 180 degrees in the usual way for complex numbers. Suppose $Q_k = -A$, where A is positive, and $\beta_k = \theta_a$. Let the resulting value of $Q_{k+1} = B$ and of $\beta_{k+1} = \theta_b$. Now if the complex number $A\underline{/\theta_a + 180°}$ is used to represent $\mathbf{Q}_k$, it is readily seen from equations 5 and 6 that the corresponding values of β_{k+1} and Q_{k+1} are $\theta_b + 180°$ and $-B$; but these would be represented by the complex number $B\underline{/\theta_b}$ which agrees with the previous result. *Mathematically*, therefore, the scheme works; it is useful providing the duality of *physical interpretation* can be rendered particular by other considerations. That this can be done will presently become apparent.

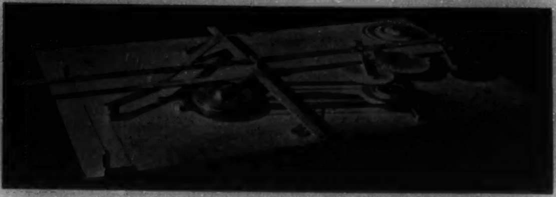

Figure 3. Mechanical device for evaluating θ in the equation $\sin\theta + X\sin(k\theta + \phi) = Y$

there is no practicable analytical method for solving the transcendental equations 6 and 10. Graphical methods could be used but these are time consuming. A mechanical calculating device has been developed for this purpose which is extremely rapid in operation and is sufficiently accurate for all ordinary engineering purposes. The machine is shown in figure 3. A schematic diagram for purpose of explanation is given in figure 4. The theory of operation is given in appendix II.

V. Charts for Matching Boundary Conditions

Given charts of the functions f_s and f_u for a particular idealized circuit, all information about the behavior of that circuit in producing subharmonics can be obtained by carrying out graphically the operation indicated in equation 12. In order that such charts may present the required information in terms of the smallest possible number of parameters, a per-unit system[2] will be used. The base values for this system are:

Base time = seconds per radian of the impressed sinusoid.

Base angular velocity = 2π times the frequency of the impressed sinusoid.

Base quantity of electricity = circuit capacitance in farads times the maximum value of the impressed sinusoid in volts.

Base flux linkages = maximum value of the impressed sinusoid in volts multiplied by base time.

To effect a further simplification in notation, let $1/\sqrt{LC}$ be replaced by ω, then in the above unit system equations 5, 6, 9, and 10 become

$$Q_{k+1} = \left(Q_k + \frac{\omega^2\sin\beta_k}{1-\omega^2}\right)\cos\omega t_k + \frac{\omega\cos\beta_k}{1-\omega^2}\sin\omega t_k - \frac{\omega^2}{1-\omega^2}\sin(t_k+\beta_k) \quad (13)$$

$$0 = \cos\beta_k\cos\omega t_k - \omega\left(\frac{1-\omega^2}{\omega}Q_k + \sin\beta_k\right)\sin\omega t_k - \cos(t_k+\beta_k) \quad (14)$$

$$Q_{m+1} = Q_m \quad (15)$$

$$\cos\beta_{m+1} + Q_m\beta_{m+1} = \cos\beta_m + Q_m\beta_m \pm 2\psi_s, 0 \quad (16)$$

From these equations it is evident that the function f_s depends upon the single parameter ω and that the function f_u depends upon the single parameter ψ_s. The parameter ω, the per-unit angular velocity, is the ratio of the "saturated natural frequency" to the impressed frequency. The parameter ψ_s, the per-unit value of flux linkages at the knee of the saturation curve, is the ratio of the reactor voltage at normal frequency when operating at the knee of the curve to the impressed voltage.

Figures 5 and 6 give the functions f_s and f_u respectively. In figure 5, ω has the value $1/2.5 = 0.63$. In figure 6, ψ_s has the value 1.15. In figure 5, the contours are plotted in rectangular co-ordinates to give greater accuracy in reading β when Q is small than could be obtained in a polar plot; this method of plotting also allows the physical interpretation to be stressed by making a distinction between negative modulus (negative charge) and a phase shift to 180 degrees. In figure 6 β_{m+1} versus β_m is shown for a range of values of Q_m. Since $Q_{m+1} = Q_m$, these curves completely determine the function f_u.

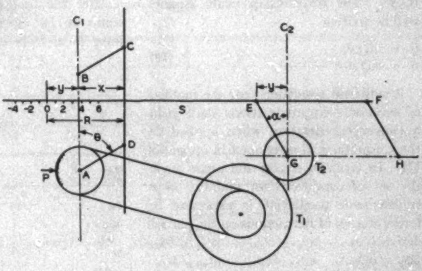

Figure 4. Mechanical device for evaluating θ in the equation $\sin\theta + X\sin(k\theta + \phi) = Y$

The curves of β_{m+1} versus β_m have discontinuities so that they appear in three or more sheets. Of these sheets all except those inside the region marked out in the heavy shield-shaped area have positive slope with β_m. All points on curves with positive slopes represent transitions from $+\psi_s$ to $-\psi_s$ or vice versa. Points on curves having negative slopes (those within the shield-shaped area) represent a return to $\pm\psi_s$ after having left it without crossing over to the opposite saturated region.

VI. Stability of Subharmonic Oscillations

If, after a certain sequence of transitions from region to region, the boundary conditions for a given subharmonic oscillation are recurrent as expressed by equation 12, then in the succeeding interval of time the same sequence of transitions from region to region should occur. Suppose that in the course of such oscillation some small disturbing influence should occur. Mathematically this may be expressed by replacing Q by $Q + \Delta Q$ and β by $\beta + \Delta\beta$. Then after a complete period we have

$$Q + \Delta_1 Q\underline{/\beta + \Delta_1\beta} = f_s f_u \cdots f_s f_u f_s f_u \left(Q + \Delta Q\underline{/\beta + \Delta\beta}\right) \quad (17)$$

If the increments $\Delta_1 Q$ and $\Delta_1\beta$ are greater than the increments ΔQ and $\Delta\beta$ the effect of the disturbing influence will grow with each successive excursion over the sequence of values constituting a period, until ultimately the periodic nature of the phenomenon is destroyed. If, however, $\Delta_1 Q$ and $\Delta_1\beta$ are smaller than the disturbances producing them the effect diminishes from period to period; in this case the oscillation is stable.

The condition for stability is the analytical equivalent of the condition for convergence of the successive approximation solution of equation 12. This equation

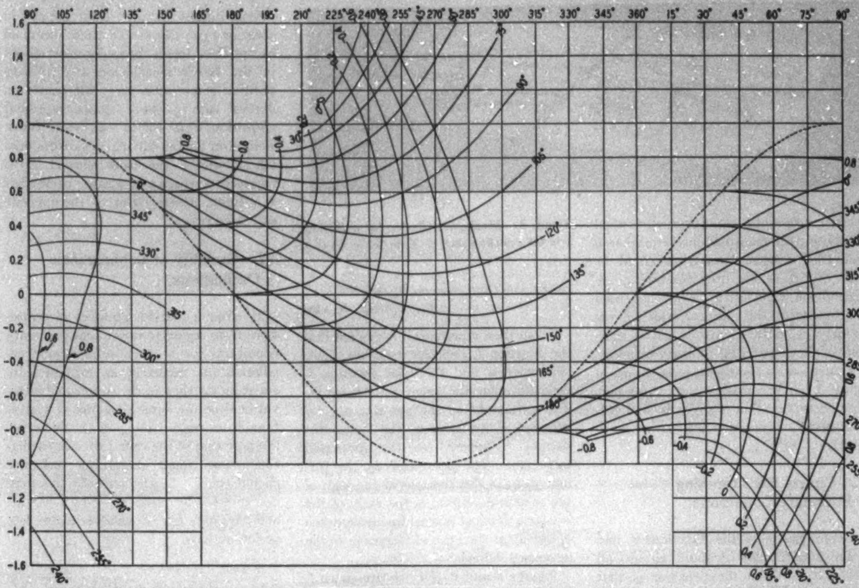

Figure 5. Chart of the function $f_s(Q_k)$

Contours give loci of $Q_{k+1} \underline{/\beta_{k+1}}$ for Q_k = constant and β_k = constant ($LC = 2.5$)

represents a functional relationship which must be satisfied by a certain number-pair (Q,β). The relationship could equally well be written

$$\left.\begin{aligned} Q &= u(Q,\beta) \\ \beta &= v(Q,\beta) \end{aligned}\right\} \quad (18)$$

A sufficient condition that the method of successive approximations shall yield a convergent solution when applied to these equations is developed in appendix III. In terms of charge and phase angle the set of conditions for stability of a subharmonic oscillation is expressed in terms of a set of restrictions on the partial derivatives $\partial Q_{k+1}/\partial Q_k$, $\partial Q_{k+1}/\partial \beta_k$, $\partial \beta_{k+1}/\partial Q_k$, $\partial \beta_{k+1}/\partial \beta_k$, $\partial \beta_{m+1}/\partial Q_m$, $\partial \beta_{m+1}/\partial \beta_m$.

The labor of carrying out a comprehensive analytical investigation of the behavior of these partial derivatives would be prohibitive. They can be evaluated in any particular case by graphical methods, however, with relatively little difficulty.

The solid curves of figure 7 show a differential analyzer solution for the charge and flux linkage* in a circuit in which $\omega = 0.63$ and $\psi_s = 1.15$.

After the first few cycles the charge seems to be essentially periodic. The wave form from cycle to cycle changes slightly but the general nature of the oscillation is the same. Although the current is not shown it is evident that there are two current pulses of the same sign and approximately equal magnitude for each half cycle of the subharmonic oscillation. This wave form is characteristic of the oscillations commonly encountered in actual electric circuits. The dotted curve shows values of charge

Table I

	Point Number							
	1	2	3	4	5	6	7	8
Q_k		0.15		−0.81		−0.08		0.94
β_k		246°		132°		51°		302°
Q_m	0.15		−0.81		−0.08		0.94	
β_m	90°		58°		298°		232°	
$\frac{\partial Q_{k+1}}{\partial Q_k}$		−0.4		+0.3		−0.6		−0.43
$\frac{\partial Q_{k+1}}{\partial \beta_k}$		+0.7		−0.92		−0.53		+1.07
$\frac{\partial \beta_{k+1}}{\partial Q_k}$		+1.13		−1.82		−1.00		+1.5
$\frac{\partial \beta_{k+1}}{\partial \beta_k}$		+0.3		+0.32		+0.26		+0.6
$\frac{\partial Q_{m+1}}{\partial Q_m}$	+1.0		+1.0		+1.0		+1.0	
$\frac{\partial Q_{m+1}}{\partial \beta_m}$	0		0		0		0	
$\frac{\partial \beta_{m+1}}{\partial Q_m}$	−2.3		+1.05		+3.14		−0.42	
$\frac{\partial \beta_{m+1}}{\partial \beta_m}$	−1.0		+1.1		−1.0		+0.8	

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*The flat tops of the flux linkage curve are due to the fact that in the analyzer setup the saturated portion of the ψ versus i curve was drawn parallel with the axis, the ratio of ψ to i in this region being maintained by means of gears. The curve therefore represents flux linkage only for values of ψ less than ψ_s; it is included for convenience in determining phase relationships.

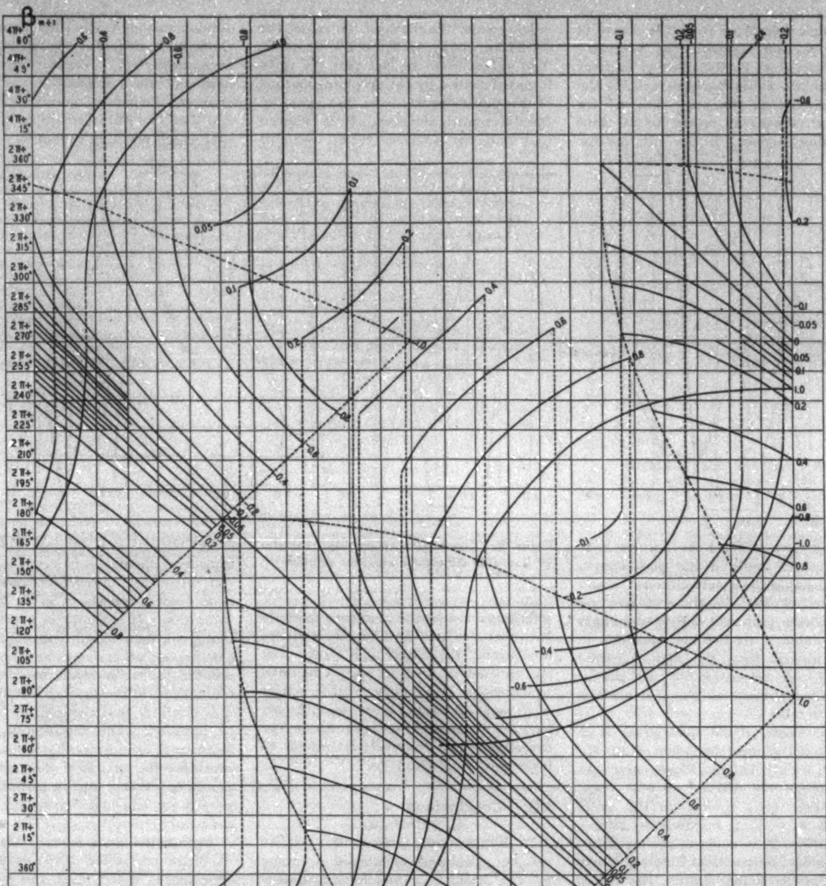

Figure 6. Chart for the function $f_a(Q_m)$

The contours shown are curves of β_{m+1} versus β_m for values of Q_m

and angle calculated from the charts of f_s and f_w, starting with the boundary conditions given by the analyzer at point 1. The two methods check well for the first period after this point, less well for the next period, and poorly for the third succeeding period. If the criterion for stability were satisfied the two methods should check well; if the phenomenon were unstable the result obtained would be expected, that is, the less accurate method (the graphical one) would cause the instability to become apparent in fewer periods than the more accurate method. A tabulation of the partial derivatives throughout the first cycle is shown in table I.

From these partial derivatives it is possible to calculate the values of $\partial u/\partial Q$, $\partial u/\partial \beta$, $\partial v/\partial Q$, $\partial v/\partial \beta$. These values are functions of the derivatives $\partial Q_{k+1}/\partial Q_k$, $\partial Q_{k+1}/\partial \beta_k$, etc., similar to the polynomials A, B, C, D in equations 29 if n of equations 29 is made to correspond to the number of transitions involved in one period of the subharmonic. The values calculated for a half-period (since the two half-periods are alike the stability criterion should apply equally well to a half or a whole period) are:

$$\frac{\partial u}{\partial Q} = 1.56 \qquad \frac{\partial v}{\partial Q} = 1.67$$

$$\frac{\partial u}{\partial \beta} = 0.15 \qquad \frac{\partial v}{\partial \beta} = 0.93$$

It is obvious that the *sufficient condition for stability* is violated; note that this condition is not *necessary*. A consideration of the polynomials A, B, C, D shows that a *sufficient* condition for *instability* is that all partial derivatives have the same sign and that at least one of the derivatives $\partial u/\partial Q$, $\partial v/\partial \beta$ be in absolute value greater than unity. Therefore the oscillation shown in figure 7 is unstable. This agrees with the fact that the wave form calculated by the graphical method of this paper departs from that given by the differential analyzer. It is believed that had the analyzer record been of sufficient duration the charge wave would have drifted into a different mode of oscillation. Experience has shown that the duration of analyzer record required to accomplish this is quite great (a matter of days at the rate of five minutes per cycle of impressed voltage) hence the analyzer is not particularly suited to investigations of this type.

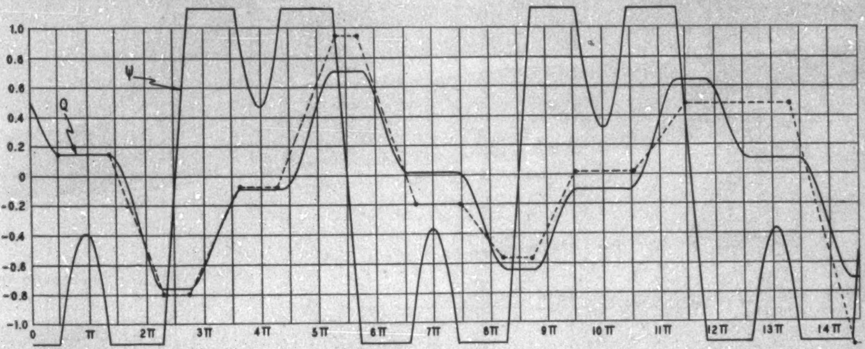

Figure 7. Comparison of graphical calculation with differential analyzer solution

VII. Conclusions

1. Subharmonic oscillations, if stable, can be calculated with good accuracy by the method of matching boundary conditions providing the circuit is such as to admit of the idealizing assumptions 1 to 4 given in the introduction. The labor of carrying out such calculations is not prohibitive if the solution of the transcendental equations involved is done by mechanical means. If the oscillation is unstable it does not seem probable that any analytic method can be developed which will reproduce experimentally observed wave forms.

2. The theory developed in appendix III provides a means for investigating the stability of a given mode of subharmonic oscillation. The application of criteria arising from this theory to specific examples leads to the belief that stable subharmonic oscillations are rare. This opinion may be colored by the fact that throughout this analysis resistance has been neglected. Whether any given harmonic which fulfills the condition of recurrent boundary conditions is stable and therefore will be sustained is a question which probably cannot be answered completely by any analysis which neglects dissipation.

3. The effect of resistance on stability is of greatest importance in the region of saturation. It is shown in appendix IV that a resistance of sufficient size to damp out the oscillation due to its effect in the unsaturated region alone, would so limit the current in the saturated region that it is doubtful that the oscillation would be initiated. The modification of the function f_s to include dissipation should generalize the method of this paper sufficiently to render it capable of determining wave form and stability in any circuit commonly encountered. The addition of exponential cams to vary the radius EG of the mechanical device described in appendix II would make this extension possible.

4. Throughout this analysis the saturation curve has been approximated by two straight lines. This approximation does not introduce appreciable errors in wave form for the first cycle after any given set of boundary conditions. It cannot be stated conclusively however that this approximation has no effect on stability. At least one example can be pointed out (equation 3 of reference 1) in which the stable subharmonic depends upon a particular shape of saturation curve. In this example, however, the two straight lines would not represent a good approximation to the curve and therefore would not be expected to produce the same character of oscillation.

Appendix I

Nature of the Function f.

Consider the equation 4. If, at $t = 0$, di/dt is positive, we have a condition corresponding to entrance into the *positive* saturation region. If, on the other hand, di/dt is negative, the condition corresponds to that which obtains at entrance into the *negative* saturated region.

If $di/dt = 0$, we have the limiting condition at which a saturated value $\pm\psi_s$ is just reached but not entered so that no change in charge results. From equation 1 it is evident that for $di/dt = 0$ when $t = 0$ the following relation holds:

$$Q_k = CE \sin \beta_k \tag{19}$$

Since this represents a condition of contact with a saturated region, but zero time of operation in the saturated region, the corresponding values of Q_{k+1} and β_{k+1} are

$$Q_{k+1} = Q_k$$
$$\beta_{k+1} = \beta_k$$

hence

$$Q_{k+1} = CE \sin \beta_{k+1} \tag{20}$$

that is, the circle in the Q_k plane given by equation 19 transforms into the circle in the Q_{k+1} plane given by equation 20. A point Q_k which satisfies (19) represents a point of contact with the positive saturated region when the curve of current versus time is concave down. This is the case providing d^2i/dt^2 is negative or, from equation 19, providing $\cos \beta_k < 0$. A point representing such a contact with the positive saturated region may also be regarded as a limiting condition of operation in the negative saturated region. In this case it represents the angle which for a given charge Q_k produces the longest possible excursion in the negative saturated region.

All contours in the Q_k plane crossing the contour $Q_k = CE \sin \beta_k$ yield contours in the Q_{k+1} plane which are discontinuous at the contour Q_{k+1} at values of β_{k+1} in the range 90 degrees $< \beta_{k+1} <$ 270 degrees correspond to operation in the positive saturated region. Those contours terminating on that contour at values of β_{k+1} in the range -90 degrees $< \beta_{k+1} <$ 90 degrees correspond to operation in the negative saturated region. Since the circle $Q = CE \sin \beta$ is traced over twice as β varies through 360 degrees all points have dual physical interpretations.

The above is illustrated in figure 2. In this figure three contours in the Q_k plane, the circle $Q_k = CE \sin \beta_k$, the circle $Q_k = 2$, the straight line $\beta_k = 120$ degrees for $4.32 > Q_k > -\infty$, are shown. The constants of the circuit are $L = 0.5$, $C = 5.0$, and $E = 1.0$.

Appendix II

Mechanical Solution of a Trigonometric Equation

Referring to figure 4, $ABCD$ and $EFGH$ are parallelograms with pivots at each corner. θ is the angle through which the arms BC and AD have been rotated in a clockwise direction from the center line C_1. α is the angle through which the arms EG and FH have been rotated in a counterclockwise direction from the center line C_2. It is evident from the geometry that

$$x = \overline{BC} \sin \theta \tag{21}$$

$$y = \overline{EG} \sin \alpha \tag{22}$$

S is a scale carried on EF extended, the zero of the scale being located at a distance from E equal to the distance between the center lines C_1 and C_2. Since the scale moves to the left and the bar CD moves to the right, the reading R of the bar on the scale is the sum of the displacements x and y.

$$R = x + y = \overline{BC} \sin \theta + \overline{EG} \sin \alpha \tag{23}$$

The protractor P which is used to measure θ is belted to a pulley of equal diameter to which is pinned a gear having T_1 teeth. This gear meshes with a gear having T_2 teeth which is attached to the arm EG. It is evident that α could be set equal to zero and θ assigned any arbitrary value β before the gears are meshed. Then we have

$$\alpha = \frac{T_1}{T_2}(\theta - \beta) \tag{24}$$

or substituting in (23)

$$\sin \theta + \frac{\overline{EG}}{\overline{BC}} \sin\left(\frac{T_1}{T_2}\theta - \frac{T_1}{T_2}\beta\right) = \frac{R}{\overline{BC}} \tag{25}$$

$\overline{BC}$ is 10 centimeters, $\overline{EG}$ is adjustable on a centimeter scale, R is read on a centimeter scale, and the gear ratio $T_1{:}T_2$ is adjustable. Hence we may set up the equation

$$\sin \theta + X \sin (k\theta + \phi) = Y \tag{26}$$

by first setting X, k, and ϕ, and then moving the mechanism as a whole until Y is read on the scale S. The corresponding reading of the protractor is θ.

Appendix III

On the Solution of a Pair of Equations by the Method of Successive Approximations

Given the two equations

$$\left.\begin{aligned} x &= u(x,y) \\ y &= v(x,y) \end{aligned}\right\} \tag{27}$$

two sequences of numbers $\alpha_0, \alpha_1, \alpha_2 \ldots \alpha_n$ and $\beta_0, \beta_1, \beta_2 \ldots \beta_n$ may be defined by the scheme

$$\begin{aligned} \alpha_1 &= u(\alpha_0, \beta_0) \\ \beta_1 &= v(\alpha_0, \beta_0) \\ \alpha_2 &= u(\alpha_1, \beta_1) \\ \beta_2 &= v(\alpha_1, \beta_1) \\ &\cdots\cdots\cdots \\ &\cdots\cdots\cdots \\ \alpha_n &= u(\alpha_{n-1}, \beta_{n-1}) \\ \beta_n &= v(\alpha_{n-1}, \beta_{n-1}) \end{aligned}$$

By application of the mean value theorem we have:

$$\alpha_2 - \alpha_1 = u(\alpha_1, \beta_1) - u(\alpha_0, \beta_0) = \left.\frac{\partial u}{\partial x}\right]_{a_1,b_1}(\alpha_1 - \alpha_0) + \left.\frac{\partial u}{\partial y}\right]_{a_1,b_1}(\beta_1 - \beta_0)$$

$$\alpha_3 - \alpha_2 = u(\alpha_2, \beta_2) - u(\alpha_1, \beta_1) = \left.\frac{\partial u}{\partial x}\right]_{a_2,b_2}(\alpha_2 - \alpha_1) + \left.\frac{\partial u}{\partial y}\right]_{a_2,b_2}(\beta_2 - \beta_1)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\begin{matrix} \alpha_0 < a_1 < \alpha_1 & \alpha_1 < a_2 < \alpha_2 & \cdots\cdots\cdots \\ \beta_0 < b_1 < \beta_1 & \beta_1 < b_2 < \beta_2 & \cdots\cdots\cdots \end{matrix}$$

$$\beta_2 - \beta_1 = v(\alpha_1, \beta_1) - v(\alpha_0, \beta_0) = \left.\frac{\partial v}{\partial x}\right]_{a_1^1,b_1^1}(\alpha_1 - \alpha_0) + \left.\frac{\partial v}{\partial y}\right]_{a_1^1,b_1^1}(\beta_1 - \beta_0)$$

$$\beta_3 - \beta_2 = v(\alpha_2, \beta_2) - v(\alpha_1, \beta_1) = \left.\frac{\partial v}{\partial x}\right]_{a_2^1,b_2^1}(\alpha_2 - \alpha_1) + \left.\frac{\partial v}{\partial y}\right]_{a_2^1,b_2^1}(\beta_2 - \beta_1)$$

$$\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots\cdots$$

$$\begin{matrix} \alpha_0 < a_1^1 < \alpha_1 & \alpha_1 < a_2^1 < \alpha_2 & \cdots\cdots \\ \beta_0 < b_1^1 < \beta_1 & \beta_1 < b_2^1 < \beta_2 & \cdots\cdots \end{matrix}$$

Let us suppose that the region under consideration is sufficiently small that partial derivative may be assumed to be constant.

$$\frac{\partial u}{\partial x} = U_x, \quad \frac{\partial u}{\partial y} = U_y, \quad \frac{\partial v}{\partial x} = V_x, \quad \frac{\partial v}{\partial y} = V_y \tag{28}$$

From the above it is possible to express $(\alpha_n - \alpha_{n-1})$ and $(\beta_n - \beta_{n-1})$ in terms of $(\alpha_1 - \alpha_0)$ and $(\beta_1 - \beta_0)$, for example

$$(\alpha_4 - \alpha_3) = (\alpha_1 - \alpha_0)(U_x^3 + 2U_xU_yV_x + U_yV_xV_y) + (\beta_1 - \beta_0)(U_x^2U_y + U_xU_yV_y + U_y^2V_x + U_yV_y^2)$$

$$(\beta_4 - \beta_3) = (\alpha_1 - \alpha_0)(U_x^2V_x + U_yV_x^2 + U_xV_xV_y + V_xV_y^2) + (\beta_1 - \beta_0)(U_xU_yV_x + 2U_yV_xV_y + V_y^3)$$

In general we have:

$$\begin{aligned} (\alpha_n - \alpha_{n-1}) &= (\alpha_1 - \alpha_0)A + (\beta_1 - \beta_0)B \\ (\beta_n - \beta_{n-1}) &= (\alpha_1 - \alpha_0)C + (\beta_1 - \beta_0)D \end{aligned} \tag{29}$$

in which A, B, C, and D are all homogeneous polynomials in U_x, U_y, V_x, V_y, each of degree $(n - 1)$ and each containing 2^{n-2} terms. Let the greatest term in any of these polynomials be

$$U_x^a\, U_y^b\, V_x^c\, V_y^d = M^{n-1} \quad (a + b + c + d = n - 1) \tag{30}$$

then we have

$$\alpha_n - \alpha_{n-1} < \frac{(\alpha_1 - \alpha_0) + (\beta_1 - \beta_0)}{2}(2M)^{n-1}$$

$$\beta_n - \beta_{n-1} < \frac{(\alpha_1 - \alpha_0) + (\beta_1 - \beta_0)}{2}(2M)^{n-1}$$

$$\left.\begin{aligned} \lim_{n \to \infty} \alpha_n - \alpha_{n-1} &= 0 \\ \lim_{n \to \infty} \beta_n - \beta_{n-1} &= 0 \end{aligned}\right\} M < \frac{1}{2} \tag{31}$$

Hence the sequence $\alpha_0, \alpha_1 \ldots \alpha_n$ approaches a limit and the sequence $\beta_0, \beta_1 \ldots \beta_n$ likewise approaches a limit providing $M < \frac{1}{2}$. Let the limits be respectively X and Y. Then

$$X = u(X,Y)$$
$$Y = v(X,Y)$$

It follows therefore that, assuming any arbitrary initial values α_0, β_0, the equations 27 may be solved by successive approximations providing

$$\left|\frac{\partial u}{\partial x}\right|^a \cdot \left|\frac{\partial u}{\partial y}\right|^b \cdot \left|\frac{\partial v}{\partial x}\right|^c \cdot \left|\frac{\partial v}{\partial y}\right|^d \leqq M^{a+b+c+d},\ M < \frac{1}{2} \qquad (32)$$

for all a, b, c, d.

Clearly this condition is satisfied if

$$\left|\frac{\partial u}{\partial x}\right| < M,\ \left|\frac{\partial u}{\partial y}\right| < M,\ \left|\frac{\partial v}{\partial x}\right| < M,\ \left|\frac{\partial v}{\partial y}\right| < M \qquad (33)$$

Note that U_x and V_y are dimensionless and are therefore independent of the units in which the quantities x and y are expressed, whereas U_y and V_x are dependent upon the units. An investigation of the polynomials A, B, C, D shows that in any term in which either U_y or V_x occurs the degree of the term in V_x is equal to the degree in U_y or to that degree plus or minus one. Hence in the application of the theorem to physical problems in which scale factors enter, the criterion can be rendered independent of units by requiring that

$$\left|\frac{\partial u}{\partial y}\right| \cdot \left|\frac{\partial v}{\partial x}\right| < M^2 \qquad (34)$$

If it is desired to apply the criterion in the form in which it appears in the inequalities (33), it is necessary first to put the physical equations in such form that the variables themselves are dimensionless.

Appendix IV

Effect of Resistance on Stability

With an initial charge on the capacitor or with a switching angle such as to swing ψ beyond saturation the phenomenon will always start. Since in the unsaturated region the charge is trapped, the flux linkages will have a component approximately equal to $-v_0 t$. In order for the phenomenon to stop it would be necessary for a current pulse completely to eliminate the charge on the capacitor and at the same time for the angle of emergence from the saturated region to correspond to a voltage peak, or for the attenuation due to resistance in the unsaturated region to be such as to reduce ψ to a size less in absolute value than ψ_s.

Consider the possibility of the latter case. Referring to figure 5 in reference 1 it is seen that at the instant T_{k-1}, the charge is $-Q_k$. If instead of assuming infinite inductance along the unsaturated portion of the characteristic, an inductance L is assumed, we have a current at the instant T_{k-1} equal to $-\psi_s/L$. The equation which holds for the succeeding interval of time is

$$L\frac{d^2q}{dt^2} + r\frac{dq}{dt} + \frac{1}{c}q = E \sin t \qquad (35)$$

This equation is subject to the initial conditions

$$t = T_{k-1} = 2r\pi + \gamma \qquad q = -Q_k$$
$$i = -\frac{\psi_s}{L}$$

Let us change the origin of time to the instant of leaving the negative saturated region. Then the electromotive force is $E \sin (t + \gamma)$ and $t = 0$ initially. Since the inductance L is very large, the *steady-state current* may be taken as $(-E/L) \cos t$, hence the total charge and current are

$$q = A\epsilon^{\alpha t} \sin (\omega t + \theta) - \frac{E}{L} \sin (t + \gamma) \qquad (36)$$

$$i = A\epsilon^{\alpha t} \sin (\omega t + \theta) + \omega A\epsilon^{\alpha t} \cos (\omega t + \theta) - \frac{E}{L} \cos (t + \gamma) \qquad (37)$$

where

$$\alpha = -\frac{r}{2L}$$

$$\omega = \sqrt{\frac{1}{LC} - \frac{r^2}{4L^2}} = \frac{1}{\sqrt{LC}} \text{ approximately since } \frac{r^2}{4L^2} << \frac{1}{LC}$$

We have the boundary conditions

$$t = 0 \qquad q = -Q \qquad i = -\frac{\psi_s}{L} \qquad (38)$$

which when substituted in (36) and (37) yield

$$\theta = \sin^{-1}\frac{E \sin \gamma - LQ}{LA} \qquad (39)$$

$$A = \sqrt{\frac{C}{L}} \times \sqrt{E^2 - 2E(\psi_s \cos\gamma + LQ \sin\gamma) + \psi_s{}^2 + L^2Q^2} \qquad (40)$$

Equation 40 is subject to the condition

$$\frac{r}{L}(E \cos \gamma - \psi_s)(E \sin \gamma - LQ) \rightarrow 0$$

which is justifiable because in normal circuits

$$L \geqq 40 \quad \psi_s < 1.5 \quad E\sin\gamma \overset{=}{<} 1 \quad E\cos\gamma \overset{=}{<} 1$$

For the subharmonic oscillation to be damped out $|\psi|$ must become and remain less than ψ_s.

Since $\psi = Li$ we have

$$\psi = LA\epsilon^{\alpha t}\,[(\alpha \sin \theta + \omega \cos \theta) \cos \omega t + (\alpha \cos \theta - \omega \sin \theta) \sin \omega t] - E \cos (t + \gamma) \qquad (41)$$

The denominator in equation 39 is a large quantity; let us neglect $\sin \theta$ to a first approximation, then

$$\psi = \sqrt{LC} \times \sqrt{E^2 - 2E(\psi_s \cos\gamma + LQ\sin\gamma) + \psi_s{}^2 + L^2Q^2} \times \epsilon^{-\frac{rT}{2L}}\left[-\frac{r}{2L}\sin \omega t + \frac{1}{\sqrt{LC}}\cos \omega t\right] - E \cos (t + \gamma) \qquad (42)$$

It is evident that for subharmonics having long periods the resistance has greater effect in the unsaturated region than for oscillations of short periods. Let us calculate the value of resistance which would make impossible all oscillations having periods greater than 10π. The following numerical values represent a typical circuit:

$t = 3\pi$ (approximately) $C = 5$ $L = 40$
$\cos (t + \gamma) = -1$(approximately) $Q = 1$
$\psi_s = 1.15$

These yield the transcendental equation

$$1.15 = 209\epsilon^{-0.117r}[-0.0077r + 0.055] + 1 \qquad (43)$$

Solving (43), we have $r = 7.1$, a value so great that the oscillation would be eliminated due to attenuation in the current flow interval long before this limit were reached. This suggests that for oscillations of comparatively short period the damping in the saturated region is far more important than that in the unsaturated region. Hence the inclusion of the effect of resistance in the charts for the function $f_s(Q_k)$ might lead to values of partial derivatives which fulfill stability criteria.

A small resistance would eliminate very long periods due to damping in the unsaturated region and very short periods due to reduction of the current pulse during operation in the saturated region. It appears that as the resistance increases there may be one intermediate harmonic which would be last eliminated, the subharmonics on either side being successively cut off. This may mean that there is a most stable mode of oscillation for a given circuit, and that, although theoretical considerations thus far presented indicate infinitely many steady states, there is one subharmonic oscillation most likely to be produced regardless of varying boundary conditions.

References

1. Subharmonics in Circuits Containing Iron-Cored Reactors, Irven Travis and C. N. Weygandt. AIEE Transactions, 1938. (A comprehensive bibliography is contained in this paper.)

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The Generalized Solution for the Critical Conditions of the Ferroresonant Parallel Circuit

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Synopsis: In a recent paper "Similitude of Critical Conditions in Ferroresonant Circuits,"[24] a generalized solution for the critical conditions of the series circuit was given. The same type of generalized treatment when applied to the parallel circuit yields a number of interesting conclusions to supplement those found for the series circuit.

Characteristics of the Parallel Circuit

FIGURE 1 represents the typical volt-ampere relation of the parallel circuit. Unlike the series circuit with its voltage-sensitive characteristics, the parallel circuit is known to be current sensitive[12,23] in the critical region.

It is noted here that the critical current is greatly dependent on the value of X_c used, and as in the series circuit, this current increases with a decrease of X_c. Also, the critical voltage increases with a decrease of X_c. The effect of varying the resistance is most noticeable in the critical region, the reduction of this resistance increasing the current dip found in such circuits.

Generalized Treatment of the Parallel Circuit

The circuit referred to in this investigation is shown in figure 2. The critical conditions are defined by the following equation:

$$\frac{di_0}{dE} = 0 \tag{1}$$

where i_0 represents the total line current. Referring again to figure 1, it should be apparent that with a resistance less than the critical value, the above relation is satisfied by two values of current and voltage, while with a critical resistance in the circuit these two values of current and voltage approach a single value.

Rewriting equation 1,

$$\frac{di_0}{dE} = \frac{di_0}{di_L} \cdot \frac{di_L}{dE} = 0 \tag{2}$$

Since di_L/dE can never become zero, $di_0/di_L = 0$ will satisfy the above relation. Generalizing, the critical points will be defined by the following equation:

$$\frac{d\left(\frac{ni_0}{l}\right)}{d\left(\frac{ni_L}{l}\right)} = 0 \tag{3}$$

It is now necessary to express ni_0/l in terms of ni_L/l. Using the same notation as that of the series circuit, the reactance and the apparent resistance of the reactor can be expressed as follows (reference 24, equations 5 and 6):

$$X_L = \frac{E_{LX}}{i_L} = \frac{nA\beta}{i_L} = \left(\frac{n^2A}{l}\right)\frac{\beta}{\left(\frac{ni_L}{l}\right)} \tag{4}$$

$$R_L = \frac{E_{LR}}{i_L} = \frac{nA\alpha}{i_L} = \left(\frac{n^2A}{l}\right)\frac{\alpha}{\left(\frac{ni_L}{l}\right)} \tag{5}$$

where:

E_{LX} = reactive component of the reactor voltage
E_{LR} = in-phase component of the reactor voltage
n = number of turns on the reactor
A = cross-section area of the magnetic path
l = length of the magnetic path
β = $4\gamma f B_{\max} \times 10^{-8}$ = a function of (ni_L/l)
γ = form factor
α = $\rho\frac{\left(\frac{W}{\#}\right)}{K\left(\frac{ni_L}{l}\right)}$ = a function of (ni_L/l)
$\left(\frac{W}{\#}\right)$ = watts per pound of iron
ρ = $\left(\frac{\#}{Al}\right)$ = density of iron used
K = i_1/i_L = ratio of fundamental to effective current

Since

$$i_0 = i_L \cdot \frac{Z_1 + Z_2}{Z_2} \tag{6}$$

$$\frac{ni_0}{l} = \frac{ni_L}{l} \times \sqrt{\frac{\left[R_1 + R_2 + \left(\frac{n^2A}{l}\right)\frac{\alpha}{\left(\frac{ni_L}{l}\right)}\right]^2 + \left[\left(\frac{n^2A}{l}\right)\frac{\beta}{\left(\frac{ni_L}{l}\right)} - X_c\right]^2}{R_2^2 + X_c^2}}$$

$$\frac{ni_0}{l} = \sqrt{\frac{\left[\alpha + \left(\frac{ni_L}{l}\right)\left(\frac{R_1 + R_2}{\frac{n^2A}{l}}\right)\right]^2 + \left[\beta - \left(\frac{ni_L}{l}\right)\left(\frac{X_c}{\frac{n^2A}{l}}\right)\right]^2}{\left(\frac{R_2}{\frac{n^2A}{l}}\right)^2 + \left(\frac{X_c}{\frac{n^2A}{l}}\right)^2}} \tag{7}$$

Table I. Data for Reactor 5

$n = 800$ $(nA) = 760$
$A = 0.95$ square inch $\left(\frac{n^2A}{l}\right) = 83{,}400$
$l = 7.3$ inches $\left(\frac{n}{l}\right) = 109.7$

Standard Circuit ($R_1 = 0$; $R_2 = 104$ ohms)		New Circuit ($R_1 = 104$ ohms; $R_2 = 0$)	
E	**I_0**	**E**	**I_0**
63	0.20	58	0.20
94	0.30	87	0.30
120	0.35	106	0.35
142	0.40	124	0.40
155	0.41	160	0.45
185	0.415	182	0.46
196	0.415	200	0.46
205	0.42	210	0.47
214	0.45	229	0.50
220	0.50	255	0.60
229	0.60		

Critical stable values

E	185	E	190
I_0	0.415	I_0	0.46
I_L	0.347	I_L	0.345
R	104	R	104
X_c	268	X_c	268
$\left(\frac{E}{nA}\right)$	0.243	$\left(\frac{E}{nA}\right)$	0.250
$\left(\frac{ni_0}{l}\right)$	45.5	$\left(\frac{ni_0}{l}\right)$	50.5
$\left(\frac{ni_L}{l}\right)$	38.0	$\left(\frac{ni_L}{l}\right)$	37.9
$\left(\frac{R}{\frac{n^2A}{l}}\right)$	0.00125	$\left(\frac{R}{\frac{n^2A}{l}}\right)$	0.00125
$\left(\frac{X_c}{\frac{n^2A}{l}}\right)$	0.00321	$\left(\frac{X_c}{\frac{n^2A}{l}}\right)$	0.00321

Paper number 39-75, recommended by the AIEE committee on basic sciences, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted September 19, 1938; made available for preprinting December 29, 1938.

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[24] For all numbered references, see list at end of paper.

Differentiation and equating it to zero,

$$\frac{d\left(\frac{ni_0}{l}\right)}{d\left(\frac{ni_L}{l}\right)} =$$

$$\frac{1}{\sqrt{\left(\frac{R_2}{\frac{n^2A}{l}}\right)^2\left(\frac{X_c}{\frac{n^2A}{l}}\right)^2}}\left\{\frac{\left[\alpha+\left(\frac{ni_L}{l}\right)\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)\right]\left[\frac{d\alpha}{d\left(\frac{ni_L}{l}\right)}+\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)\right]+\left[\beta-\left(\frac{ni_L}{l}\right)\left(\frac{X_c}{\frac{n^2A}{l}}\right)\right]\left[\frac{d\beta}{d\left(\frac{ni_L}{l}\right)}-\left(\frac{X_c}{\frac{n^2A}{l}}\right)\right]}{\sqrt{\left[\alpha+\left(\frac{ni_L}{l}\right)\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)\right]^2+\left[\beta-\left(\frac{ni_L}{l}\right)\left(\frac{X_c}{\frac{n^2A}{l}}\right)\right]^2}}\right\} = 0$$

Therefore,

$$\left\{\alpha+\left(\frac{ni_L}{l}\right)\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)\right\}\left\{\frac{d\alpha}{d\left(\frac{ni_L}{l}\right)}+\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)\right\} = -\left\{\beta-\left(\frac{ni_L}{l}\right)\left(\frac{X_c}{\frac{n^2A}{l}}\right)\right\}\left\{\frac{d\beta}{d\left(\frac{ni_L}{l}\right)}-\left(\frac{X_c}{\frac{n^2A}{l}}\right)\right\} \quad (8)$$

It is interesting to note that equation 8 is essentially identical to equation 9 of the series circuit (see equation 9, reference 24).

The generalized voltage is obtained by the following equation:

$$E^2 = \left\{nA\alpha+\left(\frac{ni_L}{l}\right)\frac{R_1}{\left(\frac{n}{l}\right)}\right\}^2+\{nA\beta\}^2$$

$$\left\{\frac{E}{nA}\right\}^2 = \left\{\alpha+\left(\frac{ni_L}{l}\right)\left(\frac{R_1}{\frac{n^2A}{l}}\right)\right\}^2+\{\beta\}^2 \quad (9)$$

The Principle of Similitude of Parallel Circuits

From equations 7, 8, and 9 a number of conclusions can be drawn. The following apply to the critical stable condition.

1. Comparison of equation 8 with the corresponding equation for the series circuit (equation 9 in reference 24) indicates that the critical stable value of ni_L/l and $\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)$ are equal to those of the series circuit.

2. From equation 8 it is possible to state that two different circuits with equal values of $\left(\frac{X_c}{\frac{n^2A}{l}}\right)$ will have equal values of $\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)$ and the ampere turns per inch of the reactor branch for the critical stable condition will be equal.

3. Since the only resistance term appearing in equation 8 is $\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)$, as far as the critical value of $\left(\frac{ni_L}{l}\right)$ is concerned, it matters not whether the resistance is placed all in one branch or divided between the two branches.

4. When $R_1 = 0$, equations 8 and 9 indicate that the critical stable value of $\left(\frac{E}{nA}\right)$ depend only on the value of $\left(\frac{X_c}{\frac{n^2A}{l}}\right)$.

5. Using $R_1 = 0$ as a standard parallel circuit, a generalized curve for $\left(\frac{E}{nA}\right)$, $\left(\frac{R_1+R_2}{\frac{n^2A}{l}}\right)$, and $\left(\frac{ni_0}{l}\right)$ can be drawn with $\left(\frac{X_c}{\frac{n^2A}{l}}\right)$ as the independent variable. This generalized curve for the standard circuit can then be used to calculate the critical stable values of $\left(\frac{E}{nA}\right)$ and $\left(\frac{ni_0}{l}\right)$ for the case where R_1 is not equal to zero by means of the two equations below.

$$\left(\frac{ni_0}{l}\right)_{\substack{R_1\neq0\\R_2=0}} = \left(\frac{ni_0}{l}\right)_{\substack{R_1=0\\R_2\neq0}} \times \sqrt{\frac{\left(\frac{R_2}{\frac{n^2A}{l}}\right)^2+\left(\frac{X_c}{\frac{n^2A}{l}}\right)^2}{\left(\frac{X_c}{\frac{n^2A}{l}}\right)^2}} \quad (10)$$

$$\left(\frac{E}{nA}\right)^2_{\substack{R_1\neq0\\R_2=0}} = \left(\frac{E}{nA}\right)^2_{\substack{R_1=0\\R_2\neq0}} + \left\{\left(\frac{ni_L}{l}\right)\left(\frac{R_1}{\frac{n^2A}{l}}\right)\right\}^2 \quad (11)$$

Equation 10 is obtained directly from equation 7 while equation 11 is obtained from equation 9 by assuming α to be negligible in comparison to β.

Experimental Verification of Derived Conclusions

Critical stable values for parallel circuits were obtained experimentally and tabulated in table II. Reactors 1, 2, and 3 used for this purpose were made from the same grade of iron and are the same reactors used in the previous investigation of the series circuit.

The tabulated values of table II are plotted in figure 3 representing the generalized curves for the standard parallel circuit. For comparative purposes the

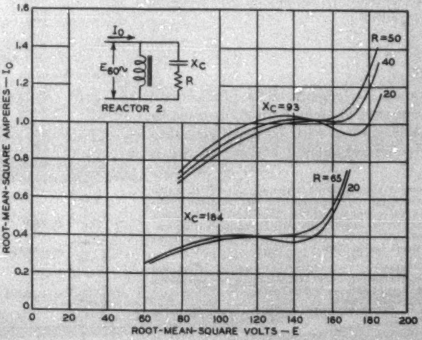


Figure 1. Characteristics of the parallel circuit

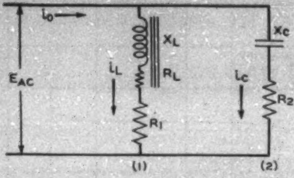


Figure 2. Ferroresonant parallel circuit

values of $\left(\frac{ni_L}{l}\right)$ and $\left(\frac{R_0}{\frac{n^2A}{l}}\right)$ of the series circuit (see reference 24, figure 4) for the same grade of iron are superimposed in dotted lines. It is apparent from these curves that conclusions 1, 2, and 4 are in fairly good agreement.

In order to verify conclusions 3 and 5, the data in table I were taken. The grade of iron used for reactor 5 is unknown; however, this is immaterial for this verification.

It is noted that the critical stable condition is obtained in both cases with R = 104 ohms, regardless of whether the resistance is placed in the reactor branch or the capacitor branch. As indicated in (8), the values of $\left(\frac{ni_L}{l}\right)$ are found to be equal for both circuits.

The two terms differing in the two circuits are (ni_0/l) and (E/nA). The following predetermination will illustrate the use of equations 10 and 11 and indicate the validity of conclusion 5.

Table II. Critical Stable Values for the Parallel Circuit

(Tabulated Values—Figure 3)

X_c	E	I_0	I_L	R_2	$\frac{X_c}{\frac{n^2A}{l}}$	$\frac{E}{nA}$	$\frac{ni_0}{l}$	$\frac{ni_L}{l}$	$\frac{R_2}{\frac{n^2A}{l}}$
Reactor 1									
188	335	1.17	0.74	110	0.00411	0.246	39.3	24.8	0.0024
272	320	0.75	0.57		0.00595	0.235	25.1	19.1	
350	310	0.55	0.48	154	0.00765	0.228	18.4	16.1	0.00338
460	290	0.36	0.37		0.0100	0.213	12.1	12.4	
Reactor 2									
93	150	1.02	0.73	49	0.00535	0.248	29.3	21.0	0.00280
184	130	0.40	0.43	65	0.0106	0.215	11.5	12.3	0.00374
Reactor 3									
272	320	0.78	0.53	160	0.0039	0.264	44.8	30.4	0.0023
305	313	0.66	0.46		0.00437	0.259	37.9	26.4	

(See reference 24, table I, for reactor dimension.)

Using the values obtained for the standard circuit and applying equations 10 and 11,

$$\left(\frac{ni_0}{l}\right)_{\substack{R_1=104\\R_2=0}} = 45.5 \times \sqrt{\frac{(0.00125)^2 + (0.00321)^2}{(0.00321)^2}} = 45.5(1.072) = 48.8$$

Experimental value = 50.5

$$\left(\frac{E}{nA}\right)_{\substack{R_1=104\\R_2=0}} = \sqrt{(0.243)^2 + (38.0 \times 0.00125)^2} = \sqrt{0.06125} = 0.248$$

Experimental value = 0.250

The two curves used for this investigation are plotted in figure 4.

Applications of the Parallel Circuit

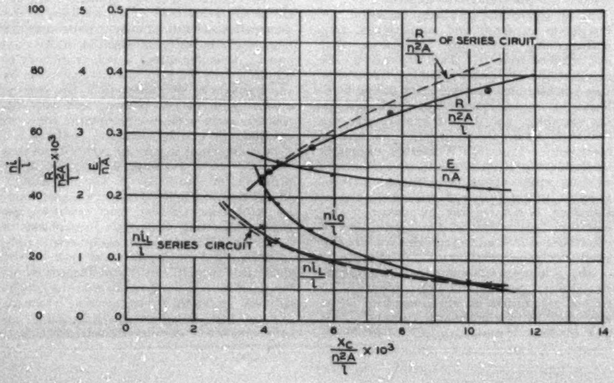
Although the applications of the parallel circuit are rather limited compared to those of the series circuit, the two examples below are worth mentioning.

The constant-current characteristics of the parallel circuit in the critical region can be utilized in a number of places where it is necessary to maintain a constant voltage in spite of the line-voltage fluctuation. This constant voltage can be obtained by placing a constant impedance in series with the parallel circuit tuned to the critical stable condition. Figure 5 represents the characteristics of such a circuit using a resistance for the series impedance. It is noted here that the line voltage may fluctuate as much as 60 volts without affecting the voltage across the resistor. This resistor may represent an instrument or any other device which must be kept at a constant voltage.

If the resistance R_2 in the parallel circuit is reduced to zero, the negative-slope characteristics of the parallel circuit can be utilized to advantage when applied to a resistance thermometer. The resistance thermometer in this case is placed in series with the parallel circuit. With this negative-slope characteristic, a reduction in the thermometer resistance is accompanied by a reduction in the current through it instead of an increase, thereby producing a greater change in the voltage across the thermometer for a given change in the resistance

Figure 3. Generalized curves for the parallel circuit

Allegheny dynamo-grade iron



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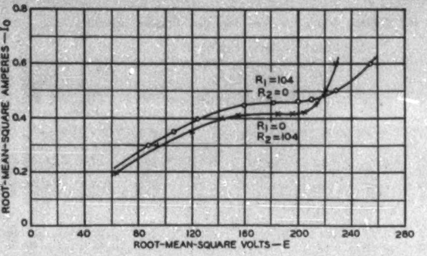

Figure 4. Characteristics of the parallel circuit

Reactor 5; $X_c = 268$

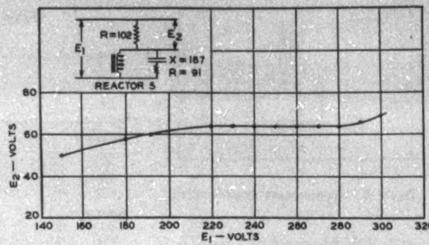

Figure 5. Constant-voltage circuit

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Discussion

Paul H. Odessey (Heyer Products Company, Belleville, N. J.): Mr. Thomson's interpretation of critical conditions through principles of similitude is of noteworthy engineering value. This method of approach happily overcomes the difficulty of dealing with harmonics although the analysis is formulated on the basis of fundamental frequency. The application of similitude principles is, however, dependent upon experimental results and, therefore, constitutes a point-to-point comparison between ferroresonant circuits employing the same grade iron. In wider application, it is essential to obtain by experiment with a standard circuit, a complete set of data covering the entire range of critical values, both stable and unstable before the constants of an unknown circuit can be determined for any condition. This limitation is, however, not objectionable for the results have proved to be uncommonly accurate, although not particularly descriptive of the phenomena in general.

As an implement of study, equation 8 is restrictive in form and application, notwithstanding important conclusions that are deduced from it. This derivative type of equation is, properly speaking, not a solution since it does not explicitly define a relation between the dependent and independent variables, nor does it characterize the influence of circuit parameters on critical conditions. Equation 8 merely expresses a relationship of interdependent parameters existing under critical conditions. A progressive interpretation of critical conditions therefore, is not feasible by means of this equation. Accordingly, the application of equation 8 is limited to problems solvable by similitude principles.

Mr. Thomson's conclusions are highly significant, particularly the identity of critical conditions in corresponding series and parallel ferroresonant circuits. This fact discloses a new approach to problems in critical conditions and embodies an analytical basis for further interpretation of the phenomena.

William T. Thomson: At present there is no exact solution to the ferroresonant problem. Every method developed so far is an approximation, and regardless of the method used recourse must be had to experimental results. For instance, the magnetization curve upon which all previous methods are based must be obtained experimentally and expressed mathematically or used in the graphical form.

The weaknesses of previous methods are twofold: (1) It is necessary to build the reactor first to obtain its characteristics before calculations for its critical points are possible. (2) The treatment is limited to the specific circuit under consideration, from which it is not possible to predict quantitatively the behavior of other circuits.

The type of information which is of greatest value must necessarily be of generalized character where the combined effect of varying the circuit parameters is capable of simple analysis. The generalized treatment with the principle of similitude makes possible this analysis.

This type of analysis is subject to criticism in that similar relations must exist in the two circuits to be compared. However, the generalization of any problem is subject to the same criticism. The accuracy of predetermination will, of course, depend on the degree of similarity obtained in the two circuits. This similarity is not as difficult to obtain as one might preassume, as shown by the agreement of values from the circuits analyzed in the two papers; and with reasonable care, accuracy acceptable for such calculations can be obtained easily.

In conclusion it may be pointed out that the solution of problems by the use of the principle of similitude is not uncommon. In the fields of hydrodynamics, aerodynamics, and heat transfer most problems are handled by such methods. In problems of flood control important conclusions are obtained by building models and applying the similitude principle in developing the model laws. The similitude principle is a powerful tool in handling engineering problems and the use of such methods should be encouraged in the various engineering fields.

Modernization of Switch-House Design

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THERE are many types and characters of switch houses on a power system, and their importance varies through wide limits. Many features which are of great importance and value in one type of installation cannot be justified in others. Probably the most important switching center is that of a generating station located near the center of a load area. In such a station continuity of service is of maximum importance. A complete shutdown of such a plant affects a great number of power users of all classes and the damage can be very great physically, commercially, and politically. This paper will be confined to a discussion of the means which have been used, or are available, to improve the reliability of this type of station.

Reliability of operation involves several factors. The possibility of the occurrence of a fault must be reduced to a minimum, the physical damage produced by a fault must be kept to a minimum, the spread of the fault must be prevented, and as far as possible an interruption of service to any consumer must be prevented.

The problem involves the physical design of the apparatus and its parts, the electrical arrangement of the gear and of the system, auxiliary equipment to cure ailments if they do occur, and the co-ordination of all into one coherent whole.

All electrical failures on a power system are caused by the application of a voltage between conducting materials in excess of the dielectric strength of the insulation between them. This may come about by the appearance of an abnormal voltage on the system whose value exceeds that of the insulation strength deemed necessary, or by the reduction of the strength below the voltages normally expected on the system. Economically sound voltage levels have been established by experience. The general level having been established, it is necessary to co-ordinate several factors to obtain a proper balance between all parts.

Each conductor on a three-phase system has a voltage between it and the other two phase conductors and ground. It is desirable, of course, to eliminate all types of faults but there is a gradation in this desirability. As will be pointed out later, faults involving two or more phases are usually more to be avoided than a single-phase-to-ground fault.

If the established-level insulation strength is obtained and maintained, then a failure is the result of some conducting element or an abnormal voltage entering the structure. The design of the structure must, therefore, prevent all conducting bodies, such as rats and other small animals, from reaching or approaching any live conductor.

Abnormal voltages are possible due to lightning, series resonance, or transient oscillations caused by the abrupt change of system conditions, such as the making or breaking of a circuit. These possibilities must be considered in the station design and steps taken to keep the voltage within reasonable limits.

Even though extreme care is used to obtain good insulation characteristics and to prevent foreign bodies and high voltages, it will be admitted by all that faults will occur. Therefore, the next consideration is to design the gear to minimize the damage which can result. A fault can cause physical damage by burning at the point of fault, by starting faults at other points, and by causing a flow of current in conducting material not designed to handle the flow. Burning at the point of fault is a function of current magnitude, its duration, and the character of the surrounding material. Trouble may be communicated from point to point by solid material thrown by an explosion, by vaporized metal, and by gases, which are the products of combustion. Consideration must then be given to the prevention of explosions and the confining of their effects if this is impossible. Metallic vapors are easily condensed but care must be used that they be confined until condensation is accomplished. Incandescent gases can be carried in a smoke cloud for considerable distances before the screen of smoke weakens and lets oxygen unite with the gas to start combustion. The cloud can deposit on insulators and reduce dielectric strength. Therefore, some attention is required in the design to provide for the cooling and confinement of these gases.

Fault currents have been known to flow in building steel and in control wiring with diastrous results. This flow of current has even caused the short-circuiting of other sound circuits with further damage and a further spread of the fault. So the prevention of this straying of fault current from suitable paths is worthy of considerable study in designing an adequate switchgear.

Care in the design of physical structure can accomplish a great deal in minimizing faults and damage due to a fault but if a fault can seriously disrupt service, then the care taken has been largely wasted. In order to complete the picture it is necessary to examine the electrical connections of the system to see what happens to the system. Provision for isolating the fault must, of course, be provided. This isolation must not cause a serious service interruption and it must be of a nature that system stability is not affected. Therefore, protective equipment and system design must be so co-ordinated that a fault cannot cause a severe system "shake-up" nor directly disconnect any important load.

Physical Structure

There have been many instances where switching stations have grown in capacity far beyond that contemplated in their original design. Such cases demand a serious reconsideration of their basic design and layout in order to accomplish objectives which become more clearly defined as a result of such growth.

The program for modernization of an existing switching structure is most commonly based on a desire to obtain one or more of the following:

1. Increased interrupting ability of oil circuit breakers.
2. Faster operation of oil circuit breakers.
3. Reduction in volume of oil.
4. Additional safety to operators.
5. Higher insulation level and phase segregation.
6. Greater reliability.

Interrupting Ability

Overstressed circuit breakers are commonly the first problem to be solved, either by substitution of entirely new breakers or, if of reasonably recent vintage, by modernization. Fortunately, new high-capacity breakers affording higher speed of operation, higher insulation level, and greater reliability are now available, requiring little if any more space

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Paper number 39-26, recommended by the AIEE committee on power generation, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 23, 1938; made available for preprinting December 14, 1938.

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than that occupied by old breakers to be replaced. In many cases these may be installed in cells already available.

Methods of modernizing oil circuit breakers have been widely discussed and will not be repeated here. The test of time and experience has proved that the proper application of arc control devices to most old-style breakers will result in greatly improved operation. In general, such changes increase interrupting ability, and because of shortened arcing time, greatly reduced contact burning and oil deterioration for any given service. This, of course, permits lengthening the period between necessary overhauls with a resultant decrease in maintenance expense. In most cases such changes can be made without altering the size of the structures in which the breakers are located, if only this one additional feature is required.

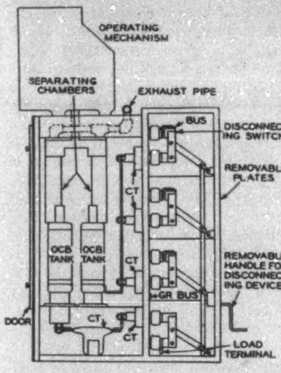

Figure 1. Cross section, metal-enclosed gear

CT—Current transformer

Speed of Operation

Circumstances under which a very short duration of the fault is most desirable will be discussed later. From the standpoint of circuit breaker operation, the element of time is broken down into two parts:

(*a*). Arcing time.

(*b*). Dead time.

With the addition of proper arc-control devices discussed above, minimum arcing time may be expected. Many old-style mechanisms are adequate to operate modernized breaker contacts, but have a dead time (defined as the interval from energizing the trip coil until contacts part) of several times the new arcing time. Marked reductions in dead time may usually be obtained by relatively simple modifications to the trip mechanisms, but these changes rarely permit reaching the minimum dead time provided by new mechanisms. Since most such old breakers were operated by non-trip-free mechanisms, modernization, particularly of higher-capacity circuit breakers, almost universally includes the addition of new high-speed trip-free mechanisms in order to secure maximum benefits. The performance which may be anticipated from a breaker modernized in this manner is practically the equivalent of that of a completely new breaker.

Reduction of Oil Hazard

The relation of oil quantity to fire hazard is a popular subject for discussion among those connected with the electrical industry and it is the general consensus of opinion that the search should continue for a noninflammable material for circuit-breaker use. A proper evaluation of the fire hazard in a station will by no means result in oil breakers rightly being singled out for criticism. The use and location of all combustible insulation material in the station or apparatus design and construction should receive equally careful attention. Under certain conditions the products of combustion of such materials may more seriously reduce the level of nearby insulation than those from oil itself.

Moderate-voltage low-oil-content breakers of high interrupting ability have been regularly used in large switching stations for many years, and have established a high level of performance and reliability. Since they are as economical of space as of oil, they find application in many cases of station modernization where it is necessary to scrap obsolete equipment, particularly where space is limited.

Development work is progressing rapidly on several types of high-capacity circuit breakers requiring no oil. Breakers of the same general configuration as the low-oil-content type in which water instead of oil acts as the interrupting medium, have been developed in ratings up to 1,500,000 kva at 15,000 volts. Tests have shown the same speed of operation and reliability of interruption as the low-oil-content breaker. Trial installations have been in service for several months, establishing a very satisfactory operating record. These breakers will fit into the same space as the low-oil-content type breakers referred to above and are favored by some users as a means of modernizing old breakers of that class.

Safety in Personnel

As long as those entrusted with the job of controlling the production and distribution of electrical energy are human beings, we may expect occasional cases of erratic and unexplainable performance. The examples of this limitation which come to attention most frequently involve the operation of the wrong disconnecting switches. At times these result in nothing more serious than tripping out one machine, or bus section, but too frequently in serious personal injury and considerable damage to nearby electrical equipment. A recent investigation of several cases disclosed the fact that in a majority of instances, the person involved was suffering from some kind of temporary emotional disturbance which apparently interferred with the mental processes accompanying even calm routine action to which he was accustomed. This is a condition which is definitely beyond the control of any employer, and from experience has been shown to affect even the higher-grade workers who are expected to think for themselves. It, therefore, appears obvious that a reduction in troubles of this kind can be expected only to the degree to which

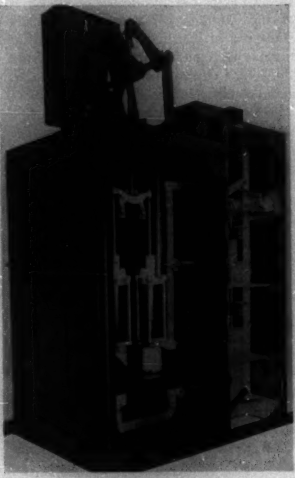

Figure 2. Metal-enclosed unit

foolproof mechanical safeguards are provided to assist the human mind when necessary, in making correct decisions.

The application of interlocks to ensure operation of correct disconnecting switches is by no means new. However, it has reached an all-time high for simplicity and effectiveness in modern metal-enclosed switchgear where six disconnecting switches are operated from one mechanism. This type of gear provides a simple, positive, and foolproof means of interlocking switches with the circuit breaker, to ensure proper sequence of operations. Gang operation safeguards against pulling two switches in one circuit and one in another. Cell-door interlocks prevent access to the circuit breaker until the switches are fully open and everything in the cell dead. And, finally, the operator is at all times separated from the switches which he is operating by a steel barrier.

Figure 1 is a cross section of such an equipment, showing the metal-enclosed busses, circuit breakers, disconnecting switches, and current transformers.

Insulation Level

Until recent years, the insulation test most commonly used for generator-voltage switching stations with an operating voltage in the 15 kv class was the 36 kv. More recently the trend has been toward a higher level, usually 54 kv, or the test value corresponding to the next higher voltage rating. Proper co-ordination of the insulation strength of various pieces of apparatus is most readily obtained in a complete metal-enclosed equipment. In many cases, the use of such steel construction has made it possible to secure the greater clearances necessary for the higher insulation level without requiring additional over-all space. Complete phase segregation by means of steel barriers throughout provides the maximum in safety, and permits application with maximum benefits of the various protective means to be discussed later.

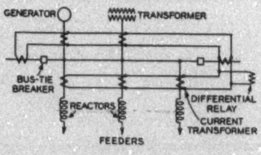

Figure 3. Differential protection applied to one bus section

Protection

Protection may be divided into two classifications, preventive and curative. The whole physical design as already discussed is bound up in preventive protection, but in addition other factors external to the gear must be considered.

The control of voltages due to lightning has received much study and as the literature has much regarding the treatment of this problem it will not be further discussed here.[1]

Prevention of serious overvoltages from other causes is a problem of the design of electrical connections and will be discussed later.

The application of curative protection is a function of the importance of the station. In an important station, such as is being considered, continuity of service is of utmost importance and therefore it is almost universal practice to use only such protection as can definitely distinguish between troubles in the protected area which must be cleared and those outside which must not be allowed to disturb anything in the area. Fast action is desirable in clearing a fault in that the damage to the gear and the probability of communication to other parts of the gear is reduced. If a study of the system shows that instability can occur if the fault duration is excessive, then fast clearing is imperative for loss of stability may mean an extremely serious disruption of service.

One of the simplest schemes in principle is the fault bus which, unfortunately, is one of the most difficult to install properly to obtain the desired results. Every element of the switching structure is insulated from ground and sections are insulated from each other. Each section is then provided with a ground through a current relay. If the structure is so designed that any fault must start as a line-to-ground fault, then the fault current must flow through the ground-current relay which causes the opening of the switches necessary to isolate the section in trouble. Very fast and reliable relays are available for this application so that faults can be cleared very rapidly with no danger of ever tripping at any but the right time. It can be shown that voltages of dangerous magnitude can be obtained on the ground bus and everything connected to it when a fault occurs. By the use of properly proportioned capacitors to ground at the right points the voltage can be held to a safe value. The practical difficulties of insulating an existing structure are so great as practically to eliminate it from consideration on a modernization program. Even if the structure is yet to be built, the difficulties are great and the apparatus is subject to accidental grounds on the structure which may by-pass the relay, making it inoperative. Therefore, the use of this apparently fine scheme has been rather limited.

The most common bus protection in use is the differential system, which may take any of several forms. Fundamentally the scheme consists of the addition of currents in all conductors connected to a bus section, as illustrated in figure 3. If a fault occurs outside of the section the incoming currents equal the outgoing, so the net sum is zero. A fault inside of the section causes a balance of current flow inward and the sum registered is equal to the fault current. The summation current is caused to flow through a relay which can trip all breakers feeding that section. All types of faults are covered by this type of protection, as each phase is treated independently. If the line-to-ground fault current is held to a low value a more sensitive relay can be used in a zero-sequence circuit for better ground protection. The major difficulty encountered in the application of this type of protection is that of obtaining accurate current transformers. At any given burden and primary current, duplicate transformers will all have about the same current ratio, but if different currents flow the ratios will vary, even with the same burden on each transformer. This variation in ratio will cause a net current to flow in the relay circuit during a fault outside of the protected area and if the variation is great enough the relay will operate. Therefore, if a simple overcurrent relay is used in the differential circuit care must be taken to use current transformers which maintain a nearly uniform ratio for the full range of possible currents.

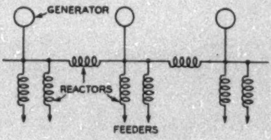

Figure 4. Straight bus

Inasmuch as the current transformers in a bus differential installation will not all be working at the same value of primary current in the event of a through fault, a certain amount of differential cur-

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1. For numbered reference, see end of paper.

rent will flow through the relay circuit in such a case. The magnitude of this current will, of course, vary with the current transformers used, the amount of primary current encountered, and will be further influenced by whether or not an offset wave having a d-c component is present. The effect of this undesirable differential current upon the operation of the protective equipment can be eliminated in several ways, depending upon the requirements of the problem. In many cases it has proved satisfactory to use a plain overcurrent relay with a relatively high setting. Other cases can use percentage differential relays similar to those in common use for transformer and generator protection obtaining restraint from proper grouping of the various current-transformer secondaries. Where high-speed protection is a requisite and neither of the two previously mentioned arrangements is deemed entirely adequate, there is available the harmonic-current-restrained relay which prevents relay operation in the case of current-transformer breakdown on a through fault, because of the distorted wave form of the current in the differential circuit. This current is made up of a difference in the exciting components required for the different current transformers and is rich in second and third harmonics which are used by means of a filter circuit to prevent relay operation.

Errors can be introduced in current transformers by the close proximity of other conductors carrying heavy currents which may cause false tripping when a simple overcurrent relay is used or may cause a long blocking interval on a harmonic-restraint relay to prevent its proper operation. This difficulty can be overcome by proper current-transformer design, by wider spacings, or by magnetic shielding.

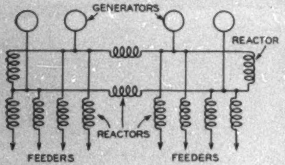

Figure 5. Ring bus

If every feeder circuit is equipped with a current-limiting reactor it is often possible to apply an impedance relay for bus protection. Current transformers are used only in the circuits which provide the major part of the short-circuit current and the secondary currents are added together and the sum passed through the current coil of an impedance relay. The relay is adjusted to operate if the equivalent impedance to the fault is less than the minimum possible impedance to a fault outside of the protected area. If the indicated fault impedance exceeds this value the fault is an external one and the relay does not close its contacts. High-speed operation can be obtained with this method of relaying. Current transformer errors cannot cause false operation on feeder faults but may on faults on a circuit from which current is taken for the relay. Careful consideration of each application is necessary on this point.

A system of protection which seems to have great possibilities consists of photoelectric relays so placed that any possible arc will throw light on a photoelectric tube. Usually there will be a large differential between the normal light level and the light produced by a short-circuiting arc and this difference can be used to differentiate a fault condition from normal conditions.

When the light of the arc falls on the tube all breakers on the affected bus section are tripped. This system can be made to operate fast and tests indicate it to be very effective in its selection of faults. Its principal disadvantage seems to be the lack of experience with its use on an actual system.

Electrical Connections

The design of electrical connections of a generating station can be such as further to limit the physical damage due to a short circuit, to simplify relay application, and what is more important, to reduce or eliminate service outages.

Short circuits in a switching equipment usually start line to ground. The magnitude of fault current for this type of short circuit can be reduced without disturbing the normal operation of the station by the use of a neutral impedance. This reduction is desirable in that it reduces the damage at the point of fault, reduces the probability of spreading the damage to other points, and reduces the strain on the switching equipment.

The most common neutral impedance in use is a pure resistance of a moderate value. The use of a high resistance approaches a condition of isolated-neutral operation which is known to produce dangerous over-voltages when a considerable transmission system is connected to the bus at generator voltage. This should be considered in the selection of a grounding resistor.

Reactors have been used and if of a low ohmic value are very successful and are more economical than an equivalent resistor. If the fault current is reduced to less than 25 per cent of the three-phase short-circuit current serious overvoltages can be expected even though no transmission circuits are connected directly to the bus.

If the zero-sequence network is limited to the station, that is, if all feeders are connected to the bus through transformers, it may be satisfactory to operate with an isolated neutral. Some stations have been operating in this manner for many years with great success. It is known that with low values of capacitance to ground and moderate values of neutral reactance voltage troubles can occur. It is also evident from limited experience that if the impedance approaches infinity that successful operation has been obtained. The limiting conditions are not very well known and, therefore, the initiation of such operation should be considered as experimental with a high degree of probability of successful operation. The results of successful operation with isolated neutral are a practical elimination of damage and spread of damage from a ground fault and a possibility of continued operation during the fault and until it is convenient to isolate the affected spot.

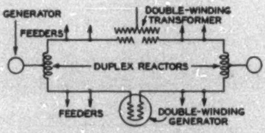

Figure 6. Modification of ring bus

It is usually desirable and sometimes necessary to reduce the short-circuit current in faults involving more than one phase. It is also desirable to so set up the bus that any fault can be isolated without materially disturbing service. These ends are both approached by use of a sectionalized bus. There are three general methods of arranging a sectionalized bus which are known as straight bus, ring bus, and star bus.

The straight bus is, as the name implies, a series of bus sections with reactors and breakers to connect them, as shown in figure 4. If the power supply is well distributed between bus sections and if all major loads are tapped off of at least two sections a bus fault properly

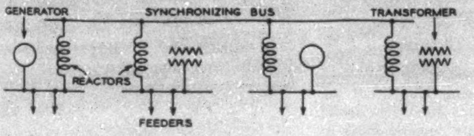

Figure 7. Star bus

relayed will not cause a serious loss of either power supply or load. That is, service would not be materially disturbed. If the affected section is any but an end section the isolation of the fault will split the station so that parts of it are connected only at the load. A careful study should be made to be sure that this does not cause instability between units of the same station.

Frequently a station operating with this type of bus is supplied with a duplicate bus to be connected in case of failure on the main bus. When good bus relaying is used it seems desirable to put the reserve bus in regular service to increase the number of bus sections and decrease the probability of an outage when a fault occurs. It is usually a simple matter to connect a main and reserve bus together with reactors to make a ring bus.

The ring bus is a series of bus sections connected together to form a continuous ring as shown in figure 5. With this arrangement the station is not split by the loss of one bus section. It is desirable to arrange the load so that as nearly as is possible each bus section has equal load and power supply. As this usually cannot be accomplished completely a transfer between bus sections is obtained and if reactor ties are used, there will be a variation in voltage on different bus sections. This regulation is less on a ring bus than on a straight bus, as there are two paths of power flow between any two points.

A modification of the ring bus is shown in figure 6 which prevents the total loss of any power supply when a bus section is lost. All large transformers are built with two low-voltage windings and connected to two bus sections. Each generator is built with a double winding or supplied with a duplex reactor. This is an economical arrangement for a steam plant, as generator reactors are usually used due to the low machine reactance and one reactor serves for both machine and bus tie.

The star or synchronizing-bus arrangement is illustrated in figure 7. The station is divided into independent sections which are each connected to a synchronizing bus through a reactor which is usually of a fairly high value. Loss of one section is equivalent to the loss of a section of a simple ring bus in that the generation and load on that section only are lost. If all load centers are connected to at least two sections no load is lost. The station is not split even if more than one section is lost. The loss of the synchronizing bus, however, splits the station into small units and the short-circuit current on this bus is apt to be very high. The regulation between sections of the star bus is often greater than it would be with the ring bus but under some conditions it may be less.

Most recent installations or modernizations have made use of either the ring or star bus arrangements and the decision of which to use has rested on local conditions and requirements. The trend in either case is to use a greater number of sections to minimize the effect on the system of the loss of one section.

It has been pointed out that reactors are often necessary to reduce the short-circuit current to a reasonable value. Bus-tie reactors are also desirable to increase the transient regulation between bus sections and thereby minimize the effect of that fault on one section on other sections and on the load voltage. It is desirable to ride through a fault without tripping undervoltage releases on any motors. Balanced against these desirable characteristics are the undesirable ones of increased normal regulation and decreased stability factors. The ability of two machines or group of machines to maintain a stable connection varies inversely with the impedance between them. A careful study should be made to co-ordinate bus connections, system connections, and relaying to ensure against loss of stability and resultant outages to power users.

Conclusion

The modernization or construction of any large switch house should take into consideration not only the physical structure with a view to minimize faults but also system connections and auxiliary equipment with a view to prevent serious outages to power users. Equipment and knowledge is available to design the apparatus and system to successfully meet any set of conditions which may be encountered.

Reference

1. PROTECTION OF STATIONS AGAINST LIGHTNING, L. V. Bewley and W. J. Rudge. *General Electric Review*, August and September 1937.

Discussion

For discussion, see page 778.

Modernization of Switch-House Design, Consolidated Edison Company of New York, Inc.

A. M. de BELLIS
MEMBER AIEE

IN THE past two or three years the question of modernization of generating stations and station equipment has been discussed liberally in the technical literature and in technical committees. It is the purpose of this paper to review the principal features of a modernization program undertaken by the Consolidated Edison Company of New York and its associated companies.

In introducing this subject a brief historical sketch of the development of such a program should prove of interest to those faced with the problem of system modernization.

Historical

The Consolidated Edison system furnishes electric service to all of Greater New York, with the exception of Staten Island, and to most of Westchester County.

It serves a population of 8,000,000 in an area of approximately 500 square miles.

Its nine generating stations have an installed capacity of 2,400,000 kw and produce annually approximately $6^1/_2$ billion kilowatt-hours, with a peak load estimated for the year 1939 at 1,715,000 kw.

Approximately 70 per cent of the total generating capacity supplies a 60-cycle load which consists largely of residential, commercial, small power, and traction.

The remaining generating capacity supplies, at 25 cycles, a large traction load which includes all of New York City trunk line railroads and, through rotary-converter substations, supplies a residential and commercial d-c load, mostly in Manhattan.

Obviously, in a system of such magnitude, there are to be found generating stations which vary considerably in age, size, type of design, and function.

Figure 1 shows the location, size, and the period in which the various stations of the New York system were originally built.

The first active planning for station modernization was undertaken in 1934 when it became necessary to make provision for the growing Manhattan 60-cycle load, and it was decided to install 60-cycle high-pressure topping units in Waterside No. 2, a 30-year-old station which had accumulated an excess of 25-cycle capacity due to the gradual replacement of 25-cycle load by 60-cycle load. Rebuilding of the Waterside No. 2 switch galleries was started in 1935 and will be completed in 1940.

Planning for the modernization of the Sherman Creek station was begun in 1935 and construction work for the rebuilding of the switch galleries was started in 1937, this being the first step in a program which includes the rebuilding of 40,000 kw of 25-cycle generators for operation at 60 cycles and the installation of 60-cycle topping units.

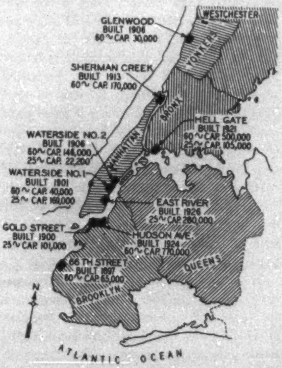

Figure 1. Generating stations owned by the Consolidated Edison system

Similar plans have been under consideration for the eventual revamping of the Gold Street, 66th Street, and Glenwood generating stations.

At the Hell Gate station, plans for the installation of bus differential protection and for the reinforcing of the station grounding system were started in 1934 and construction work on these improvements was in progress when the bus failure of January 1936 occurred.

As a result of this experience, the Hell Gate switch galleries are being completely revamped and minor improvements have been made to the Hudson Avenue station switch galleries.

General Design Features

In each station the type of design which has been adopted for the modernization of the switch galleries has been influenced not only by physical limitations of existing structures and existing types of design, but also by the necessity of maintaining the highest possible degree of station safety and service reliability while rebuilding operations were in progress.

In all cases, however, it may be said that as a result of past experience and the advancement in the art since the various stations of the Consolidated Edison system were built, the major objective has been that of securing a greater sectionalization of the switching equipment than previously existed.

The most important of the various steps which have been taken in order more effectively to confine to a relatively small section of the switch galleries the damages caused by equipment failures may be summarized as follows:

1. Improved physical sectionalization by means of fire walls and fire doors. The switch galleries have been divided in sections and, wherever possible, direct passage between sections has been eliminated. Switching equipment which is normally open has been provided on opposite sides of the fire walls between sections so that failure in one section is not likely to spread to the next.

2. Extensive electrical sectionalization of the main busses. This has resulted in a greater number of bus sections being available in a given station. Generator connections to the bus sections have been rearranged whenever necessary to secure a better balance between feeder and generator capacities. The loss of any one bus section will not seriously affect the station generating capacity and will result in a minimum feeder outage with no serious effects insofar as a given load area is concerned.

3. Use of the single-bus arrangement in preference to the double bus which had been more generally used in the past. This arrangement has made it possible to secure

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Paper number 39-24, recommended by the AIEE committee on power generation, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 23, 1938; made available for preprinting December 14, 1938.

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a greater sectionalization of the equipment within the various sections and has greatly reduced the number of oil circuit breakers required. Wherever possible, the high-duty circuit breakers which connect directly to the busses and which, in general, contain relatively large quantities of oil have been segregated into small groups, in separate rooms, thereby reducing and localizing the fire hazard.

Although oil-circuit-breaker performances have been generally satisfactory, some consideration is now being given to the possible use, in the future, of oilless breakers so as to reduce still further the fire hazard.

4. Metal-enclosed equipment of the group-phase type of construction, with each phase in a separate housing, has been used wherever possible.

5. Bus differential protection and high-speed relaying have been provided as additional means of localizing station faults and reducing to a minimum the destruction caused by such faults. In some cases the station grounding systems have been improved.

6. Ventilating systems have been improved so as to maintain proper isolation between sections and to facilitate the clearing of smoke and fumes in the event of fire. In some cases a water-spray system of fire protection has been provided for the better control of and to localize oil fires.

7. Power-supply systems for station auxiliaries and for generator excitation have been sectionalized in order to reduce to a minimum the hazard of a serious station shutdown resulting from failures in these systems.

What follows is a brief description of the modernization plans as developed for the individual stations.

Waterside No. 2 Station

This station was originally designed for ten turbogenerators and an ultimate station capacity of approximately 106,000 kw at 6,600 volts, 25 cycles.

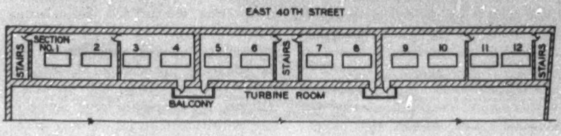

EAST 40TH STREET
SECTION NO. 1
2
3
4
5
6
7
8
9
10
11
12
STAIRS
STAIRS
STAIRS
BALCONY
TURBINE ROOM

Figure 3. Waterside No. 2—typical feeder floor

The switching equipment was of the double-bus group-phase arrangement and masonry compartments were used throughout. Connections to the main and auxiliary busses were of the familiar H-type design, each feeder group consisting of two selector oil circuit breakers, a short stub bus, and two feeder breakers, one for each of two feeders.

By 1934 the installed turbogenerator capacity had been increased to 152,000 kw and the 25-cycle voltage had been changed from 6,600 volts to 11,400 volts.

Although various improvements had been made to the switching equipment in order to meet the higher voltage requirements and the increased duty imposed upon the breakers, the main features of the original design had been maintained.

The original auxiliary power and excitation systems were still in existence and consisted of separate 250-volt d-c power and excitation busses supplied from motor generator sets and motor-driven exciters, respectively, with separate d-c station ties and standby batteries as back-up protection.

In accordance with present plans, the switch galleries are being completely rebuilt for operation at 13,200 volts, 60 cycles. The ultimate station capacity will be approximately 312,000 kw and will consist of seven or eight generators, four of which will be topping units of

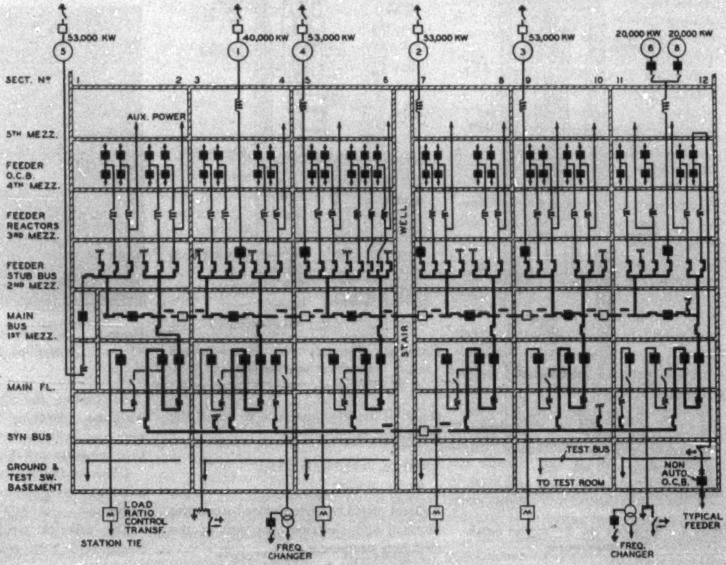

53,000 KW
40,000 KW
53,000 KW
53,000 KW
53,000 KW
20,000 KW
20,000 KW
5
1
4
2
3
6
8
SECT. N°
1
2
3
4
5
6
7
8
9
10
11
12
AUX. POWER
5TH MEZZ.
FEEDER O.C.B. 4TH MEZZ.
FEEDER REACTORS 3RD MEZZ.
FEEDER STUB BUS 2ND MEZZ.
MAIN BUS 1ST MEZZ.
MAIN FL.
SYN BUS
GROUND & TEST SW. BASEMENT
WELL
STAIR
TEST BUS
TO TEST ROOM
NON AUTO O.C.B.
LOAD RATIO CONTROL TRANSF.
STATION TIE
FREQ. CHANGER
FREQ. CHANGER
TYPICAL FEEDER

Figure 2. Waterside No. 2—main connections

53,000 kw or larger, and the balance will be existing low-pressure 25-cycle units rebuilt for 60-cycle operation.

Bus Arrangement

The scheme of connections adopted for the 60-cycle equipment is a modified type of main and synchronizing bus arrangement in which the generators are connected directly to the main busses.

The main bus is divided in 12 feeder-bus sections and the synchronizing bus is divided in two sections as shown on figure 2.

During peak-load conditions the feeder-bus sections are to be tied in pairs through automatic bus-tie breakers, thus operating as a six-bus-section station and the synchronizing bus is to be operated in two sections. Under these conditions, each bus section will be supplied from three independent sources: a generator, an interstation tie, and the synchronizing bus. During off-peak load conditions the feeder busses may be tied in groups of four, thus operating as a three-bus-section station. Under the same conditions, the synchronizing bus will be operated in one section.

The station ties, being connected to the synchronizing ties between the main busses and the synchronizing bus, offer a means of transferring power between generating stations independently of the feeder busses and offer the additional advantage of rendering all station ties available to any feeder-bus section without impairing the sectionalization of the main busses.

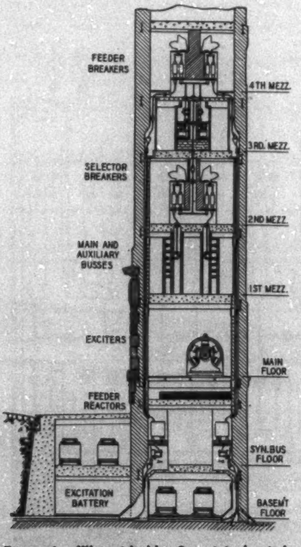

FEEDER BREAKERS
4TH MEZZ.
3RD. MEZZ.
SELECTOR BREAKERS
2ND MEZZ.
MAIN AND AUXILIARY BUSSES
1ST MEZZ.
EXCITERS
MAIN FLOOR
FEEDER REACTORS
SYN.BUS FLOOR
EXCITATION BATTERY
BASEM'T FLOOR

Figure 4. Waterside No. 2—original switching arrangement

Each feeder-bus section supplies four outgoing distribution feeders arranged in two feeder groups and each group is connected to the main bus through a group-feeder reactor and disconnecting switch.

Locating reactors between the bus and the automatic breakers on all outgoing feeders, station ties, and frequency changers, has effected a large saving in breaker costs by reducing the duty on the breakers to a point where existing breakers could be readily modified to meet the new rating requirements.

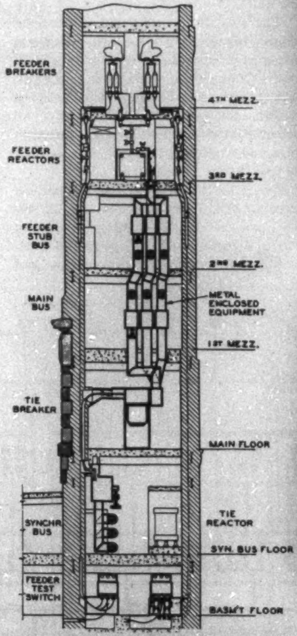

FEEDER BREAKERS
4TH MEZZ.
FEEDER REACTORS
3RD MEZZ.
FEEDER STUB BUS
2ND MEZZ.
MAIN BUS
METAL ENCLOSED EQUIPMENT
1ST MEZZ.
TIE BREAKER
MAIN FLOOR
SYNCHR BUS
TIE REACTOR
SYN. BUS FLOOR
FEEDER TEST SWITCH
BASM'T FLOOR

Figure 5. Waterside No. 2—modernized switching arrangement

Physical Sectionalization of Equipment

Great stress has been placed upon the physical and electrical isolation of the switching equipment.

Figure 3 shows how the switch galleries have been divided longitudinally in six major sections by means of fire walls and fire doors.

No direct access is provided between alternate major sections, the only access between these sections being by means of open balconies located outdoors or in the turbine room.

B
SYN.BUS
SEC.Nº2
4

Figure 6. Waterside No. 2—metal-enclosed synchronizing-bus equipment

ASSEMBLY 33
9853
9656

Figure 7. Waterside No. 2—metal-enclosed feeder-bus equipment

Essentially, the electrical equipment, with the exception of the synchronizing bus, has been isolated physically into three separate switch galleries.

Likewise, the synchronizing-bus equipment has been isolated in two separate sections.

The galleries occupy six floors and means have been provided to secure, if possible, a vertical isolation of the major equipment within each section.

For this purpose gas seals have been used between the various floors and it is expected that they will help to localize to any one floor the damage caused by equipment failures.

Figure 8. Waterside No. 2—metal-enclosed main-bus equipment

Figure 9. Waterside No. 2—metal housings for oil-circuit-breaker bushings

High-duty breakers are used only for bus-tie and generator connections. To reduce the fire hazard these breakers have been segregated into small groups located in separate rooms. With the exception of the breakers on the main floor, under peak-load conditions, there will be only one such breaker energized and carrying load in any one room. On the main floor there will be two such breakers in any one room. In the basement, where the nonautomatic metal-clad oil-filled ground and test switch units are located and where eight such units are housed in any one room, a water-spray system of fire protection is being installed.

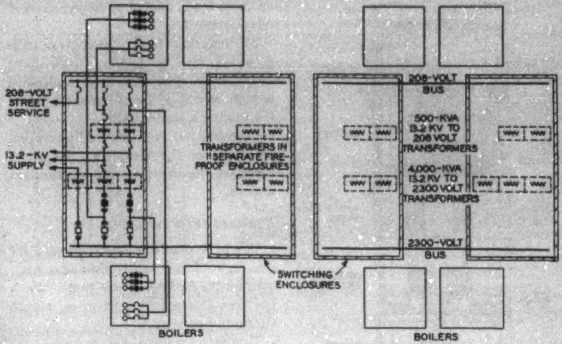

208-VOLT STREET SERVICE
13.2-KV SUPPLY
TRANSFORMERS IN 11 SEPARATE FIRE-PROOF ENCLOSURES
208-VOLT BUS
500-KVA 13.2 KV TO 208 VOLT TRANSFORMERS
4,000-KVA 13.2 KV TO 2300 VOLT TRANSFORMERS
2300-VOLT BUS
SWITCHING ENCLOSURES
BOILERS
BOILERS

Figure 10. Waterside No. 2—auxiliary power supply

Figure 2 shows to what extent the electrical equipment has been physically isolated by means of walls and floors.

Electrically, the isolation between major bus sections is obtained at the fire walls by means of switching equipment which is normally kept open. For this purpose a bus-tie breaker and a disconnecting switch are provided on one side and a motor-operated disconnecting switch on the other side of the walls.

Where the bus goes through the fire walls, a gas-tight type of construction is used.

With this arrangement there are interposed between any two major bus sections, two distinct lengths of bus normally de-energized and physically separated from each other. It is felt that with this arrangement the effects of an equipment failure in any one major section are not likely to spread to other sections.

METAL-ENCLOSED EQUIPMENT

In the modernization of the Waterside switch galleries extensive use has been made of metal-enclosed equipment. Figures 4 and 5, which are cross sections of the original and the modernized switch galleries, show the simplification in station arrangement made possible by the use of metal-enclosed equipment.

Space limitations imposed by the existing building, and stringent requirements for service reliability and safety to personnel which had to be met, brought about the development of a new type of bus construction.

This new type of construction offers the advantages of a separated phase arrangement in a compact, simplified design which permits the use of factory-assembled equipment and has made it possible to lower installation and maintenance costs.

This new bus design consists, essentially, of a supporting structure on which are mounted a ground grid, the bus disconnecting switches, and the bus insulator supports which carry not only the bus conductors but also the bus enclosures.

To facilitate the use of factory-assembled equipment and to reduce installation costs, most of the supporting structures consist of unit-type fabricated-steel assemblies although, in some cases, use has been made of existing building steel and building walls.

The bus insulator supports which are the main members designed to withstand the short-circuit stresses consist of assemblies of four adjustable porcelain insulators mounted at 90 degrees to each other on a metal ring and brought to bear against the busses which are thus held in place.

In addition, the busses are taped for 23-kv insulation.

The metal bus enclosures, one for each phase, consist of cylindrical split covers which are bolted together and clamped around the insulator supporting rings, forming a dust-tight assembly.

This type of construction, in which the bus enclosures are not only entirely independent of the bus conductors and the bus supports, but are readily assembled after all bus connections are completed, offers a means of readily installing, inspecting, or repairing the bus equipment.

Phase isolation and metal-enclosed construction have also been used for the

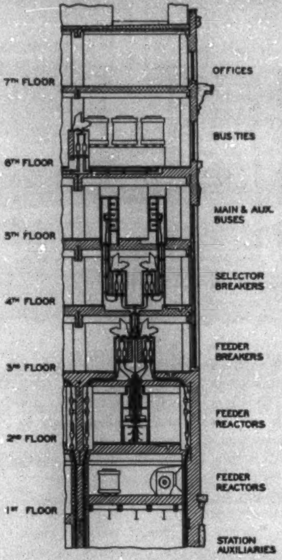

7TH FLOOR
6TH FLOOR
5TH FLOOR
4TH FLOOR
3RD FLOOR
2ND FLOOR
1ST FLOOR
OFFICES
BUS TIES
MAIN & AUX. BUSES
SELECTOR BREAKERS
FEEDER BREAKERS
FEEDER REACTORS
FEEDER REACTORS
STATION AUXILIARIES

Figure 11. Sherman Creek—original switching arrangement

bus disconnecting switches and the main oil circuit breakers which are connected directly to the busses.

Figures 6 and 7 are partial views of the metal-enclosed equipment used for the synchronizing bus and the feeder stub busses respectively.

A view of one of the main bus sections and a detail of the enclosures for the bus-tie oil-circuit-breaker bushings are shown in figures 8 and 9 respectively.

Relay Protection

Each of the 12 feeder-bus sections, that is, each group of four outgoing feeders, and each of the two synchronizing-bus sections are protected by a complete and overlapping scheme of high-speed current differential relays.

The bus differential relays act to trip all bus connections including the generators with a minimum time delay of approximately 0.05 second.

A local differential protection which overlaps and is selective with the main-bus and synchronizing-bus differential relays is used to protect the synchronizing ties which connect the main and the synchronizing busses.

As an additional protection against failure of the synchronizing-tie oil circuit breakers, the differential protection on these ties is backed up by overcurrent relay protection.

All outgoing feeder groups are also provided with a back-up overcurrent relay which clears the main feeder-bus section should a feeder circuit breaker fail to clear a feeder fault.

Conventional schemes of protection are used for the generators, frequency changers, station ties, and distribution feeders.

Auxiliary Power Supply

The basic plan of connections used for the high-voltage supply has also been used for the auxiliary power supply which has been changed from direct current to alternating current.

Two services have been provided, one at 2,300 volts and the other at 208 volts. For each service the ultimate bus arrangement will consist of eight load busses, one for each boiler, and a transfer bus for the reserve supply.

Each load bus will be supplied by its own power transformer and the transfer bus will be supplied by house generators for the 2,300-volt service and by ties to the street secondary network for the 208-volt service.

Station auxiliaries, with the exception of some emergency steam-driven boiler-feed pumps and condensate booster pumps, are all motor-driven.

Single-speed motors are used throughout and with the exception of two boiler-feed-pump motors, they are all started on full line voltage.

Most of the auxiliaries being boiler auxiliaries, the transformers and the switching equipment for the two services have been located at the load centers in the boiler house as shown on figure 10.

A minimum amount of automatic switching equipment has been used. For each boiler, for example, all 2,300-volt motors are supplied from one radial feeder. Nonautomatic starting switches are provided, of course, for the individual motors. All switching equipment for both service supplies is of the metal-enclosed type of construction.

Manual operation of the switching equipment on the supply circuits to the 2,300-volt and 208-volt busses is by means of supervisory control from the main control room in the switch galleries.

Physical sectionalization of the equipment has been obtained by installing the transformers and the switching equipment for each pair of boilers, in separate fireproof masonry housings.

Emergency lighting throughout the station is provided by groups of separate emergency outlets which are supplied by individual battery sets arranged for automatic operation and located at important operating points.

Other major changes which are part of the station modernization program include a new control supply which consists of duplicate control busses and switching equipment located in separate fireproof enclosures; the revamping of the control switchboards and the replacing of the present excitation-bus system by direct-connected main and pilot exciters on each generator.

Operating Experience

The first section of the rebuilt switch galleries went into service in May 1937

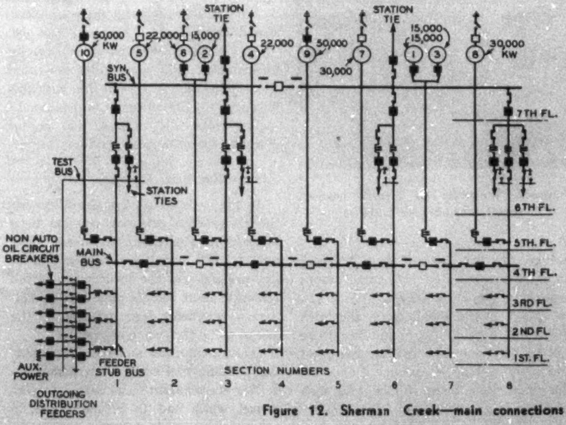

50,000 KW
22,000
15,000
STATION TIE
22,000
50,000
30,000
STATION TIE
15,000
15,000
30,000 KW
10
5
6
2
4
9
7
1
3
8
SYN. BUS
7TH FL.
TEST BUS
STATION TIES
6TH FL.
5TH. FL.
NON AUTO OIL CIRCUIT BREAKERS
MAIN BUS
4TH FL.
3RD FL.
2ND FL.
1ST. FL.
AUX. POWER
FEEDER STUB BUS
SECTION NUMBERS
1
2
3
4
5
6
7
8
OUTGOING DISTRIBUTION FEEDERS

Figure 12. Sherman Creek—main connections

and operating experience, so far, has been entirely satisfactory.

To what extent metal-enclosed equipment and fast relaying will contribute toward localizing the effects of equipment failures and safeguarding personnel as well as reducing to a minimum the damages caused by such failures was illustrated in November 1937 when a short circuit occurred on one of the frequency-changer circuits.

Each of the two frequency changers is connected directly to the synchronizing bus through a bus disconnecting switch, a reactor, and an oil circuit breaker.

At that time the necessary interlocks between the manually operated bus disconnecting switch and the oil circuit breaker were not completely installed.

With the frequency changer at a standstill, an attempt was made to close the bus disconnecting switch while the circuit breaker was not in the completely open position, the contacts on two phases being partially closed.

The disconnecting switches on the two phases arced over to ground causing a double-line-to-ground short circuit on the synchronizing bus. The fault was cleared by the synchronizing-bus differential relay in approximately 0.3 second of total clearing time, including breaker time. Feeder busses were not affected. The total fault current at the disconnecting switch was calculated to have been 56,000 amperes root-mean-square symmetrical current and the estimated instantaneous asymmetrical current 140,000 amperes.

The damage to the switches was limited to local burning of the stationary contacts and blades, and also burning and chipping of some of the insulators.

The outside of the switch housing showed no indication of damage.

On the inside of the housing, a slight burning showed where the switch arced to ground.

The insulated bus connections between the disconnecting switch and the bus as well as the bus and bus supports and the bus housings were undamaged.

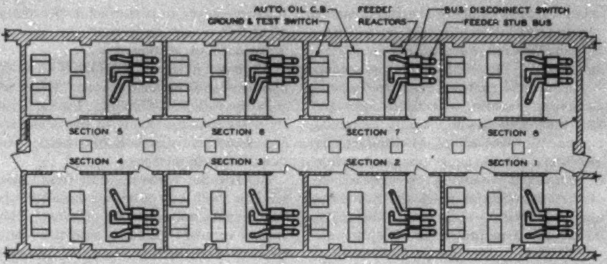


Figure 14. Sherman Creek—typical feeder floor

However, beyond the metal-enclosed equipment the connections from the disconnecting switch to the oil circuit breaker are carried on the ceiling in three-phase compartments of the conventional type of construction with concrete barriers between phases and with bolted gasketed metal covers.

These covers were blown open by the gas pressure.

Operating personnel was not injured.

The damaged equipment was replaced in a few hours and the total cost of repairs did not exceed $2,000.

Sherman Creek Station

This station was originally designed for eight turbogenerators and an ultimate station capacity of approximately 145,000 kw, of which 40,000 kw were generated at 6,600 volts, 25 cycles, and the balance at 7,800 volts, 60 cycles.

Although the generation voltage for part of the 60-cycle supply was later raised to 13,200 volts, no major changes were made in the general station layout nor in the original arrangement of the switching equipment which, as shown in figure 11, is substantially a duplicate of the original Waterside design.

As part of the modernization program for this station which includes the installation of topping units, it is planned to rebuild the switch galleries for an ultimate station capacity of approximately 280,000 kw at 13,200 volts, 60 cycles, as shown in figure 12.

To date, the modernization work already completed consists of the rewinding of two 25-cycle generators for operation at 60 cycles and the rebuilding of the old 25-cycle section of the switch galleries.

Basically, the station layout for the

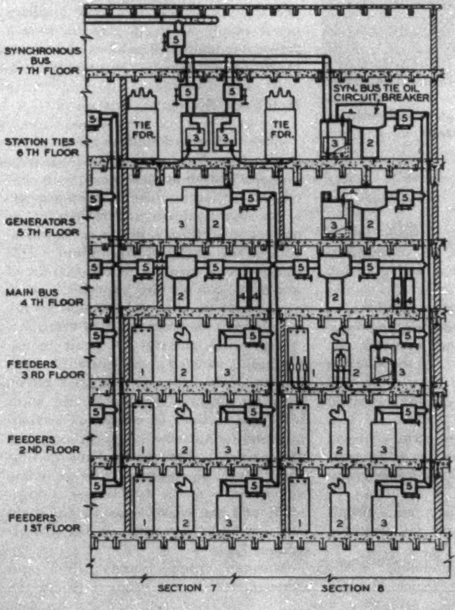


Figure 13. Sherman Creek—longitudinal section of switch galleries

1—Ground and test switch

2—Oil circuit breaker

3—Reactor

4—Potential transformer

5—Disconnecting switch

rebuilt galleries follows the fundamental requirements for equipment sectionalization and protection established for Waterside No. 2. Physically, however, a number of radical changes have been made. Structural building conditions and the fact that none of the existing switching equipment could be reused satisfactorily and economically in its present location have made it possible to develop a type of station design in which the switching equipment for any one circuit is arranged on the unit-type principle.

Essentially, this design isolates in a separate room all the main switching equipment related to a given circuit, such as bus disconnecting switches, oil circuit breakers, reactors, and ground and test switches.

From figure 13 which shows a longitudinal view of a typical major bus section, it can be seen how the switching equipment has been sectionalized and grouped in separate rooms.

Each room of the first, second, and third floors houses all of the equipment for one feeder group and all rooms are alike in design.

Likewise, the equipment for the generators and the station ties is located in separate rooms on the fifth and sixth floors respectively.

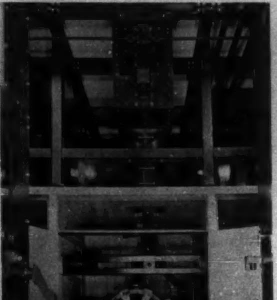


Figure 15. Sherman Creek—typical feeder-reactor installation

It is expected that this type of construction will offer some advantages from the standpoint of equipment sectionalization, station operation and maintenance, safety to personnel, and replacement of major equipment, as well as uniformity of station design.

As shown in figure 14 which is a typical plan of one of the feeder floors, the switch galleries will be divided physically in eight sections by means of fire walls and fire doors, no direct access being provided between sections.

Electrically, however, only four major sections will be provided because the busses will normally be operated connected in pairs through automatic bus-tie circuit breakers as shown in figure 12.

Metal-enclosed equipment is to be used almost entirely in the switch galleries, the only masonry compartments of any importance being those which house the reactors.

Figure 16. Sherman Creek—metal-enclosed bus equipment

A typical reactor compartment and a station-tie reactor with its connections to a metal-enclosed disconnecting switch are shown in figure 15.

The design of the metal-enclosed busses and the methods used to secure the proper electrical sectionalization between the major bus sections as well as the schemes used for bus differential relay protection are a duplicate of those already described for Waterside No. 2.

Figure 16 shows the metal-enclosed vertical bus runs and the bus disconnecting switches for a typical feeder-group installation.

Metal-enclosed feeder oil circuit breakers and ground and test switches are shown in figure 17.

In addition to the rebuilding of the switch galleries, other major changes which are part of the modernization program for this station include a revamping of the control switchboards, the installation of direct-connected main and pilot exciters on the generators to replace the present excitation-bus system, and the installation of a new auxiliary power supply which, in general, from a design standpoint will probably be similar to the one established for Waterside No. 2.

Figure 17. Sherman Creek—metal-enclosed switching equipment

Hell Gate Station

The modernization work planned for this station does not involve any additions to station generator capacity but consists of changes and improvements to the switch galleries undertaken for the purpose of securing a better sectionalization of the electrical equipment. This station has a capacity of 605,000 kw of which 105,000 kw are generated at 25 cycles, 11,400 volts, and 500,000 kw are generated at 60 cycles, 13,200 volts.

The switch galleries are built on the horizontal isolated-phase principle and, longitudinally, they are divided physically by means of transverse fire walls into nine sections of which six sections are used for the 60-cycle equipment and the remaining three sections are used for the 25-cycle equipment. The switching equipment is of the double-bus arrangement and the connections to the main and auxiliary busses are of the H-type design.

Masonry compartments are used throughout. In the original design the

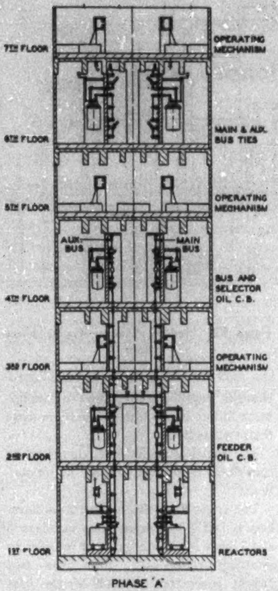

7TH FLOOR
6TH FLOOR
5TH FLOOR
4TH FLOOR
3RD FLOOR
2ND FLOOR
1ST FLOOR
OPERATING MECHANISM
MAIN & AUX. BUS TIES
OPERATING MECHANISM
AUX. BUS
MAIN BUS
BUS AND SELECTOR OIL C. B.
OPERATING MECHANISM
FEEDER OIL C. B.
REACTORS
PHASE 'A'

Figure 18. Hell Gate—original switching equipment

60-cycle busses were sectionalized electrically by bus-tie breakers and bus-tie reactors into four sections. The 25-cycle galleries are sectionalized in three sections. As shown in figure 18 the main and the auxiliary busses and related oil circuit breakers are located on the fourth floor with longitudinal walls separating the busses from the breakers.

Likewise, the bus-tie equipment for both busses is located on the sixth floor.

BUS REARRANGEMENT

In order properly to balance feeder, generator, and station-tie capacities and to provide the required feeder diversity to the various network areas it becomes necessary, under certain conditions, to operate both the main and the auxiliary 60-cycle busses.

As a result of the disturbance of January 1936 in which the trouble communicated between the main and the auxiliary busses, these busses are being rearranged and relocated.

From an operating standpoint the 60-cycle switching equipment will consist of a single bus which will be divided in 12 feeder-bus sections as shown in figure 19.

During peak load conditions the feeder-bus sections are to be tied in groups of three through automatic bus-tie breakers, thus operating as a four-bus-section station.

During off-peak load conditions the feeder busses may be tied in groups of six, thus operating as a two-bus-section station.

Generator connections to the bus sections will be rearranged in some cases to secure a better balance of feeder and generator capacities and the two 160,000-kw units will be reconnected so that each will feed two bus sections.

PHYSICAL SECTIONALIZATION OF EQUIPMENT

In order to reduce to a minimum the possibility of trouble on one bus section communicating to other sections, the switching equipment is being relocated as shown in figures 20 and 21.

With this arrangement the busses for each half of the station are completely isolated, being located on separate floors.

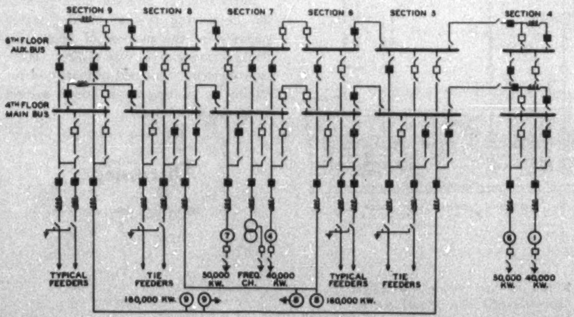
All bus-tie reactors, breakers, and selector breakers for six bus sections will be located on the sixth floor and the corresponding equipment for the other six sections will be located on the fourth floor.

Between major bus sections on the same floor, provision will be made for two separate transverse walls with fire doors and a passageway between walls. This will eliminate direct access between sections and it is expected it will reduce the possibility of communication of trouble from one section to the other.

To avoid outages during periods of maintenance and inspection of the bus equipment and to facilitate the reconstruction program, the H-type design of bus connections has been maintained and all circuits are provided with bus selectivity, being connected to the busses on the two floors.

However, risers to the sixth floor busses will be completely walled off from the busses on the fourth floor and on all circuits additional disconnecting switches in gas-tight enclosures will be installed on separate floors from the bus equipment so that between the fourth- and the sixth-floor busses there will be double disconnecting switches which will be operated open.

Figure 19. Hell Gate—main connections


SECTION 9
SECTION 8
SECTION 7
SECTION 6
SECTION 5
SECTION 4
6TH FLOOR AUX. BUS
4TH FLOOR MAIN BUS
TYPICAL FEEDERS
TIE FEEDERS
50,000 KW.
FREQ. CH.
40,000 KW.
TYPICAL FEEDERS
TIE FEEDERS
50,000 KW.
40,000 KW.
160,000 KW.
160,000 KW.

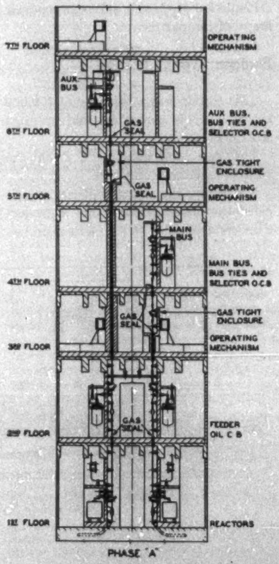

7TH FLOOR
6TH FLOOR
5TH FLOOR
4TH FLOOR
3RD FLOOR
2ND FLOOR
1ST FLOOR
OPERATING MECHANISM
AUX. BUS
GAS SEAL
AUX. BUS, BUS TIES AND SELECTOR O.C.B.
GAS TIGHT ENCLOSURE
GAS SEAL
OPERATING MECHANISM
MAIN BUS
MAIN BUS, BUS TIES AND SELECTOR O.C.B.
GAS SEAL
GAS TIGHT ENCLOSURE
OPERATING MECHANISM
GAS SEAL
FEEDER OIL C.B.
REACTORS
PHASE 'A'

Figure 20. Hell Gate—modernized switching arrangement

In addition, the connections between disconnecting switches are to be made gas-tight where they go through the floors. With this arrangement, trouble in any one bus is not likely to communicate to the other bus.

In order to maintain the necessary segregation between floors and between sections, extensive changes are also being made to the ventilating system.

Bus Relay Protection

Each of the 12 feeder-bus sections is protected against phase-to-ground faults by a combination of partial-current differential relays with impedance and zero-sequence-voltage fault detectors.

The bus differential relays act to trip all bus connections including the generators.

All generators, bus ties, and tie feeders are included in the current differential circuits, while faults on outgoing feeders are detected by the impedance elements and the zero-sequence-voltage fault detectors differentiate between the busses on the sixth and the fourth floors.

Other changes included in this modernization program, most of which have already been completed, consist of the reinforcing of the station grounding system and the installation of larger generator-neutral grounding breakers with a physical isolation of these breakers in separate fireproof compartments.

Hudson Avenue Station

This station has a capacity of 770,000 kw in 60-cycle generators and operates at 27,600 volts.

The switching equipment is of the vertical isolated-phase type.

Physically, the switch galleries are divided by walls and doors into eight sections corresponding to the eight main bus and feeder sections as shown on figure 22.

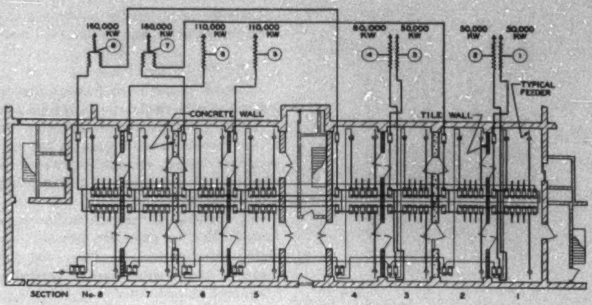


Figure 22. Hudson Avenue—typical phase floor

In the center, a fire tower with heavy brick walls and fire doors divides the switch galleries in two separate parts and prevents direct access from one part to the other.

Modernization work in this station has consisted primarily of the following:

1. Changes to fire walls.
2. Interchanging of generator connections.
3. Installation of bus differential protection.
4. Improvements to the ventilating system.

In order to secure a more effective sectionalization of the switching equipment and to prevent, if possible, the spread of damage caused by equipment failure, alternate fire walls have been changed from hollow tile to concrete. In addition, these walls have been equipped with sets of double fire doors.

It is expected that with these structural changes any damage caused by equipment failure is not likely to involve more than two sections.

The switch galleries may, therefore, be considered as divided in four major sections.

The connections from generators numbers 6 and 7 to bus sections numbers 6 and 7 have been interchanged in order to establish connections from the two largest generators to each of the four major bus sections and also to provide a better physical sectionalization between the connections of these two large generators.

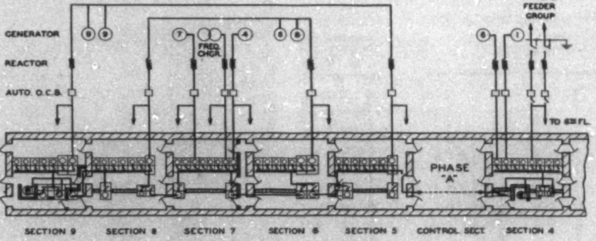
The bus arrangement in this station consists of sectionalized main and synchronizing busses.

Each main bus section is essentially an H-bus arrangement which, through automatic circuit breakers, is connected to four feeder busses and to its own generator and to the synchronizing bus.

Each of the H-bus sections have been equipped with current differential relays which act to trip all bus connections including the generator.

Improvements have also been made to the ventilating system of the switch galleries for the purpose of facilitating smoke scavenging in case of fire. These improvements consisted primarily of replacing an existing natural-draft system by forced ventilation.

Figure 21. Hell Gate—typical selector-breaker floor



Discussion

For discussion, see page 778.

Reconstruction of Switching Facilities at Essex Generating Station

D. W. TAYLOR
MEMBER AIEE

LOCATED on the Passaic River, three miles from the business district of Newark, N. J., the Essex generating station of the Public Service Electric and Gas Company was designed and constructed in 1915–16, to supply the load of the Newark and surrounding area at 13 kv with ties of approximately 20,000 kva to the neighboring Marion generating station. By 1924, six generators had been installed, totaling 214,444 kva, and the switch house had been extended to the ultimate of the 1915 plans.

During the growth of the system, a 26-kv outdoor substation and a 132-kv outdoor switchyard were added, the latter to tie in with the state-wide 132-kv transmission system which was interconnected through 220-kv with the Philadelphia Electric Company and the Pennsylvania Power and Light Company.

Progress in protective devices by 1935 had outmoded much of the station's equipment, and studies were started to determine the feasibility of applying modern protective schemes to the existing plant. As system conditions in 1936 required the installation of a superposed turbine generator and a third 132-kv line, the modernization studies were broadened to include a complete survey of the station's adaptability not only for these two immediate additions, but for future requirements.

At that time, the load of the 13-kv switch house was mainly that of the business and industrial area of Newark and vicinity, and was carried over 44 outgoing feeders and had a peak of 133,000 kva. The 26-kv switchyard carried a maximum load of 53,000 kva over seven feeders and was supplied from the 13-kv bus through transformers of 84,500-kva capacity.

Load estimates predicted a maximum 13-kv peak of 168,000 kva for future economical transmission at this voltage, the additional growth to be carried at 26 kv.

The 132-kv switchyard consisted of two 100,000-kva lines and two 45,000-kva transformer banks for interchange of bulk power.

Figure 1 shows the relation of the Essex generating station to its 13-kv load area and to the rest of the system.

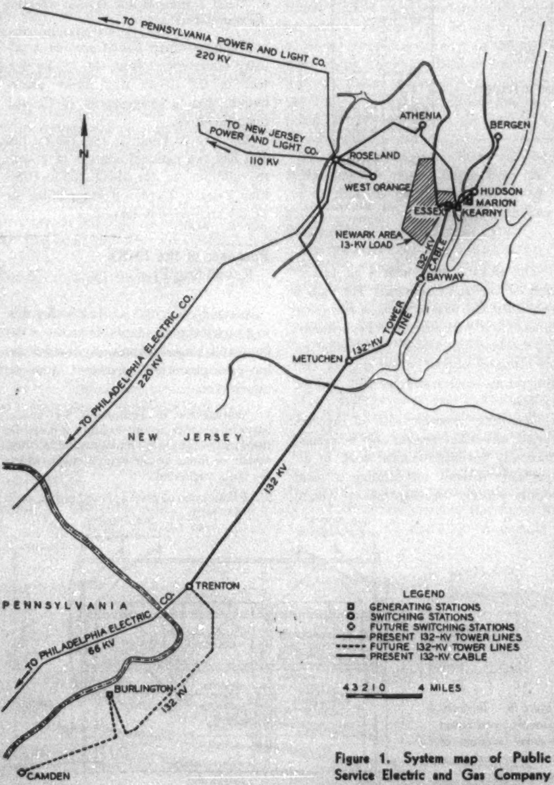

TO PENNSYLVANIA POWER AND LIGHT CO.
220 KV
N
TO NEW JERSEY POWER AND LIGHT CO.
110 KV
ROSELAND
ATHENIA
BERGEN
WEST ORANGE
HUDSON
MARION
ESSEX
KEARNY
NEWARK AREA 13-KV LOAD
132-KV CABLE
BAYWAY
132-KV TOWER LINE
METUCHEN
TO PHILADELPHIA ELECTRIC CO.
220 KV
NEW JERSEY
132 KV
TRENTON
PENNSYLVANIA
TO PHILADELPHIA ELECTRIC CO.
66 KV
BURLINGTON
132 KV
CAMDEN
LEGEND
GENERATING STATIONS
SWITCHING STATIONS
FUTURE SWITCHING STATIONS
PRESENT 132-KV TOWER LINES
FUTURE 132-KV TOWER LINES
PRESENT 132-KV CABLE
4 3 2 1 0 4 MILES

Figure 1. System map of Public Service Electric and Gas Company

Reasons for Rebuilding

That extensive rebuilding of the 13-kv, 26-kv, and 132-kv switching facilities was necessary is evident from the following summary of the then existing conditions.

1. Space was not available in the 13-kv busses for the proper location of current transformers for differential relays, which combined with the presence of many nonautomatic main breakers of inadequate interrupting capacity for automatic operation, made it impractical to provide bus protection. The layout was also unsuited for the most advantageous use of the 132-kv bulk power ties, and for future extensions.

2. Adequate segregation and isolation of equipment in the switch house to reduce the extent of oil-fire damage was lacking. One hundred seventy-six oil circuit breakers were housed on five floors of the 146-foot by 58-foot switch house without separating walls or barriers except that provided by the masonry cell work.

Paper number 39-25, recommended by the AIEE committee on power generation, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 15, 1938; made available for preprinting December 14, 1938.

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The author desires to express his thanks to the members of the electric engineering department for their assistance in the preparation of this paper.

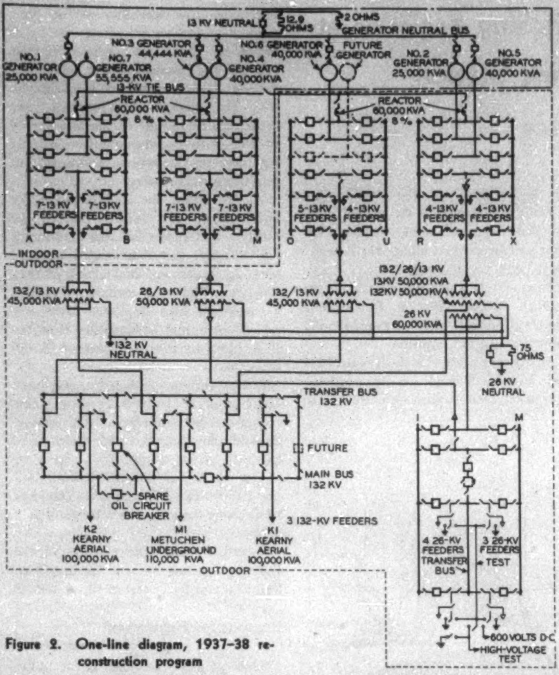

13 KV NEUTRAL
12.9 OHMS
2 OHMS
GENERATOR NEUTRAL BUS
NO.1 GENERATOR 25,000 KVA
NO.3 GENERATOR 44,444 KVA
NO.7 GENERATOR 55,555 KVA
NO.6 GENERATOR 40,000 KVA
NO.4 GENERATOR 40,000 KVA
FUTURE GENERATOR
NO.2 GENERATOR 25,000 KVA
NO.5 GENERATOR 40,000 KVA
13-KV TIE BUS
REACTOR 60,000 KVA 8 %
REACTOR 60,000 KVA 8 %
7-13 KV FEEDERS
7-13KV FEEDERS
7-13 KV FEEDERS
7-13KV FEEDERS
5-13KV FEEDERS
4-13KV FEEDERS
4-13KV FEEDERS
4-13KV FEEDERS
A
B
M
O
U
R
X
INDOOR
OUTDOOR
132/13 KV 45,000 KVA
26/13 KV 50,000 KVA
132/13 KV 45,000 KVA
132/26/13 KV
13KV 50,000 KVA
132KV 50,000 KVA
26 KV 60,000 KVA
132 KV NEUTRAL
75 OHMS
26 KV NEUTRAL
TRANSFER BUS 132 KV
FUTURE
MAIN BUS 132 KV
SPARE OIL CIRCUIT BREAKER
3 132-KV FEEDERS
K2 KEARNY AERIAL 100,000 KVA
M1 METUCHEN UNDERGROUND 110,000 KVA
K1 KEARNY AERIAL 100,000 KVA
OUTDOOR
M
4 26-KV FEEDERS
3 26-KV FEEDERS
TRANSFER BUS
TEST
600 VOLTS D-C
HIGH-VOLTAGE TEST

Figure 2. One-line diagram, 1937–38 reconstruction program

3. The control room was lacking in facilities for future additions and in need of modernization.

4. Oil circuit breakers were provided for four feeders and two transformer banks only in the 26-kv switchyard, the remaining three feeders making use of individual transformers and 13-kv switching. With increase of load, additional transformer capacity and bussing of all feeders would be necessary. Bus protection was also lacking.

5. The 132-kv switchyard was of the high, strain-bus construction with numerous crossovers, difficult to maintain, and with the possibility of a falling high-level connection, causing a total shutdown. The arrangement was unsuited for the future extensions.

The reconstruction program was started early in 1937 to be completed in two years.

Schematic Plan

The schematic plan developed is shown in figure 2. The 26-kv and 132-kv layouts are those developed over a period of 12 years in the 132- to 26-kv switching stations of the Public Service Electric and Gas Company.

The 13-kv arrangement is an adaptation of the 26-kv scheme. The station is divided into four sections, of two group busses each, with a tie bus for synchronizing and load balancing. Each section has an ultimate of four power sources, two generators, one transformer bank, and the tie bus.

Operating normally with all breakers closed, any main breaker may be opened manually for maintenance work or inspection, without interrupting a main supply source; a main breaker failure will clear only one bus or one-eighth of the feeders, in addition to its own generator or transformer, but that generator or transformer may be cut in again on the other group of the pair to carry load; and a fault on a feeder group bus will not clear any supply source from the system. In case the tie bus is cleared due to a fault, the four sections will be held in synchronism through the transformer banks and the 132-kv bus. The only main switching operations necessary are cutting generators in and out of service, as the transformers and tie bus are to be in service at all times except for inspection or maintenance.

The feeders to each substation are divided between the two group busses forming a section. This prevents:

1. Reducing effectiveness of tie-bus reactors.

2. Unbalanced loading of feeder cables.

3. Fault current in the station affecting the transmission system.

The transformer banks provide a primary power source from the 132-kv system to the 13-kv and 26-kv feeder busses, that is independent of the station generators.

Referring to figure 1, the 132-kv bus also has two external sources of power, the addition of the third 132-kv line, from Essex to Metuchen, completing the 132-kv transmission loop.

Purposes of the 13-Kv Rebuilding Plan

In transposing the schematic diagram to a physical plan adaptable to the existing switch house and property, the following principles were considered of major importance:

1. Segregation of equipment by space, barriers, or other means, to limit, if possible, the damage caused by an explosion, oil fire, smoke, or fumes to the equipment in which the fault originated.

2. Elimination of points of congestion, such

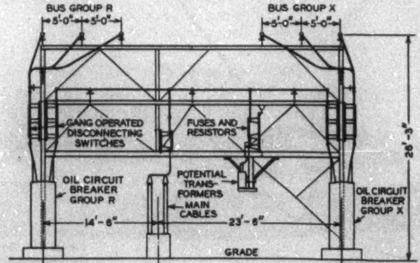

BUS GROUP R
5'-0"
5'-0"
BUS GROUP X
5'-0"
5'-0"
GANG OPERATED DISCONNECTING SWITCHES
FUSES AND RESISTORS
POTENTIAL TRANSFORMERS
MAIN CABLES
OIL CIRCUIT BREAKER GROUP R
OIL CIRCUIT BREAKER GROUP X
14'-6"
23'-6"
26'-3"
GRADE

Figure 3. Thirteen-kilovolt switchyard—cross section of generator and transformer bays

as control or main-lead "bottle necks," where a fire, explosion, or external force in a small area might cause a total shutdown.

3. Reduction of the amount of oil in the switch house to the lowest possible quantity.

Development of the 13-Kv Plan

After making preliminary layouts, it became evident that only about half of the necessary equipment could be housed in the present building with ample spacing, segregation, and smoke and fire barriers. Rather than extend the building to almost double its size, it was decided to locate one half of the switching equipment entirely separate from the other half in an outdoor switchyard. This would eliminate, so far as possible, a major conflagration involving all of the station's switching facilities and, in addition, had the advantage of allowing the outdoor portion to be erected and cut in service with only minor disturbance to the existing indoor busses.

Insulation and Short-Circuit Duty

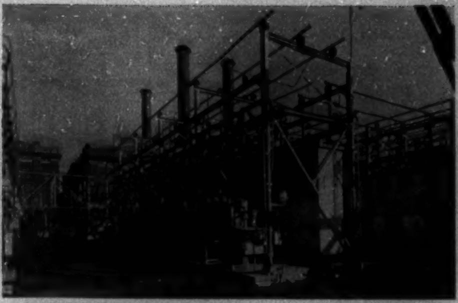
All new apparatus and equipment for 13-kv service was purchased with 23-kv-class insulation. Some of the existing indoor apparatus and equipment reused is of the 15-kv class. All main generator, transformer, and tie-bus breakers have an interrupting rating of 1,500,000 kva at 13 kv.

Outdoor 13-Kv Switchyard

The cross section of a typical generator and transformer bay, and the cross section through the feeder bays are shown in figures 3 and 4, respectively.

As an outdoor arrangement depends to a large extent on air-line distances for segregation, the spacing was made as ample as the available plot would permit, resulting in bus and lead spacing sufficient in general for 26-kv service.

Figure 5. View of 13-kv switchyard



The feeder-cable potheads, line disconnecting switches, current transformers, and feeder reactors were placed in masonry cubicles, which allowed the former indoor-type reactors and disconnecting switches to be used, and saved considerable space over an arrangement using outdoor oil-filled reactors. The cubicles are of masonry to eliminate any heating or magnetic difficulties that might be encountered by the use of a metallic housing.

The connections from three of the generators were routed direct from the turbine room to the outdoor switchyard, so that they are well separated from the generator leads to the indoor sections.

The main oil circuit breakers are 2,000- and 3,000-ampere type *GO-5A*. The feeder oil circuit breakers are 600-ampere type *GO*-3, of 500,000-kva interrupting capacity. All disconnecting switches are of the high-pressure type, those of 2,000 and 3,000 amperes capacity being of extra-heavy construction.

Flexible connections were used on both sides of the 2,000- and 3,000-ampere disconnecting switches to eliminate vibration or expansion stressing the oil-circuit-breaker bushings or disconnecting switches. Figure 5 shows a general view of the outdoor feeder section and figure 6 a view of the 2,000- and 3,000-ampere disconnecting switches, flexible leads, and the oil circuit breakers.

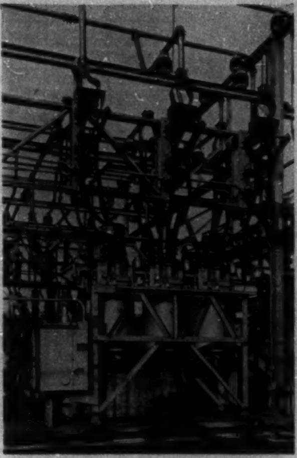
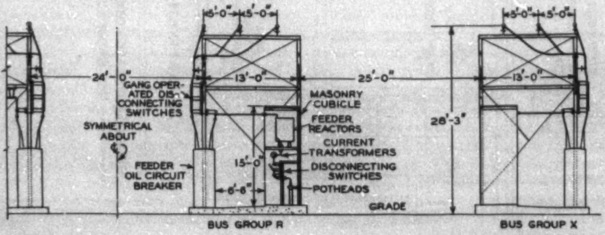


Figure 6. Two thousand- and 3,000-ampere 13-kv outdoor oil circuit breakers, disconnecting switches, and connections

Figure 4. Thirteen-kilovolt switchyard—cross section of feeder bays



Indoor 13-Kv Arrangement

By moving 15 of the 44 feeders and half of the generator and transformer connections to the outdoor yard, the space available for the large-capacity main breakers was more than three times that provided previously. The longitudinal sections, figure 7, compare the relative space occupied by the main equipment and feeder equipment before and after the rebuilding.

Referring to figure 7, the switch house is divided into two halves by walls from the first to the sixth floors, forming a corridor through the center of the building.

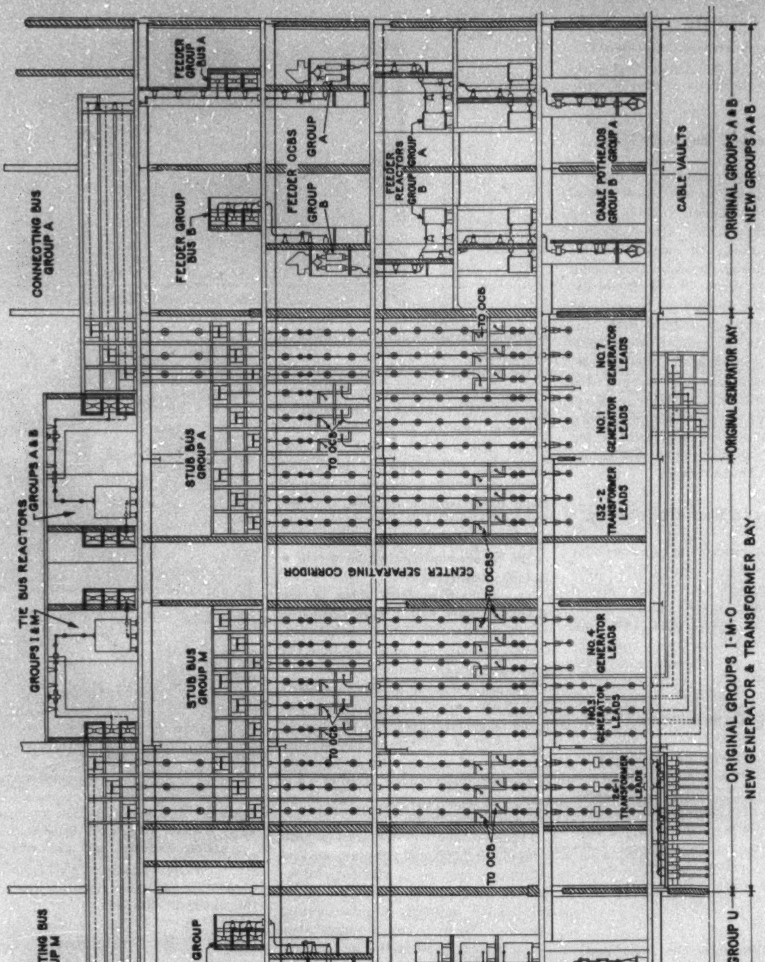

CONNECTING BUS GROUP A
FEEDER GROUP BUS A
FEEDER GROUP BUS B
FEEDER OCBS
GROUP A
GROUP B
FEEDER REACTORS
GROUP A
GROUP B
CABLE POTHEADS
GROUP B
GROUP A
CABLE VAULTS
ORIGINAL GROUPS A & B
NEW GROUPS A & B
TIE BUS REACTORS
GROUPS A & B
GROUPS I & M
STUB BUS GROUP A
TO OCBS
NO. 7 GENERATOR LEADS
NO. 1 GENERATOR LEADS
132-2 TRANSFORMER LEADS
ORIGINAL GENERATOR BAY
CENTER SEPARATING CORRIDOR
STUB BUS GROUP M
NO. 4 GENERATOR LEADS
NO. 3 GENERATOR LEADS
26-1 TRANSFORMER LEADS
ORIGINAL GROUPS I-M-O
NEW GENERATOR & TRANSFORMER BAY
CONNECTING BUS GROUP M
FEEDER GROUP BUS I
FEEDER OCBS
GROUP I
GROUP M
FEEDER REACTORS
GROUP I
GROUP M
CABLE POTHEADS
GROUP I
GROUP M
CABLE VAULTS
FEEDER GROUP BUS M
ORIGINAL GROUP U
ORIGINAL TRANSFORMER BAY
NEW GROUPS I & M

Figure 7. Thirteen-kilovolt switch house—longitudinal section

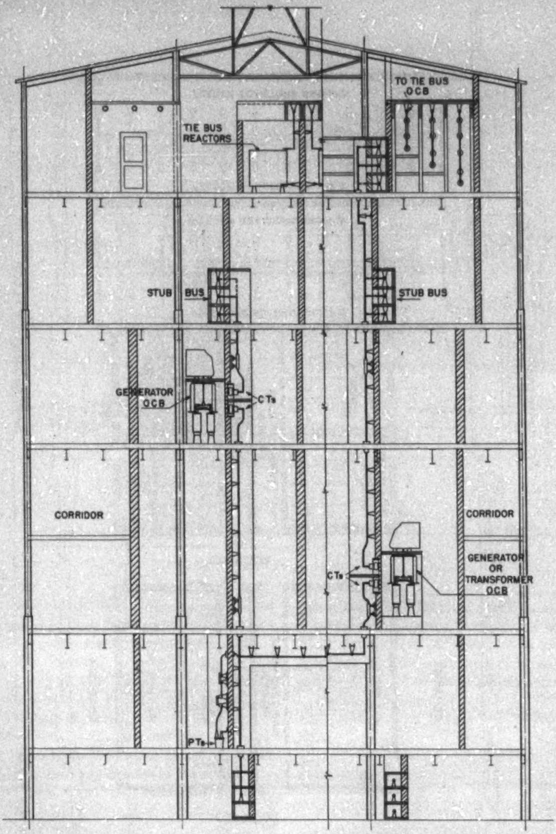


Figure 8. Thirteen-kilovolt switch house—composite cross section of generator and transformer bays

Each half contains a section consisting of two group busses with their generator, transformer, and tie-bus connections. Figures 7, 8, and 9 show the routing of the main and feeder connections and the segregation and arrangement of apparatus. As all oil circuit breakers, except the four for the tie bus, are located on the second and fourth floors, figures 8 and 9 were drawn as composite views, symmetrical about the center line, to include both floor arrangements. To make it possible to place each main oil circuit breaker in a separate room, two of each group were placed on the second floor, and the third on the fourth floor. It should be noticed that each of the four feeder groups, and the main supply leads for each group, are surrounded by barrier walls extending from the first to the sixth floor.

There are no doors or openings from any oil-circuit-breaker room to an adjacent room containing apparatus; access being provided only from the corridors. Smoke and flames, even in case the doors of a breaker room are blown off, cannot come in contact with other breakers or equipment without passing through a second door. All stair wells are enclosed to isolate each floor. All access doors are of steel, tight fitting, with spring-return hinges. The breaker room contains no other equipment, the connections from the breaker pots passing through the back wall of the cell into the lead compartment. The rear walls of breaker rooms, and all walls separating adjoining apparatus rooms, are 12 inches thick, constructed of special concrete brick of tested characteristics. Walls of oil-circuit-breaker rooms on the longitudinal corridors are of eight-inch terra-cotta tile, in order to have less structural strength toward the corridors at the outside of the building than toward adjacent apparatus rooms.

The circuit breakers are the only oil-containing apparatus in the switch house. Potential transformers are of the dry type protected by fuses and resistors. A minimum number were used, and as their ratio is 13,200 to 110 volts, and they are wye connected, only 7,650 volts is impressed across the windings.

By using *FH*-129 oil circuit breakers, oil contents were reduced to the minimum. The total oil content of the 176 breakers in the old switch house was approximately 19,000 gallons, while in the new layout the 44 breakers located indoors contain 660 gallons.

Some of the oil circuit breakers formerly used contained 90 gallons per pole, while the largest present breaker contains 3 gallons per pot or 18 gallons total.

The main breakers are top connected and mounted on post-type insulators, gaining an advantage in that the forces incident to the closing operation are not taken by bushings surrounded by grounded parts.

Type *FH*-203 and *FH*-206 feeder oil circuit breakers of 350,000 kva and 500,000 kva interrupting capacity respectively, were reused, as the feeder reactors limit the duty of the breakers to much less than this value.

The four tie-bus breakers on the sixth floor are in separate rooms similar to those on the floors below. Segregation between the four group connections, equivalent to that on the lower floors, is obtained by corridors and the separating barrier walls. The connection to the outdoor portion of the tie bus, while not shown on any of the drawings included in this paper, passes down to the fifth floor in a separate room and then to the outside of the building, where it connects to the outdoor tie-bus leads on an overhead bridge.

The same general design of masonry cell and bus structure used previously and at Marion and Kearny generating stations, was continued in the new work; special concrete brick being used for vertical barriers and either precast or cast-in-place concrete for horizontal slabs. This construction has been found in the past to be effective as a barrier to stop the transmission of heat, from an arc or fire, to other equipment, it being found in several instances that equipment on the opposite side of an eight-inch wall from a fire or arc had not been damaged. Where practical, portions of the former lead and bus compartments were utilized.

All vertical leads, feeder and main, are insulated with 13/32-inch varnished cambric. Except for the back-connected stud of the feeder breakers, all leads, horizontal or vertical, through wall or floor tubes, are insulated. All floor and wall tubes are sealed with asbestos packing to prevent the passage of smoke from one room to another.

Main Leads

Generator and transformer leads are mainly single-conductor cable insulated with 18/32 inch of varnished cambric, covered with tinned shielding tape and a 6/32-inch rubber jacket, without an external braid.

Fire-Fighting Equipment and Provision for Smoke Removal

Owing to the small quantities of oil involved, 105 gallons, divided among the 42 pots of seven oil circuit breakers, being the largest amount contained in any one room, portable equipment was considered sufficient for extinguishing oil fires. Seventy-five-pound portable CO_2 extinguishers are located on each floor in sufficient number to produce a 50-percent CO_2 concentration in the largest oil-circuit-breaker room on each floor. Every oil-circuit-breaker room is provided with one or two small openings in the wall through which the CO_2 funnel may be inserted from the corridor and a cover closed to fit tightly around the pipe at the rear of the funnel. In this manner the CO_2 can be blown into the room without the use of the main access doors. As a second line of defense, special water-spray nozzles are provided which throw a finely divided spray or mist and use only a small quantity of water. The nozzles are permanently set so that a solid stream cannot be obtained, but the spread of the spray is adjustable from approximately 30 to 150 degrees.

Three portable two-inch rubber hoses are provided on each floor, each with a $1^1/_2$-inch spray nozzle permanently attached, and so located that at least two are available for each oil-circuit-breaker

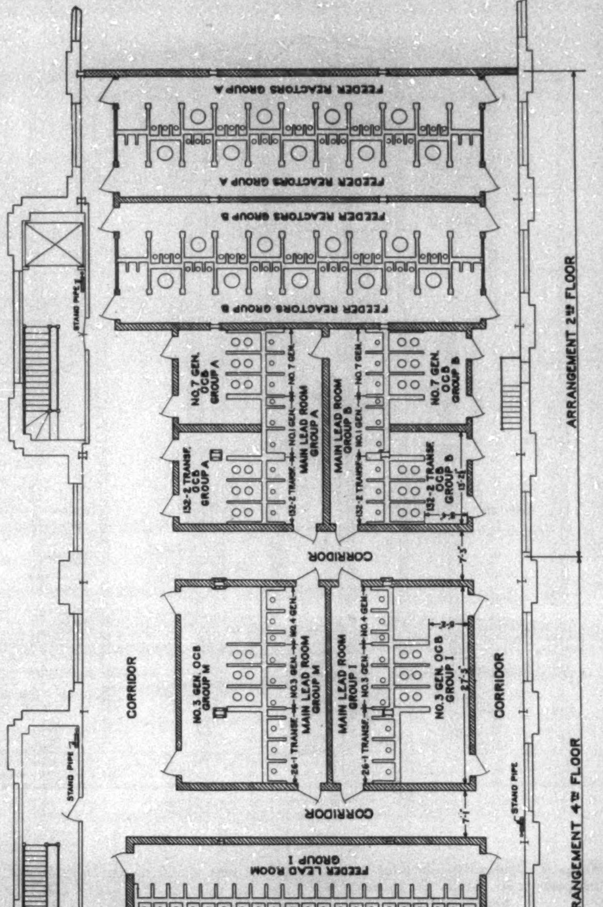
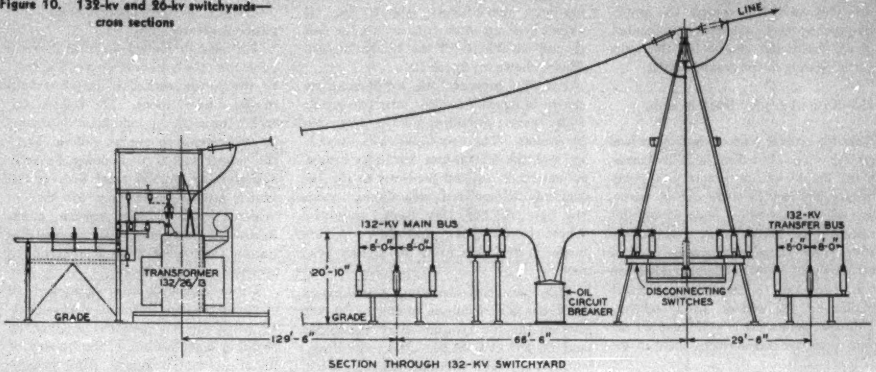
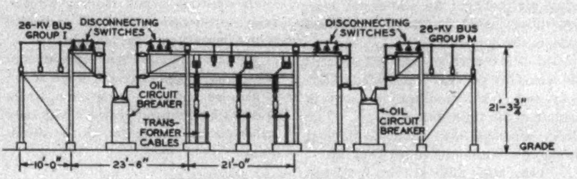

FEEDER REACTORS GROUP A
FEEDER REACTORS GROUP A
FEEDER REACTORS GROUP B
FEEDER REACTORS GROUP B
STAND PIPE
NO.7 GEN. OCB GROUP A
NO.7 GEN.
NO.1 GEN.
132-2 TRANSF.
MAIN LEAD ROOM GROUP A
MAIN LEAD ROOM GROUP B
NO.7 GEN.
NO.1 GEN.
132-2 TRANSF.
NO.7 GEN. OCB GROUP B
132-2 TRANSF. OCB GROUP A
132-2 TRANSF. OCB GROUP B
CORRIDOR
CORRIDOR
NO.3 GEN. OCB GROUP M
26-1 TRANSF.
NO.3 GEN.
NO.4 GEN.
MAIN LEAD ROOM GROUP M
MAIN LEAD ROOM GROUP I
NO.3 GEN. OCB GROUP I
CORRIDOR
STAND PIPE
CORRIDOR
STAND PIPE
FEEDER LEAD ROOM GROUP I
7 FEEDER OCBS GROUP I
7 FEEDER OCBS GROUP M
FEEDER LEAD ROOM GROUP M
ARRANGEMENT 2ND FLOOR
ARRANGEMENT 4TH FLOOR

Figure 9. Thirteen-kilovolt switch house—composite second- and fourth-floor plans

Figure 10. 132-kv and 26-kv switchyards—cross sections



SECTION THROUGH 132-KV SWITCHYARD



SECTION THROUGH 26-KV SWITCHYARD

room. These spray nozzles may be inserted in the above mentioned hand holes in the same manner as the CO_2 funnels.

By installing a raised saddle at the bottom of each access door, a two-inch sill is obtained which is sufficient to keep oil from flowing from the floor of the breaker rooms out into the corridors. This sill also will assist in holding water from the spray nozzles in the rooms.

Previous to this rebuilding program, the 3,750-kva station auxiliary power transformers, housed in a section of the building adjoining the switch house, were equipped with a permanently piped foam system with a foam generator. All smaller indoor auxiliary power and lighting transformers have been replaced with transformers filled with noninflammable fluid.

Fire hydrants, connected to the city fire mains, are located at strategic points in the three outdoor yards so that all breakers and transformers may be easily reached with portable hoses equipped with the large spray nozzles supplemented by fog nozzles mounted on a 90-degree elbow with a 15-foot aluminum extension.

Fans are located in the outside east wall of the building on each oil-circuit-breaker and reactor floor in sufficient numbers to clear the corridors of smoke and fumes. Previous experience has shown that these fans are effective, and it is expected, particularly in view of the small quantities of oil contained in any one room, that the corridors will be sufficiently cleared of smoke to permit access to the small fire-fighting openings. The fans are controlled from a remote location.

Interlocks

Interlocks of the key type prevent the opening or closing of isolating disconnecting switches when the oil circuit breaker is in the closed position. In the case of stick-operated disconnecting switches, the locks are applied to the compartment doors.

Grounding

Grounding of apparatus and equipment in the indoor switch house is accomplished by a grid system consisting of four-inch by one-fourth-inch copper bars tied together so that the effective cross section between any piece of apparatus and ground is at least two square inches. All apparatus is grounded to this grid, the oil circuit breakers being grounded at two points. The double grounding of breakers and the two-way flow in the grid makes it possible to have a poor connection at any one point and still retain good grounding conductivity and capacity. Likewise, the grounding of all of the outdoor switchyards consists of a similar grid system. Cable ties totaling 2,500,000 circular mils cross section connect the 132-, 26-, and 13-kv outdoor and 13-kv indoor grounding systems.

Neutral Grounding Systems

The neutrals of the 13-kv generators were previously grounded through a two-ohm resistor of one-minute thermal capacity. As overheating troubles with the neutral resistors had occurred when a ground did not clear quickly, additional precautions were thought to be necessary to make absolutely certain that in case of any failure of the protective devices to function, the system would not be ungrounded by the burning out of the resistors. The continuous capacity of this two-ohm resistor is 450 amperes. As replacing it with a continuous-capacity resistor good for the 3,800 amperes which would be obtained by full phase-to-neutral voltage being maintained across the resistor was impractical on account of the space occupied and the excessive cost, a 12.9-ohm resistor of 450 amperes continuous duty was installed, normally short-circuited by a single-pole oil circuit breaker. A timing relay actuated by the ground current opens this breaker if ground current flows for 30 seconds, and the total of 14.9 ohms reduces the current to 450 amperes, the continuous capacity of both resistors. The 132-kv system is dead grounded, while the 26-kv system is grounded through a 75-ohm resistor which has an oil circuit breaker actuated by a timing relay, so that in case a ground does not provide sufficient current to operate the protective ground relays, the resistor is shunted, giving additional chance for the automatic clearing of the ground fault.

132-Kv and 26-Kv Switchyards

As the greater part of the future load growth will be carried on the 26-kv busses, a site on the station property, approximately 900 feet from the switch house, was selected, providing space for an ultimate of six 132-kv lines, four 132-to-26-to-13-kv transformer banks, and if necessary, 40 26 kv feeders, divided into four two-group sections in a manner similar to the indoor 13-kv scheme. This site permitted a well-balanced design, with the transformers between the 132-kv and 26-kv portions, and placed the 26-kv feeders close to the duct lines entering the property and thus relieved the duct congestion adjacent to the switch house.

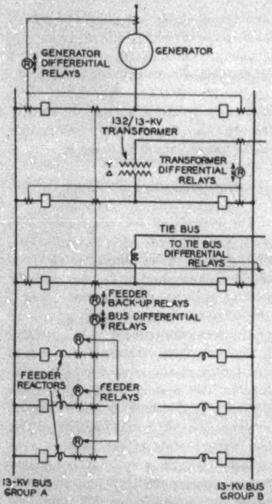


Figure 11. One-line diagram—13-kv group-bus relay protection

The 132-kv layout is of the ground-bus A-frame type, a standard for the switching stations on the Public Service Electric and Gas Company's system.

In the 26-kv portion, the general structure has been lowered in height and spread out more than the standard 26-kv layouts previously used, to remove any busses and disconnecting switches from locations immediately above the oil circuit breakers and approaches the flat ground-bus scheme of the 132-kv design. This is shown on figure 10.

For the present, the equipment as shown in figure 2 suffices, one two-group 26-kv section providing for the seven 26-kv feeders. The two 45,000-kva 132/13-kv and the 50,000-kva 26/13-kv transformer banks existed previous to the rebuilding. Connected as shown, with the new 132/26/13-kv bank, a power source to all 13-kv and 26-kv feeder busses is available from the 132-kv system.

This new three-winding transformer bank has a continuous rating of 50,000 kva on the 132-kv and 13-kv windings, and 60,000 kva on the 26-kv winding, with 33 per cent overload capacity with forced air cooling. An additional overload rating, based on the system's load cycle, is also available.

Load ratio control of plus ten per cent and minus five per cent is provided on the 13-kv and 26-kv windings, to assist in regulating the three bus voltages. The 132-kv windings have standard no-load tap changers with one five-per-cent tap.

A-c calculating-table studies indicated that the above kilovolt-ampere ratings and range of voltage regulation formed the best economical balance for the results desired.

Control

Partly due to the distance between the 132-kv and 26-kv switchyards and the switch house; and partly to the desire for segregation, two separate control rooms were decided upon, one located in the switch house for both indoor and outdoor 13-kv sections, and the other in the center of the 132-26-kv switchyard for the remaining apparatus.

The 13-kv control room is located in the end of the switch-house building, toward the outdoor 13-kv switchyard, in a section separated from the switching equipment. This location permitted the control from the indoor and outdoor 13-kv sections to enter the conduit room at opposite ends; the indoor, from the adjacent switch galleries, and the outdoor, from a shaft built into the end wall of the building. The control board is divided into an indoor and outdoor section, running longitudinally, on opposite sides of the room, so that the controls from the indoor and outdoor sections can be racked on opposite sides of the room, without any crossovers or congestion. The new control room is larger than the previous room, and with less than half the circuits to control, the spacing and segregation is greater.

The outdoor control is run in armored cable from the bottom of the vertical shaft to the panels, racked in metal troughs in the conduit room. The indoor control is run from the individual apparatus in rigid conduit to conduit galleries along the outside wall of the building, formerly a portion of the old third floor of the switch house, where they are run in armored cable to the conduit room. Additional segregation is obtained by routing the control of alternate groups to opposite conduit galleries.

Trip-coil supervision is obtained on all oil circuit breakers by means of an indicating lamp, which is energized in either the closed or open position of the breaker, if the trip circuit is intact. This includes the breaker trip coil and auxiliary switches, relay test switches, and all protective relays that may trip the breaker.

The same precautions in segregation of control have been exercised in the design of the new outdoor control room, which operates on a separate battery with only minor control connections to the switch house.

Protective System

Differential protection consisting of type *CA* and *CA*-4 relays, with the current transformers overlapping, is used on all apparatus and busses, so that there are no unprotected spots. Current trans-

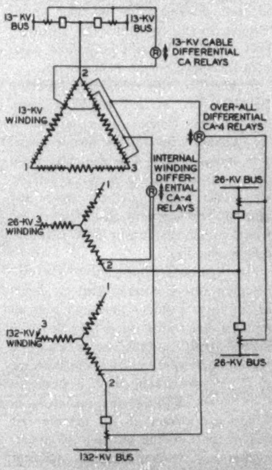

13-KV BUS
13-KV BUS
13-KV CABLE DIFFERENTIAL CA RELAYS
R
2
13-KV WINDING
1
3
OVER-ALL DIFFERENTIAL CA-4 RELAYS
INTERNAL WINDING DIFFERENTIAL CA-4 RELAYS
26-KV BUS
26-KV WINDING
132-KV WINDING
26-KV BUS
132-KV BUS

Figure 12. One-line diagram—three-winding transformer-bank relay protection

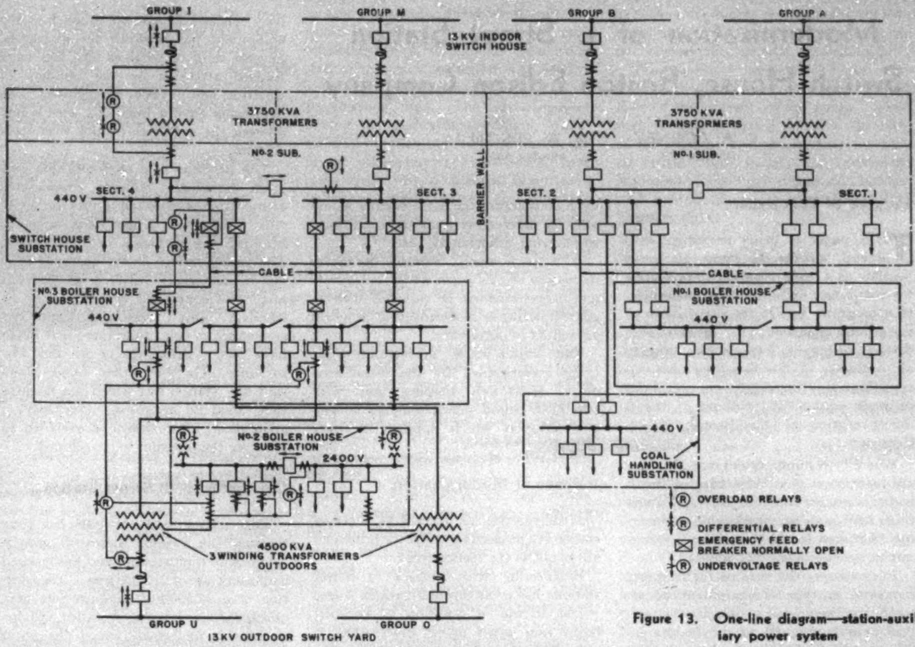

GROUP I
GROUP M
13 KV INDOOR SWITCH HOUSE
GROUP B
GROUP A
3750 KVA TRANSFORMERS
3750 KVA TRANSFORMERS
NO.2 SUB.
NO.1 SUB.
BARRIER WALL
440 V
SECT. 4
SECT. 3
SECT. 2
SECT. 1
SWITCH HOUSE SUBSTATION
CABLE
CABLE
NO.3 BOILER HOUSE SUBSTATION
440 V
NO.1 BOILER HOUSE SUBSTATION
440 V
440 V
COAL HANDLING SUBSTATION
NO.2 BOILER HOUSE SUBSTATION
2400 V
4500 KVA 3 WINDING TRANSFORMERS OUTDOORS
GROUP U
GROUP O
13 KV OUTDOOR SWITCH YARD
OVERLOAD RELAYS
DIFFERENTIAL RELAYS
EMERGENCY FEED BREAKER NORMALLY OPEN
UNDERVOLTAGE RELAYS

Figure 13. **One-line diagram—station-auxiliary power system**

formers in the same protective zone were purchased with matched characteristics and tolerances limited up to the maximum through short-circuit current values.

Feeder reactors limit the current on feeder faults to a value well within the capacities of the breakers. With the current transformers located as shown in figure 11, the feeder relays do not function on faults between the reactor and breaker, a fault so located being cleared by the bus differential relays. A 36-cycle time delay is interposed between the tripping of the main source relays and the feeder relays, so that in no case do the feeder breakers interrupt the bus short circuit. To provide back-up protection for the feeder breakers, the source differential-current transformers are summarized through three *CO* overcurrent relays before reaching the bus differential relays, so in case a feeder oil circuit breaker fails to open, the group bus will be cleared.

A spare oil circuit breaker and transfer bus are included in the 132-kv switchyard to replace any regular breaker. For all replacements except when used for the bus section breaker between sections 3 and 4, the bus differential is maintained by transfer of secondary control circuits.

The new three-winding transformer has three sets of differential relays, as shown in figure 12, consisting of an "internal" for winding protection, an "over-all" for 132-kv and 26-kv leads and bushings, and a "cable" differential, for the 13-kv cables. This has been found desirable due to the difficulty experienced in phasing and testing a single over-all differential on a three-winding bank where one winding is delta connected. An added advantage is that the internal protection remains intact during any switching changes necessary when the spare oil circuit breaker is used.

Auxiliary Power

The existing auxiliary power system consisted of a four-section 440-volt bus with nonautomatic section breakers, without smoke or fire barriers to isolate the various sections. This bus was divided into two parts, as shown in figure 13, with a barrier wall between, and the cables rearranged so as to have no interconnections between the two halves. The two sections of each half are joined by a tie oil circuit breaker with overcurrent relays set so that in case of a 440-volt bus fault, the two sections will be separated before the overcurrent back-up relays on the transformer breakers operate. It was not feasible to install bus differential protection on the existing busses. Cables between the main auxiliary substation and the boiler-house substations are equipped with differential protection and overcurrent back-up protection to clear bus faults.

To supply the auxiliaries of the new turbine generator and boilers, a new boiler-house substation, No. 2, was installed. Differential relays protect the two sections of the 2,400-volt metal-clad bus which are separated by a barrier wall. This bus is fed by two new 4,500-kva 13-kv to 2,400-volt to 440-volt transformers connected to two of the outdoor 13-kv groups. The 440-volt windings provide an additional power source to a portion of the existing 440-volt bus. The auxiliary power system will normally operate in three parts; sections 1 and 2 and No. 1 boiler-house substation, fed from indoor groups *A* and *B;* sections 3 and 4, fed from indoor groups *I* and *M;* and Nos. 2 and 3 boiler-house substations fed from outdoor groups *O* and *U.*

With this division, faults on the 440-volt and 2,400-volt busses can only shut down a portion of the auxiliaries. In addition, duplicate essential auxiliaries, wherever possible, were reconnected to obtain their supply from different bus sections.

Conclusions

In the reconstruction of Essex generating station, no unique or unusual apparatus or design features were used. Rather, with part existing equipment, new commercially available apparatus, and refinements in standard designs of structures, a plan was developed which it is believed will provide a high order of reliability. Emphasis was placed on separation and segregation as a second line of defense to stop the spread of a conflagration. In striving toward more reliable future designs, circuit breakers without inflammable fluid would be of most assistance. With concentrations of large blocks of power, safe back-up protective schemes are desirable in addition to bus and apparatus differential, and more sensitive transformer protection would be welcomed. In an attempt to obtain the latter, it is intended to make a trial installation of the so-called "gas relays" on the transformer banks at Essex generating station.

Discussion

For discussion, see page 778.

Modernization of L Street Station Switch House, Boston Edison Company

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Purpose of This Paper

THIS paper is being presented with several objects in view, to serve possibly as a guide or outline of procedure for companies undertaking a modernization program, to list the main items of equipment installed, to discuss why certain selections were made, and to aid engineers in studying their own stations by indicating dangerous conditions or weak equipment as found at the L Street station of the Boston Edison Company.

Where type numbers and manufacturer are mentioned it is done to give credit to the manufacturer who produced something new or spent considerable engineering effort and to aid the reader in determining quickly what was adopted.

In discussing the reasons for adopting a system or type of equipment no attempt has been made to indicate the relative weight assigned to the factors involved. Obviously the final conclusions even considering the same factors would vary for different systems and conditions, engineering preferences, etc.

It is particularly desirable to invite discussion of this paper as the comments and suggestions will be valuable to those contemplating a modernization program as well as to ourselves.

The switch-house modernization is being accompanied by the installation of a 37,500-kva topping unit with two 1,200-pound pulverized-coal boilers, a description which appears in other articles.

Purpose of Modernization

Modernization as applied to a power station is a modern term but the principles are as old as the station itself.

Engineering with respect to power stations has gone through various stages—first it was a question of building larger and larger power units—then it was economy at any cost—this was followed by recognition that fixed expenses are important—and now we have modernization expressing itself in segregation.

There are several reasons why this subject has recently attracted such interest. Many of the older and larger stations have reached an age where much of the equipment has outlived its usefulness. Several cases of troubles and fires have focused attention on the question of electrical and physical segregation. The depression gave a breathing spell which allowed the engineers to look over their stations.

The adoption of a modernization program sometimes requires considerable courage as the expected results are somewhat intangible. There may be no or little increase in station load or capacity. The actual service to the customer may not be materially bettered as the past history of continuity of service may be excellent. Often there is no definite circumstance to point to as there may never have been a serious fire or electrical disturbance. On the other hand, there isn't any station but which has features that could be improved. Obviously, a great deal of care must be exercised in selecting the program to be followed.

Description of L Street Station

This station built in 1898 has gone through the various stages of growth common to such stations and has reached its present size of 210,967 kva. The addition of a 37,500-kva high-pressure unit number 12 now being installed will increase this to 248,467 kva. The seven 14-kv units were connected to a double ring bus which was divided by the breakers into four sections. The outgoing lines, tie lines, station transformers, etc., totaled 68 cell positions and were distributed along the ring bus.

Each 14-kv generator has a four-ohm neutral resistor and circuit breaker, one only being in service at a time.

The electrical equipment per line is located on three floors—reactors and ducts on first floor, busses on second, and oil circuit breakers on the third. The fourth floor was empty. The switch house is in two connecting buildings, but in general there was practically no segregation between main and auxiliary busses or between bus sections.

Preliminary Studies

Due to the increased generating capacity and to tie lines with Edgar generating station and with New England Power Company the calculated short-circuit fault current far exceeded the ratings of many of the oil circuit breakers. Several years ago a study was started for increasing the rupturing capacity of these breakers. These are General Electric type *H* breakers and

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Paper number 39-58, recommended by the AIEE committee on power generation, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 23, 1938; made available for preprinting December 29, 1938.

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vary from the original design to the more recent installations. It would have been quite expensive to modernize or replace these breakers to meet the required rupturing capacity since the ring bus is normally operated closed and there are no bus sectionalizing reactors.

The next scheme studied called for a synchronizing bus with reactors connected to each of the four bus sections. As the bus tie breakers could now be normally opened the short-circuit duty per bus section would be very materially reduced. A bus fault should shut down only one-fourth of the station instead of the whole bus. Also the voltage disturbance of the sections not in trouble would be much less.

Electrically this layout was decidedly an improvement but due to the arrangement of busses and breakers in the switch house the modern principles of segregation could not be applied effectively.

This led to a proposal providing for an entirely new and separate switch house having main and auxiliary busses divided into four sections. These sections would be tied together through reactors and a synchronizing bus although bus tie breakers would be installed for load transfer purposes at light loads or in emergencies (figure 1).

Each section would have the switching equipment for two generators which would be transferred from the present bus and would also have switches for a group feeder tie connected to the present corresponding line bus section. The tie breakers between the line sections would be removed and installed between generator bus sections.

Partition walls would be installed between sections of the generator busses and between sections of the present line busses so that electrically speaking the station would be divided into four groups each consisting of a double-bus generating station with two generators and feeding a double-bus substation with the various transmission lines. The advantages of having the generating switching equipment in a switch house incorporating the newer principles of construction were considered to be of sufficient importance to warrant the increased cost over the preceding scheme. These features will be discussed in detail later on.

There were three locations considered for the generating-bus switch house.

A new building would have given complete separation from the present switch house, but the cost was excessive and the space available was somewhat limited.

Space was available in the engine room or boiler room of a building which had recently been cleared of equipment. This would have given good segregation from the rest of the station and the cost would have been reasonable as the building, floors, and foundations are in good condition. Use of either of these rooms, however, would have seriously interfered with possible use of the building as a future boiler and turbine station. The biggest disadvantage was the problem of running the generator and group feeder leads between this and the present station. Based on statistical records the expected faults with lead cables and potheads would have been entirely unacceptable for a generating station. Insulated cable mounted on porcelain insulators would have required a tunnel or overhead enclosed bridge either of which would have been very expensive as well as having the danger of shutting down the entire station in case of a serious fault or fire, earthquake, air raid, or sabotage.

The necessary additional space was secured by using the unoccupied fourth floor of the present switch house. This floor is entirely sealed off from the rest of the building and can be entered only by outside stair wells. The headroom is somewhat small and introduced serious problems but the width is ample and the length is right.

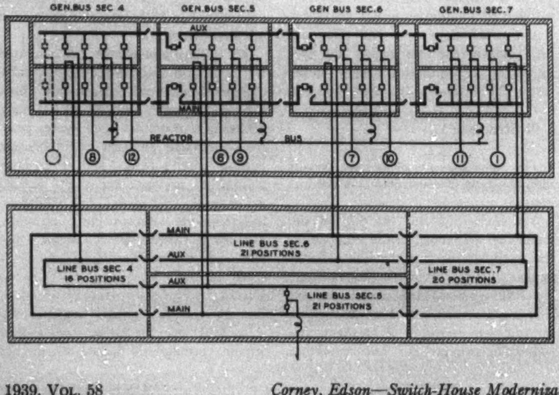


Figure 1. Single-line diagram—generator and line bus sections

Switch-House Design (Figures 2 and 3)

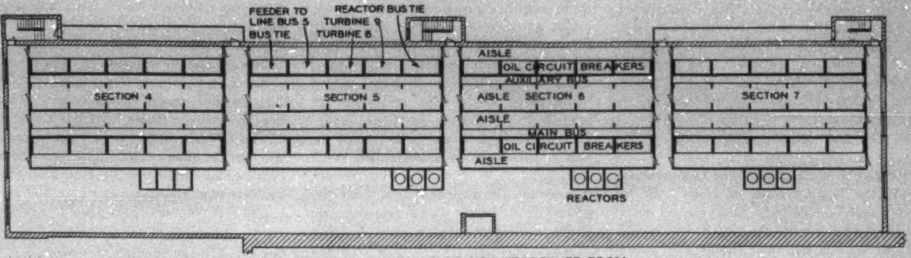
The only building work required was to close in a skylight, brick in a few windows, and install two doors connecting to the stairs by outside walkways of fire-escape construction. The strength of the floor was checked to insure it was suitable for the weight of the additional switching equipment and cell construction.

In line with the principles of physical separation it was decided to locate the switching equipment for the four bus sections in individual rooms separated by five-foot aisles. These aisles serve as safety exits since workmen leaving a bus section in trouble do not have to pass through other section aisles. The doors swing outward into the aisles and are equipped with door closers. A fire or explosion in a bus section might swing open or blow off a door but fire being forced into other bus rooms is improbable as their doors would be tightened against their jambs and as the aisles act as expansion chambers. The doors are equipped with wire-glass windows to permit inspection without the operator entering the room.

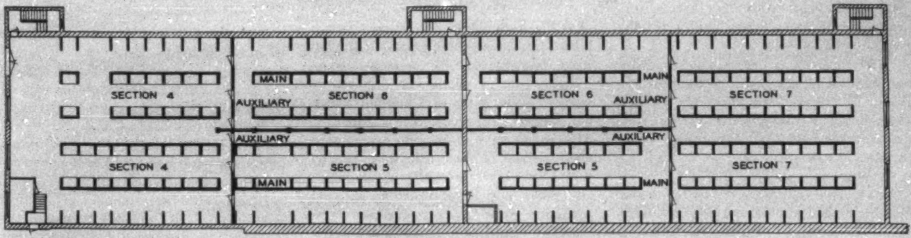
Each room is divided longitudinally into four aisles—one for the main bus and disconnects, one for auxiliary-bus disconnecting switches, and instrument transformers, and two aisles for the main and auxiliary oil circuit breakers. The bus structure and breakers cells are of reinforced concrete. All other walls and partitions are two-inch walls having a metal lath specially reinforced and three coats of plaster on each side.

Fire Protection—Gas (Figure 4)

A CO_2-gas Lux fire-protective system is installed with thermostats to operate an annunciator or to discharge into the particular aisle in trouble. Manually operated selector valves and a bank of reserve gas cylinders are also available.



PLAN — GENERATOR BUS AND OIL-CIRCUIT-BREAKER ROOM



PLAN — LINE OIL-CIRCUIT-BREAKER ROOM

Figure 2. Plan—generator-bus and line-circuit-breaker floors

Fire Protection—Water

There is also a system of eight Los Angeles water fog nozzles installed for each aisle. The 16 feeder pipes are brought out into the stair wells to a six-inch header, the connections being made through short lengths of removable hose normally disconnected as an aid to the operator in insuring selection of the right valve. As a further check there is a set of indicating lamps at the valves—one to show if the bus is alive, one to indicate a bus-differential—relay operation, and one to be operated by the switchboard operator. Hose and reels with portable fog nozzle applicators are provided for mop-up work.

The fog type of nozzle was selected in preference to the spray type as it was felt that the fog would more readily roll behind the cell doors and around the apparatus and wiring within the cells or bus structure. The nozzles are located in front of alternate cell barriers and tests indicate both cells are well filled when 100-pounds water pressure is at the nozzles.

It is intended to rely on the city fire-department pumper to supply the six-inch header; however, the system can be connected to the 60-pound city water supply. As all hose connections correspond to the Boston fire department it can supply directly the individual aisles or the portable applicators.

Each aisle has suitable drains and a four-inch dam at each end.

Ventilation (Figure 5)

Two exhaust fans are mounted on the roof. Separate ducts with manually operated dampers are run at one end of each aisle. An automatic louver is mounted above the door at the other end of the aisle. After a fire the proper damper can be opened and the aisle scavenged of smoke. This system may also be used for ventilation in hot weather if found to be necessary. The aisles and area outside the bus rooms are connected to the ventilating system for the rest of the station.

Cell Structure

The question of metal-clad versus masonry type of construction in the bus rooms is quite difficult to decide.

The engineering and drafting for the metal-clad equipment would be done by the manufacturer but there would still be many drawings required to show interconnections, main leads, and control. The manufacturer would be responsible for complete assembly of breakers, disconnecting switches, etc., but assembling the units, lining up, and making main and control connections would still have to be done in the field. Quotation on the metal-clad would be independent of possible future labor troubles or price increases. Flexibility and ease in relocating equipment in the future are not of much moment as such a change is not expected and as the cost of interconnections, main and secondary wiring, etc., would have to be written off in either case. In the metal-clad construction the individual-phase equipment and conductors are surrounded by metal so that most faults should be ground faults. This is a very important point, but it is believed that the equivalent has been approached in the concrete construction adopted (figure 6).

All circuits and phases are separated by metal lath and plaster barrier walls. Ebony-asbestos cell doors are used throughout and Transite or Micarta enclosing barriers are used where practical. All wall bushings and tubes are packed with asbestos and water-glass stuffing to prevent seepage of ionized gas or smoke. The bases of all insulators, bus supports, disconnecting switches, instrument transformers, fuses, and breakers are directly grounded by copper strap so that flashovers should be to ground. Equipment-supporting steel, through conduit, ground copper, etc., is insulated from the barrier walls so that ground currents will follow the ground copper and not the building or wall steel. Reinforcing rods in the concrete were spaced at the ends and were taped at crossings to prevent ground currents as well as possible circulating currents. An attempt was made as far as possible to keep reinforcing rods a good distance from busses or heavy currents. Care was taken to prevent possible metallic loops of supporting steel or reinforcing rods around current-carrying parts. It is realized that there are a few spots where heavy conductors are somewhat near building steel and they will be watched and remedies made if found necessary.

Due to the low headroom there are a few places where exposed live parts are only a few inches from concrete. In such cases the cell was lined with one-fourth-inch Micarta barriers.

Gang-operated disconnecting switches in a metal-clad switchgear are somewhat safer as they can be enclosed and remotely operated but the cell type of construction offers the advantages of visibility for the individual blades and of better separation from the bus. A green indicating lamp is being installed at each set of disconnects to indicate the open position of the associated circuit breaker but main reliance will be placed in the "tag-out" procedure.

In the metal-clad gear interlocks either mechanical or key type can be installed between the breakers, disconnecting switches, and cell doors, but their desirability may be mitigated somewhat by the possibility of giving the operators a false sense of security. Interlocks are valuable for small substations or industrial switchgear installations but are not essential for large generating stations having highly trained operators accustomed to rigid operating procedures.

It must be kept in mind, however, that notwithstanding the above apparently unfavorable comments the idea of factory-assembled switching equipment is good and the Boston Edison Company has many installations in the generating and substations as well as in customers' stations. In this particular installation the estimated saving of the cell type over the metal-clad was too great to be disregarded.

Oil Circuit Breakers

The release of the type *H* generator and bus tie breakers from the line bus forced the new structure design to be suitable for this type of breaker. This was acceptable as the record of the type *H* breaker over the whole life of the station has been exceptionally good, and as their oil contents are relatively small. Before reinstalling these breakers they are modernized and brought up to 1,000,000-kva rupturing capacity and 23-kv insulation by adding new explosion chambers, 45 degrees silver contacts, new insulators, etc. As the subcell disconnecting switches could not be used in the new layout the breakers were changed to be top connected.

Because of several instances of pumping due to breakage of dogs or stops in the mechanism it was decided to add an antipumping interlock.

The use of bus selector breakers only introduced operating and synchronizing difficulties but the cost of a series breaker could not be justified.

Nine new *FH*126 breakers with motor-operated mechanisms were installed to supplement the relocated breakers.

Bus and Cell Structure

The main and auxiliary bus structures consist of horizontal reinforced-concrete shelves with removable Transite front covers except at bus taps. All of this concrete was mechanically vibrated while being poured to obtain a more homogeneous structure. As the expected short-circuit current is fairly small (20,000 amperes) the magnetic

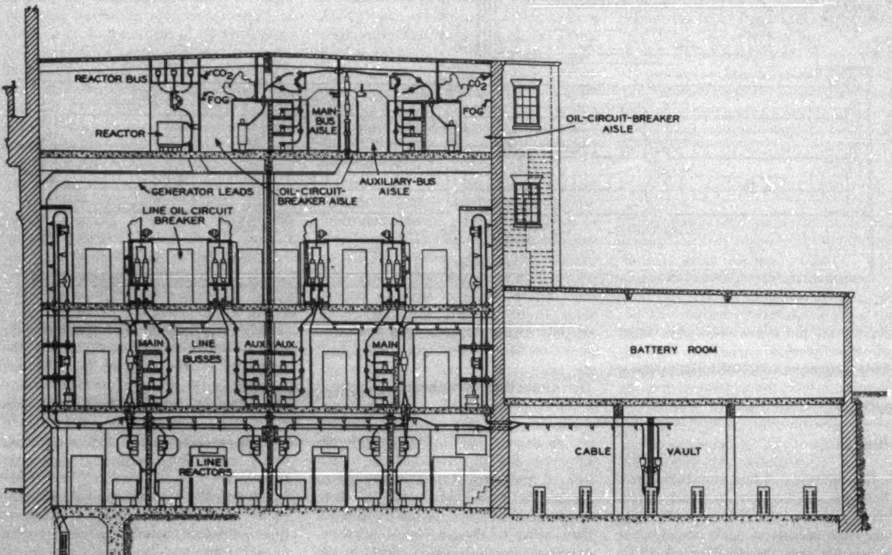


Figure 3. Cross section—switch house

forces between phases are not large. It is not felt that the longitudinal forces tending to strip the bus through the end clamps are serious.

All cells are equipped with removable hanging doors. The design of the cell doors has an unvarnished one-fourth-inch black ebony asbestos panel with a wooden frame impregnated with fire resistant paint. Various materials were considered but the above was selected as it is fireproof and will not glow, it is strong mechanically, will not absorb moisture or warp, does not give off excessive fumes or smoke during a fire, and being homogeneous it may still be serviceable after grooves or scorched parts are sandpapered out. There are several materials available which do not require a frame, but usually a binding around the edges and metal corner plates are needed to prevent chipping. This introduces a hazard to an operator hanging a cell door in front of live apparatus.

Busses

Several types of bus were considered. Bare bar, tubing, or structural shapes of copper or aluminum could easily carry the current of 3,000 amperes but it was felt that the disadvantage of an insulated bus in increased cost and the possibility of fire and smoke was well outweighed by the advantage of an insulated bus in reducing flashovers caused by dirty or broken insulators, rodents, or ionized gases.

Two 3,000,000-circular-mil insulated cables per phase mounted on 23-kv insulators were selected for main and auxiliary busses as this was materially cheaper than copper bar or tubing having formed or wrapped insulation. Vertical bars to give good ventilation would have introduced serious difficulties in making taps as the headroom was limited.

Similarly, all bus taps and cell wiring are insulated cables mounted on 23-kv insulators with all joints filled and taped to a streamline shape.

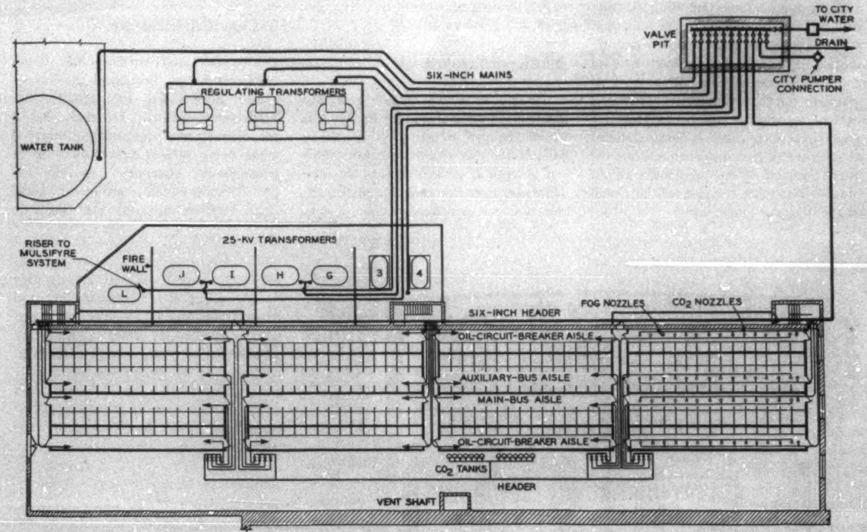


Figure 4. Fire protection system—transformers and switch house

Disconnecting Switches

The disconnecting switches selected are single-pole stick-operated with 23-kv type *B*-3 insulators. Due to the lack of headroom it was impossible to use a front-connected switch as there was insufficient space for the top connections going to the oil circuit breakers. A rear-connected design particularly the 3,000-ampere size has the following objections: Any undue strain in service or during erection tending to turn the through stud as might be caused by the weight of cables or by a mechanic putting too much force on the nuts would cause misalignment. Similarly, any expansion of the stud due to temperature might loosen the assembly and cause misalignment. The terminal-stud passage through the insulator is not ventilated and the temperature of the stud is increased. The temperature of the contact clips would tend to be increased due to the distortion of the current distribution by the magnetic action. There is also danger of corona between the switch stud and the inside of the porcelain bushing. Due to the right-angle path of the current the forces on the switch lock are materially increased during short-circuit conditions.

These mechanical and heating difficulties were compensated for in the proposals of the various manufactures by pinning the studs to prevent turning using two or more straps instead of a round stud, coating the inside of the bushing, increasing the current-carrying parts, etc. The Delta-Star company, however, developed an entirely new design for disconnecting switches of this capacity. This switch consists of two post-type insulators mounted at right angles to each other and mounted on an angle-iron base. The switch blade is located

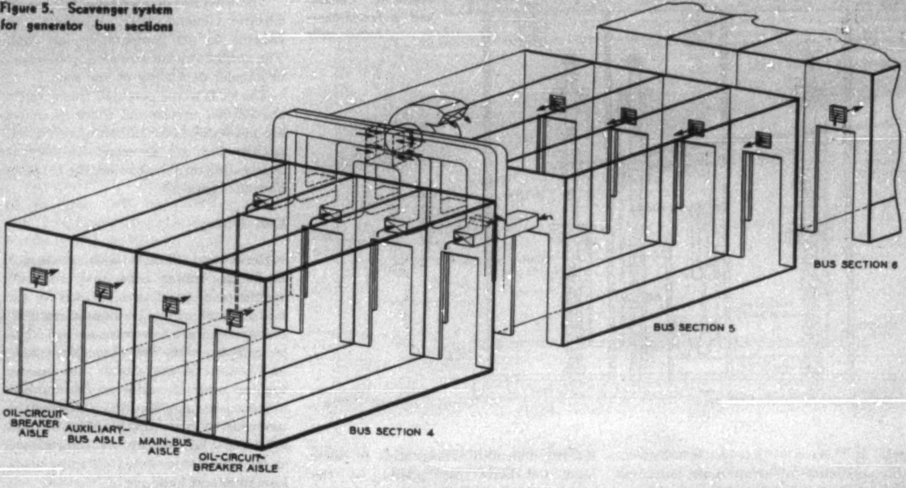


Figure 5. Scavenger system for generator bus sections

across the two outer ends of the insulators. The terminal blocks can be rotated for the connections to be to the front, in line, or to the rear at either top or bottom stud. Obviously, such a design offers the advantages of a front-connected switch, the interchangeability is good, bus-type insulators are used instead of through bushings, there is no base plate to have possible heating by eddy currents, installation is cheaper as two switches per phase are mounted on an angle at the factory, the problem of accurate alignment of supporting steelwork to prevent distortion does not exist, and the first cost is materially less than for equivalent rear-connected switch.

Area type of contacts using double blades gripping a central tongue was selected over the line type as it was felt the latter is more sensitive to accurate adjustment. As an extra precaution, however, the contacts are somewhat flexible and pressure is maintained by a central spring washer.

The contacts are silvered, not with the intention of increasing the capacity of the switch but with the expectation of prolonging the life of the contacts.

A bar type of lock was selected as it was felt a latch on a round stud might wear and become ineffective.

Current and Potential Transformers

The present generator current transformers will be used in the new structure. These are 15 kv whereas all the rest of the equipment is on the 23-kv basis; however, these transformers are within the generator differential protection and the cost of new ones was not felt to be justifiable.

The current transformers used for bus protection are new General Electric type *KC*-59*Y* and are of special design to have good ratio characteristics during fault conditions.

New General Electric type *JE*-41 dry-type potential transformers, 13,800/-115-volt insulation, are installed one per generator for synchronizing and three per bus section (main and auxiliary) for metering and synchronizing.

Connecting the generator potential transformer from phase to ground retains the phase segregation principle in the structure and allows the transformer to operate at 8,000 volts. Theoretically a potential transformer connected to ground with the generator neutral open as an incoming machine might cause neutral shift and unbalanced voltages. This effect could be overcome by closing the neutral breaker by connecting the potential transformers to generator neutral instead of to ground, connecting it phase to phase, or installing two potential transformers phase to ground but these schemes have the objections of being more complicated, costing more, destroying phase segregation, or operating at 13,800 instead of 8,000 volts.

The generator manufacturer, however, feels that the above voltage distortions will not take place as there is a 90-ohm resistor in series with potential transformer. Star-connected bus potential transformers also work in better with the rest of the station relaying.

Each potential transformer is protected with a Schweitzer and Conrad combination 23-kv 90-ohm resistor and disconnecting-type boric-acid fuse. The fuse itself has sufficient rupturing capacity but the resistors aid in preventing the above-mentioned voltage distortion, they reduce the duty on the fuses, and they allow the bus fuses to blow in case of potential-transformer troubles instead of involving the high-voltage relaying.

Generator-Bus Relaying (Figure 7)

Each of the four generator-bus sections have phase and neutral differential protection using General Electric type *IAC*11*E*4 and *E*44 relays respectively. The bus tie breakers are overlapped. All the current transformers have the same ratio 3,000/5 or 1,500/2.5 depending on the circuit capacity. As the short-circuit current is only six or seven times the rating and as the transformers are specially designed and the relays are set above load currents there should be no troubles due to d-c components or transformer-ratio breakdown.

Due to the special design of current transformers, no serious leakage of current is expected through the paralleled secondaries of the current transformers.

The fault-bus system of protection was not selected because of the increased

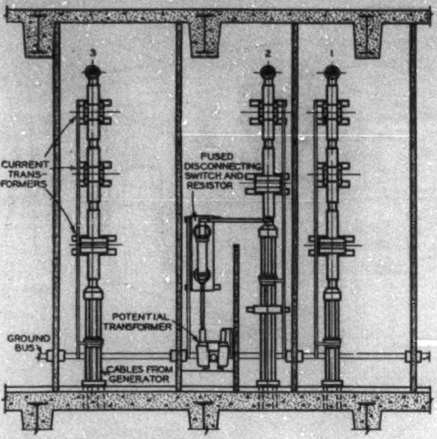


Figure 6. Current- and potential-transformer cells

cost and complication of installation, the possibility of stray fault currents, and the undesirability of not having equipment definitely grounded.

The reactor bus including reactors and leads are similarly differentially protected.

The group feeders have overload back-up protection with long time setting. Type *IAC* relays are used.

Each generator has differential protection which trips the bus breakers, neutral breaker, field switch, throttle trip, and CO_2 gas (last item for number 12 unit only).

Cables

The insulation selected was 23/64-inch varnished cambric (about 22-kv rating) with a copper shielding tape, 6/64-inch rubber jacket, and a fireproof-weatherproof braid. The present generator leads are not shielded but they have 23-kv insulation and a hose jacket. In order to reduce possible corona and to prevent electrical shock it was decided to shield the new cable. A grounded sheath will cause a positive relay operation on an insulation failure whereas an ungrounded sheath may allow a fault to simmer along until a serious disturbance occurs. The switch house is normally dry but the rubber jacket was added as a precaution against condensation or possible water during a fire.

A typical group feeder or generator has three conductors per phase and are arranged in three three-conductor duct banks. Here again the manufacturers and published data varied considerably in the recommended arrangement to cause least unbalance per phase. As the lengths are fairly short the unbalance problem is not serious.

Supporting clamps at the upper ends of the ducts were felt to be necessary because of the long vertical runs.

Thermocouples in cages have been drawn about 20 feet into the ducts with the leads for one group feeder and for two generators which are expected to have steady loads. It is uncertain as to whether these couples will read the conductor surface, air, duct-wall temperature, or combinations of these but we should get some interesting comparative temperature data on current, time lag, cyclic loading, effects of changing ambients, etc. The portion of the generator leads retained were not replaced by new cable as it was felt the cost was not justifiable.

The order was divided between General Electric, General Cable, Anaconda, and Okonite to get comparative life data. The conductors are stranded, concentric, or annular depending on the size.

The leads to the potential transformers are 500,000 circular mil which of course are not required for current-carrying capacities but are necessary to prevent overheating on a fault before the breakers have time to open.

Cell Wiring

Kerite and Okonite with 20/64-inch insulation, rubber tape, and fireproof-weatherproof braid were selected for the bus and cell wiring because of its flexibility, resistance to condensation and to water or fog of the fire protective system, and because of the shorter and simpler taps or joints allowable as the material is homogeneous. Shielding could not be used due to the short lengths of cables involved; however, it is unnecessary as the cable is mounted on full-voltage insulators and bushings.

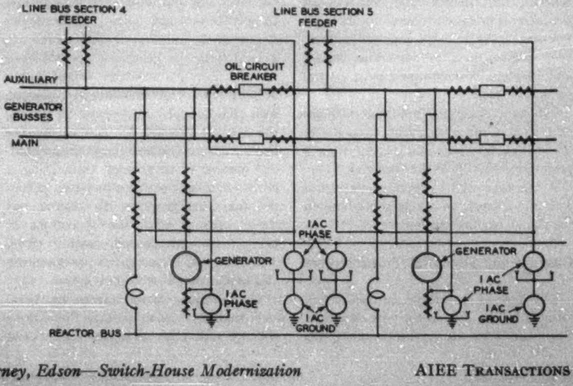
The control cable is the Anhydrex of the Simplex company. Solid conductors were used when possible to eliminate possibility of strands causing short circuits at terminal studs.

Control Conduit

The control circuits are segregated as follows: An individual conduit is run to each circuit breaker. Separate cables are run for the multiconductor breaker control and for the associated current transformers.

There are two control supply feeders.

Figure 7. Generator-bus differential protection



one for the main and one for the auxiliary breakers. Each of these supplies has four stud busses, one for each section.

There are no common pull boxes, terminal rooms, or wire trenches.

Structural Changes in Line-Bus Switch House

Along with the construction of the new switch house there has been a considerable modernization program progressing for the rest of the station.

Cinder-concrete barrier walls have been built between bus sections on all three floors. Bus-sectionalizing disconnecting switches have been installed on both sides of the barrier walls to permit bus sections being tied together in emergencies. This provision should be seldom used since the generator bus sections can be tied together by the reactor-bus or main- and auxiliary-bus tie breakers and as the group feeders between generating and corresponding line-bus sections have separate leads and breakers for the main and auxiliary connections. As the line-bus pairs of sectionalizing disconnecting switches are normally open there is no part of one live bus extending into an adjacent bus compartment which might be contaminated with ionized gases.

Transite and Micarta compartment doors and barrier walls were installed in the line-bus floor to enclose the bus structure and to separate the main and auxiliary busses and their tap connections.

The present ventilating system was changed to include an exhaust fan on the roof with manifolds and ducts leading to each bus-section compartment on the three floors.

Modernized Equipment

Many of the line type-*H* circuit breakers were modernized and increased in current-carrying or rupturing capacity by changing contacts, pots, baffles, tie rods, etc. The parts removed were in turn used to increase the capacity of still older breakers especially in the 6,900-volt section of the station.

There were many obsolete and dangerous current transformers and several line reactors replaced by new ones.

The oil-filled bus-potential transformers were replaced by new dry-type transformers connected in wye.

Incoming-line varnished-cambric cables in wall fiber conduits were rerouted or changed to have 25-kv insulation to stop corona cutting at duct entrances.

Overload line relays of obsolete type were replaced by type *CO*. Generator differential relays were replaced by type *CA*.

Five 14-kv switching equipments for station transformers and a motor generator were removed from the 6,900-volt switch house and distributed along the 14-kv line-bus sections.

In order to balance the loads on the four sections and to distribute parallel feeds to substations it was necessary to do considerable interchanging of lines and switching equipments. Certain reactor shifts were required to balance the loads on the parallel lines properly.

Line Bus Relaying (Figure 8)

The usual differential protective system is impractical for the line-bus sections due to the large number of circuits involved. The current transformers are not of a design to hold up their ratios. The sizes are small and there are so many different ratings that the balancing current transformers would be complicated. Ground differential would be impossible as there would be so many secondaries in parallel there would be nothing left to operate a relay.

Reverse-current relays could be in-

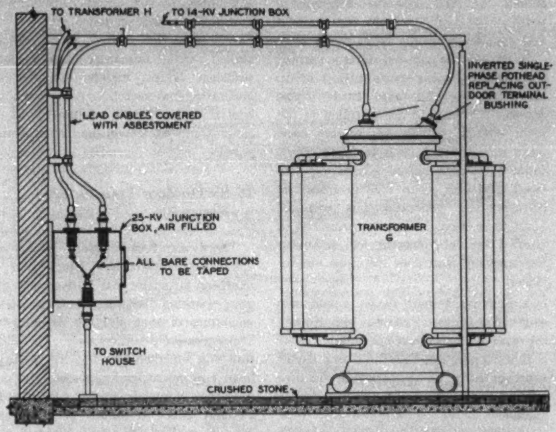
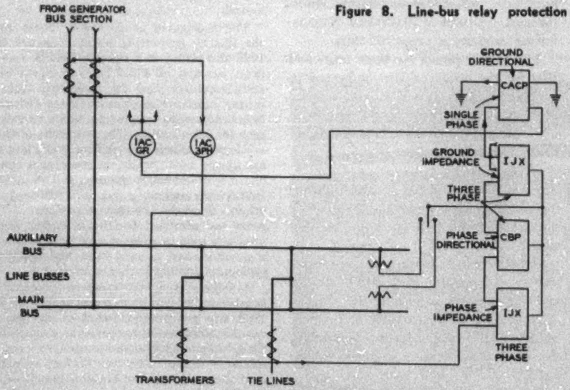

TO TRANSFORMER H
TO 14-KV JUNCTION BOX
INVERTED SINGLE-PHASE POTHEAD REPLACING OUTDOOR TERMINAL BUSHING
LEAD CABLES COVERED WITH ASBESTOMENT
25-KV JUNCTION BOX, AIR FILLED
ALL BARE CONNECTIONS TO BE TAPED
TRANSFORMER G
TO SWITCH HOUSE
CRUSHED STONE

Figure 9. Cable connections from junction box to transformer

Figure 8. Line-bus relay protection


FROM GENERATOR BUS SECTION
IAC GR
IAC 3PH
GROUND DIRECTIONAL
CACP
SINGLE PHASE
GROUND IMPEDANCE
IJX
THREE PHASE
PHASE DIRECTIONAL
CBP
PHASE IMPEDANCE
IJX
THREE PHASE
AUXILIARY BUS
LINE BUSSES
MAIN BUS
TRANSFORMERS
TIE LINES

stalled for each line to differentiate between line and bus faults but the cost and complexity would be excessive.

In the scheme adopted suitable current transformers having special ratio hold-up characteristics and large ratios (type *KC*-51*Y*—1,500/2.5) are installed in the main circuits having appreciable back feeds into the bus such as large transformers, ties to other generating stations, and the group feeders from the corresponding generator bus sections.

As these secondaries are connected in parallel the total current will represent the outgoing load over the lines not included.

A fault on a main feeder would also cause large operating current but tripping is prevented by a directional relay.

Bus tripping due to a fault on a feeder is prevented by an impedance or distance relay differentiating through the feeder reactors.

The various relay elements per double bus section are incorporated into a General Electric type *IJX*12*A*3 phase impedance relay having three instantaneous voltage-restrained current-operated elements each operating on line current and phase-to-phase voltage and one type *CBP*11*A*1 phase directional relay having three directional elements with voltage restraint using line currents and phase voltages, the tripping contacts of these two relays being in series. Similarly a type *IJX*12*A*4 ground impedance relay having three elements as above except using line currents with neutral voltages and one type *CACP*11*A* ground relay using residual line current biased by the current of the generator-neutral current transformers are in series to trip the bus multicontact relay.

As there are 30 to 40 breakers to be tripped per bus section special multicontact relays had to be developed. These 48-circuit relays consisted of a solenoid linked to two drum-type auxiliary switches each having 12 double stages. The relay is star-wheeled and counterbalanced in the open position and requires a positive application of current to cause its operation whereas the other available multicontact relays are spring operated and released by a solenoid. Any slight wear, continued vibration, sudden jar, or inaccurate adjustment might allow the relay to close which would be disastrous to a bus carrying load.

A manually operated multicontact switch is installed between the above relay and the circuit breakers to permit testing of the bus protection system.

As only one bus is normally operated there would not be much advantage in attempting to differentiate between main and auxiliary bus sections.

A four-section control bench was installed with miniature bus, control switches, lamps, meters, etc., for the generating-bus switching equipments except that the control equipments of the generators were left on the former control panels.

25-Kv Outdoor Transformers (Figure 9)

There are four outdoor 25,000-volt 7,500-kva transformers which had an overhead structure with bare copper, and gang-operated disconnects. Cleaning the insulators of soot and fog deposits has been excessively bothersome. This situation was remedied by removing the overhead equipment and replacing the outdoor-type porcelain bushings of the 14-kv and 25-kv terminals of the transformers with single-phase inverted potheads in the same cover openings. Single-conductor paper and lead-covered cables were connected to outdoor junction boxes having removable links for connecting the transformers in pairs. The common connections were then run to the 14-kv and 25-kv switch houses.

Transformer Fire Protection

A permanent fire protective system using Grinnel Mulsifier nozzles was installed for these transformers along with a fifth 25-kv transformer and three 25-kv load- and voltage-regulating transformers. A total of 39,000 kva of transformer capacity is protected thus.

The supply piping for these units and that for the header of the fog system in the switch house is taken to a concrete pit containing the various selector valves. The common manifold can be connected to the city water pressure, to the Boston Fire Department's pumper, or to drain. There is also a line run to two hydrants for portable applicators and to a water screen to protect three large wooden storage water tanks.

Dams were installed around these transformers and crushed stone laid to absorb and cool escaping or burning oil.

Three reinforced metal-lath-and-plaster barrier walls were installed separately, the 25-kv transformers to serve as fire walls.

The two General Electric Pyranol-filled 3,000-kva station-auxiliary power transformers were also equipped with a water protective system which can be used for cooling in case of fire in an adjacent transformer.

Discussion

P. H. Adams (Public Service Electric and Gas Company, Newark, N. J.): The Essex switch house followed the general practice in vogue in 1914 of large switchgear rooms in which the barriers formed by the cell work were the only protection against spread of damage. At that time generating capacity of most systems was small so that short circuits caused relatively little damage. The original plan for Essex was followed generally to the completion of the building in 1923.

In working out the design for the reconstruction described in Mr. Taylor's paper, the principles that we followed are:

1. Improved arrangement of equipment.
2. More definite separation of bus sections.
3. More frequent fire walls and smoke barriers, coupled with ventilation.
4. Limitation of capacity connected directly to each section.
5. Physical isolation of main circuit breakers.
6. Adequate relay protection for the bus sections.
7. Reduction of oil content of circuit breakers.
8. Segregation of control circuits.
9. Better fire-fighting equipment.

It is interesting to note that this question of protection against service interruptions caused by explosion or fire in switchgear houses also confronted the Electricity Commission in Great Britain and at the time the work at the Essex generating station was being started, an investigating board appointed by that commission recommended the adoption of principles very similar to the above for the use of the British engineers.

In particular reference to limitation of capacity in a bus room, they set 60,000 kw as a desirable maximum. This may seem too small for use in the United States, but the principle involved should not be overlooked.

The segregated-phase switch house for the Kearny generating station designed in 1924 and placed in service in 1925 is, I believe, the first in which the switchgear for each generator and its associated transformer bank are in separate rooms with no direct access to rooms containing switchgear for other units. The pole units of the oil circuit breakers on each floor in this house are in one room, while the associated conductors, disconnects, reactors, and all other connections and operating rods are in another. Separate compartments in that room are provided for the operating and interlock rods to prevent any possibility of a grounded rod coming loose and making accidental contact with a conductor.

A failure of a 3,000-ampere oil circuit breaker in one of these rooms in February, 1928, was not communicated to any other compartment even in the same room, although the oil ignited and burned for a considerable time. The ventilating system kept the smoke cleared away so that it was possible to remove the oil from the breaker and extinguish the fire.

It has been our experience that brick or concrete walls are effective fire or arc stops if adequate ground connections are provided for all equipment frames. The ground connections must have sufficient carrying capacity to function under any condition that may arise without fusing. Mr. Taylor points out in his paper that at Essex a complete ground network provides two paths to ground and at least two square inches cross section for all main equipment frames, while for lesser circuits the ground conductor is in most cases as large as the main conductor.

The general principles established at Kearny in 1924 were of considerable assistance to us working out the Essex plans. Improvements in oil-circuit-breaker design enabled us to use the type *H* breaker with its low oil content, thus having less than 300 gallons in the 16 main breakers at Essex, while 27 main breakers at Kearny contain nearly 8,500 gallons.

Essex has a separate room for each main circuit breaker, while Kearny with its unit construction has four single-pole breaker elements in a room.

While the work was progressing at Essex, the entire high-voltage yard at Kearny was rebuilt and differential protection was applied to the high-voltage bus sections, the 13-kv busses and connections, and the main auxiliary power busses.

Plans are now under way for modernizing the equipment at the Marion generating station, where each main circuit will be in a separate room and complete differential protection applied to the busses and connections. This will bring the three main stations of the company up to date as regards main equipment and service protection.

C. R. Reid (Shawinigan Water and Power Company, Montreal, Que., Canada): In reviewing these papers, I have been impressed by the unanimous agreement on the fundamental points under consideration. There were three points which impressed me most:

1. The attempt to improve relay protection and the difficulties with regard to differential features.

2. The necessity of improved circuit breakers and the evident desire to reduce the amount of oil.

3. The stress which has been placed on increased segregation of switching equipment.

In connection with faulty actions of differential relays on through short circuits, two thoughts come to mind. Have any of the authors had experience with the installation of a low-resistance high-reactance iron-core reactor across the coil of the differential relay, to by-pass the direct current? What is the approximate ratio between minimum current settings on the differential bus relays and the maximum through current on fault?

The remarks concerning circuit breakers have proved most interesting. During 1938 the company which I represent purchased, tested, and installed a 6,600-volt 2,000-ampere air-blast circuit breaker. The primary tests seemed to indicate that the circuit breaker in question had a rupturing capacity in excess of 900,000 kva at 12 kv. The opening time from energizing the trip coil to extinction of arc was less than 5 cycles on a 60-cycle base. It would appear that the ultimate rating of this equipment may exceed 1,000,000 kva with a clearing time of less than three cycles.

Considerable interest has been shown recently with regard to air circuit breakers. If this development proves successful, is it possible that the necessity of extensive segregation of switching equipment will be reduced? A consensus of opinion from the authors might prove of special interest.

M. H. Hobbs (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): It seems to me this is a fine series of papers on the important subject of plant modernization, showing that a great deal of study is being given to this reconstruction problem with the aim of improving service reliability and eliminating the possibility of major shutdowns. A few comments seem to be in order.

First, with reference to Messrs. Corney and Edson's paper the statement is made that a decision as to concrete structure versus steel was difficult to make although it was finally decided to use masonry construction. Some advantages of steel were brought out in the paper. In addition to these, steel has the advantage of lighter weight which is particularly important in modernization of existing stations where the floors and supporting walls already exist and may have to be used. The statement is also made that gang-operated switches in metal-clad switchgear are better isolated but that cell type offers better visibility and better segregation. This is not necessarily characteristic of metal enclosure, and complete visibility, as well as segregation of the bus from the switches may be provided in metal-enclosed gear. This is shown in figure 1 of this discussion.

With reference to interlocks, the opinion expressed that interlocks are not required in large stations having highly trained operators seems somewhat at variance with that of other operators. It would seem that in many cases the matter of interlocks is carried to an unnecessarily complicated degree, and that simpler interlocks between isolating switches and breaker, to prevent operating the switches unless the breaker is open, and to prevent access to the breaker compartment unless the switches are open, would be adequate. However, it does seem that such simple interlocks are desirable even in the larger stations.

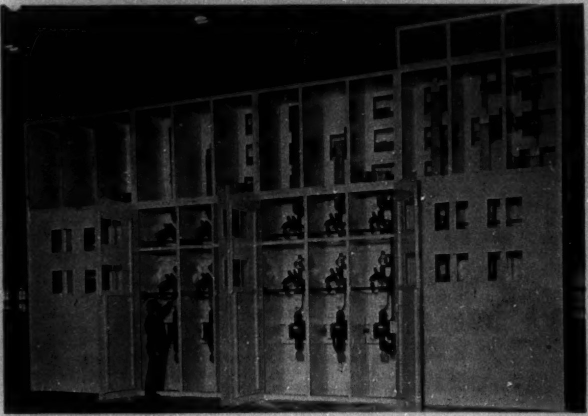
The disconnecting-switch design shown is somewhat unusual and apparently worked out quite well for this particular structure arrangement, where height was an important factor. A number of disconnecting switches have been made, however, using rear-connected studs even when fairly heavy current-carrying capacity was required, in fact, even in "L" Street, disconnecting switches for bus sectionalizing are of this type. However, the problem of expansion must be taken care of as indicated.

With reference to Mr. de Bellis' paper, the outstanding features seem to be bus sectionalization and segregation of the different sections, using metal enclosure throughout, even for breakers and instrument transformers. The operating experience in the case of a fault seems rather impressive.

Mr. Taylor's paper states that cubicles for the reactors were made of concrete so that there would be no undue heating in metal enclosures. It may be noted, however, that a number of installations have been made, using metal enclosures. The arrangement in Essex differs quite materially from that described by Mr. de Bellis, in the fact that metal is not used for the enclosure although every effort is made to carry the ground bus throughout the structure to insure satisfactory operation of protective relays. The statement is made that with the type of indoor breaker used, the forces incident to the closing operation are taken by post insulators rather than bushings. Tests in the high-power laboratory indicate that there is a possibility of fracture of the porcelains under heavy short-circuit current and we make every effort to use material other than porcelain for this application.

In all the papers the importance of the protective scheme, particularly bus differential protection, has been stressed, and the matter is undoubtedly receiving much more

Figure 1



attention than ever before. Metal enclosures are conceded to be of much assistance in providing quick clearing of faults, and limiting troubles which occur to grounds rather than phase-to-phase failures. Even where masonry is used, the effort seems to be made to approach metal enclosure by carrying ground busses to each piece of apparatus and insulator base. The tendency now seems to be definitely away from the segregation of phases by relatively large distances, and group phase construction, with a maximum sectionalization, would appear to be the more favored practice.

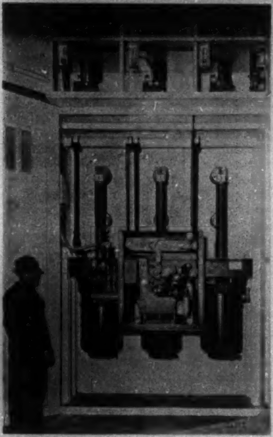


Figure 2

Figure 1 of this discussion shows metal-enclosed switchgear for a section of the main bus before installation in West End generating station of the Cincinnati Gas and Electric Company. This shows quite completely the accessibility of the disconnecting switches and their complete isolation from the bus and circuit breakers which are located at the rear. All disconnecting switches for a particular breaker operate from a single mechanism interlocked with the breaker and the doors to the breaker compartment.

Figure 2 shows a breaker at the rear of the structure, with its mechanism, and the connections to the rear-connected disconnecting switches and bushings. Each phase is metal isolated from the others and potential transformers are located in compartments above the breaker cells, each transformer being in its own compartment and connected to the bus or circuit by means of fuses and disconnecting switches.

A somewhat similar construction has been used in the modernization of the Millers Ford generating station of Dayton Power and Light, except that the busses are located on different floors from the breaker compartments, somewhat similar to the arrangement in the New York stations described by Mr. de Bellis. On the first floor are reactors in metal-enclosed compartments and oil-circuit-breaker cubicles. On the second floor are located the two main busses, together with risers going to the generator breakers on the first floor and feeder breakers on the third floor.

For both the Cincinnati and Dayton installations the fault-bus scheme of protection has been used. Before construction work had been completed a fault occurred in one of the Dayton bus sections which the fault-bus relay cleared successfully. In connection with metal enclosures, a good deal of consideration is being given to this scheme of protection, and where the bus arrangement is not too complicated the protection scheme is very simple. It is believed that further consideration of this method of protection is warranted in the future.

J. W. Lingary (Lynn Gas and Electric Company, Lynn, Mass.): The four papers presented before this committee are indeed indicative of the strenuous efforts and thought that is being vigorously prosecuted to safeguard against traps causing power-system failures which not only interfere with continuous flow of power for the requirements and established routine of the public, but which often result in major loss of property and possibly injury and even death to employees.

It is not my intent to discuss in any detail any one of the presented papers. On the contrary I wish to confine my remarks as a sort of synthesis of personal comment where from personal experience, observation, and study of reports on several major power-system emergencies which have occurred around the country now and then during the past few years, I hope my remarks may be of some benefit. It is needless for me to say that all companies are not cognizant that there exists somewhere on their systems some form of trap which may cause power-system failure of diverse extent. I believe there are. There is no company regardless of size, whose people delegated with responsibility for operation of the power system do not spend their time on oft occasions in worry because they have come to recognize the existence of such traps. By a trap, I mean as some people prefer to call it a "bottle neck." Most companies have them regardless of size and it requires a vigorous, intelligent engineering prosecution to smoke many of them out into the open before trouble and disaster follow.

Most power and manufacturing companies employ highly skilled and competent engineers. In reality most of them are great individual specialists in their respective fields, whose efforts are especially concentrated to the many various components which make up a power system, but in spite of this traps do persist, whether avoidable or unavoidable either in the layout of the company's system or perhaps manufacturer's apparatus. However, when one realizes the vast intricacies that make up a power system, which in general consist of a great many delicate and intricate relays, exposures of transmission and distribution systems to the wrath of natural elements, failure of insulating mediums, the use of high steam pressures and temperatures, and the motion at high speeds of huge bodies of metals, one cannot help but wonder that trouble does not occur more frequently than it does.

We cannot do more than admire the vigorous efforts of engineers today as proved by the papers presented. Their every effort it seems is to prevent, isolate, minimize, and correctly with high-speed dispatch automatically diagnose troubles and expediently clear them by use of correct application of relays and other apparatus. All this must be so, in order to control the follow of an outage either as an unimportant event or at best a very minimum.

In prosecution of these efforts no one should question the wisdom of responsible engineers when they propose justification at times of enormous capital investment. I do not by all means here mean that economy should be forgotten or even slighted. However, I do wish to convey that it should not be overstressed at the sacrifice of system reliability because reliability to some extent is economy of no small importance.

Perhaps there exists nothing short of a dynamite factory where things may happen with such sudden destruction and rapidity as they do on a power system, particularly in the make up of a generating station where there are a great many things which may cause sudden emergencies and even serious outages. It may be only a small control wire or perhaps some small insignificant relay costing a few dollars, which of necessity performs an important function, but at the same time its failure to function properly is capable of shutting down a boiler costing a million dollars. Therefore, we must consider in the design of a power plant a great many small and large complexes. We cannot afford, for example, to load up a boiler house with pipes, boilers, and other apparatus and offer what insignificant space there may be left to install the all and most important station service electrical equipment. The installation of this equipment should have as much effort and thought put forth as to its design, location, and reliability as the major switch house, busses, and switching apparatus. Control wiring should be chosen with great care, and should not be exposed to high temperature, dampness, and other deterrents. Important control terminal centers, delicate relays, and equipment of this sort should be housed in the cleanest and most desirable places that can possibly be found. I believe that no important major station cables should be of the lead-covered type with their incident potheads and vulnerability to break down. In general, I believe these cables should be mounted in suitable raceways, insulated and mounted on insulators designed for operation at least at full voltage. Important station transformers should be segregated as much as possible away from buildings and one another and protected with suitable rapid fire-fighting equipment. Transformer-oil drain valves should be remotely controlled so that in event of fire, oil may be withdrawn quickly if necessary. Station ventilating systems should have a reliable source of power consisting of main and auxiliary, because in event of explosion, if the use of ventilating fans is lost a considerable length of time elapses before men are able to enter and work. I cannot speak too highly of the efforts made in connection with providing suitable fire-fighting equipment. It has been my experience that the use of

CO_2 automatically controlled is most desirable. I have known of cases following a major explosion where CO_2 was immediately injected and there was little or no evidence of deposit of smoke or volatilized metals throughout the switch house. I believe the smoke and gases were so rapidly and thoroughly cooled and emulsified with CO_2 which I believe prevented their spread and concentrated deposits. Based on this experience, I would recommend highly automatic CO_2 installations for all segregated switch rooms and other similar important locations particularly if inflammable materials are present. Every effort should also be made to design bus and switching compartments against the entrance of rodents. Many a bad spillover has been experienced from this one cause alone. The interior of switch rooms must never be painted except with proved fireproof paint. I have known of a very bad shutdown from this cause where the wall paint caught fire following a serious explosion and continued to burn for some time. Skylights over turbogenerators, switchboards, and other important station equipment are extremely bad in event of severe storms. Similarly no important station equipment should be installed in basements or various pits where it is subject to inundation from flood waters or even from bursting or badly leaking water mains. Coal storage, handling, and conveyor systems including docks especially should be protected against fires and coal-dust explosions. Oil storage systems whether for lubricating or insulating purposes should be held to a minimum indoors and especially protected against fires. Oil piping should not be in close proximity to hot steam pipes; if so they should be adequately protected against fire as result of oil leaks. Standardization and maintenance of adequate and essential spare parts are absolutely necessary in any power plant.

We must not in our efforts when considering the physical arrangements of a power plant forget for one moment its personnel, whether it be a switchboard operator in the control room or an operator at the turbine throttle, or an operator in the fire aisle of the boiler house, because they are exceedingly important persons, probably not often recognized as such. In general they must and should be highly intelligent and skilled individuals. I have known it to happen where due to some form of mental lapse or for some psychological reasons they may become individually the biggest and most dangerous trap of the power system, because during an emergency they may emotionally or otherwise fail to carry through the proper operating function. Great care must therefore be used in the choice and caliber of these men for their respective positions. They must be properly instructed, they must never be instilled with a feeling of inferior complex by supervisors or others, they must not be overstressed that severe punishment will follow their mistakes. Every effort should be made to ascertain that these men have no detrimental habits, home or financial worries which may deter or otherwise interfere in the correct and vigilant execution of their duties.

We must remember that perhaps two emergencies are probably never exactly alike. There may be long intervals between emergencies; howsoever the case may be, operators must and are expected to be alert to carry the responsibility of their function. Therefore, it seems reasonable that frequent drills and instructions are absolutely necessary, mimicking all possible emergencies which might probably occur.

In closing I might say we learn a great deal from the other fellow's sad experience. We may well copy the highly efficient efforts of insurance companies who in general investigate and study every accident of their clients, whether it be to an employee or property. They then correctly classify it and make every strenuous effort to eliminate all such possibilities of repetition throughout their clientele. Therefore, I believe it is quite in order that we should have a committee of specialized engineers who would investigate and study and report in classified detail all happenings incident to major power system difficulties for the benefit of all of us. We then may be able to proceed more intelligently in scrutinizing our own power system for near like or similar pit falls.

A. P. Fugill (The Detroit Edison Company, Detroit, Mich.): I would like to discuss particularly the two papers presented by A. M. de Bellis and D. W. Taylor.

These papers indicate rather definitely certain major trends in design for stations where large amounts of energy are concentrated. Briefly these trends are: automatic bus protection with resultant electrical sectionalization; elimination of oil-filled apparatus, or at least reduction in the amount of oil used; and localization of trouble by physical segregation of equipment and adequate fire-fighting equipment. It is not necessary to cover these factors in detail as they already have been discussed adequately by others.

It is rather evident that there is general agreement among switch-house designers on these general principles but considerable difference of opinion exists as to the extent to which these principles should be applied in any modernization program and how the desired results can best be accomplished. Although the question of economics is present, it is modified by the indefinite factors of the probability of the occurrence of trouble and the effect of a serious disturbance on public good will. It is usually proper to spend considerably more money than could be justified by equipment damage or direct loss of revenue, because of the possible effects of such a service interruption on the company's relations with its customers. Because of the indefiniteness of this factor, there is a tendency, especially on the part of those who have experienced such a major disturbance, to adopt designs which are rather expensive, so as to obtain an extra factor of safety. Since there is no positive criterion, the judgment of the designer and his associates must be relied upon to strike the right balance.

Although several features of station design described in Mr. de Bellis' paper merit comment, I will confine myself to a short discussion of the bus and bus housing. Without doubt, the design used is unique and, as was pointed out in the paper, has several advantages over the more conventional self-supporting, rectangular metal housing with metal baffles between phases and with the bus mounted on one or, at most, two insulators. The installation is undoubtedly easier because of the smaller pieces to be handled but it appears that the over-all cost of the bus and housing would be higher. In the design described in the paper, there are the heavy supporting rings which should be of nonmagnetic material and the four insulators per support with the proper provision for initial adjustment and movement of the bus due to expansion. The enclosing covers are made of expensive, nonmagnetic materials, and the structure for supporting the three assemblies, though of structural steel, is quite heavy. It would be interesting to know whether Mr. de Bellis has any cost comparisons on the different types of bus and housing.

The use of four insulators for each point of support gives an exceedingly strong bus for forces in all four directions. In determining the required mechanical strength of a bus, the only force which is important is that due to the electromagnetic effect of the fault current. This force will be in the direction of a line through the three busses and not at right angles, except for a minor effect where the bus changes direction. Two of the insulators used in this design, therefore, are of little use. There may be some justification for two insulators so that one insulator will always be in compression. However, we have carefully designed all our heavy duty busses in recent years to insure mechanical sufficiency and have not found a single case where more than one insulator was necessary, with spans of four or five feet and initial symmetrical root-mean-square currents up to 50,000 amperes, even though, in some cases, the insulators were stressed in cantilever.

There is a good reason for keeping the number of insulators to a minimum since the insulator is generally the weak point in the installation electrically. With proper clearances, a flashover to ground usually occurs over an insulator because the distance to ground is less at this point, the surface of the insulator may become dirty, or the porcelain may break. We believe that the fewer insulators in a bus run, the less the chance of trouble, assuming, of course, sufficient mechanical strength.

Mr. de Bellis states that the bus is insulated for 23 kv throughout its length which we assume means through the insulator heads as well, or the benefit would be largely nullified. The purpose in insulating a bus is to reduce the chance of flashover in case conducting gases should leak into the housing. We have felt that with a reasonably tight metal housing, there is little chance for conducting gases to enter the enclosure in sufficient quantities to cause flashover even with a bare bus. The use of insulating material, on the other hand, in case trouble does occur, introduces the additional hazard of smoke and even explosive gases from the combustion of the insulating materials. We wonder what Mr. de Bellis thinks of the possibility of sufficient internal pressure being developed to blow off the housing covers with consequent hazard to personnel and damage to equipment.

In his paper on modernizing switching facilities for Essex switching station, Mr. Taylor states that masonry enclosures for equipment were used, perhaps because some of the present cells and bus housings could be utilized. I would like to ask Mr. Taylor whether he feels that such construction would be adopted for a new station of similar character, or whether he would then use metal enclosures with building walls to provide fire barriers.

Mr. Taylor mentions the ground network in the station and states that equipment is grounded to this network. Does that mean that each insulator base is thus grounded? Are there any reinforcing rods in the masonry housings and, if so are they grounded or is there some provision for keeping ground current out of these rods? Is the ground network tied to building steel or building reinforcing rods in any way? The answers to these questions have a vital bearing on the very important problem of the potential hazard resulting from undirected fault currents flowing in a switching station or power plant. Recent experiences have indicated that considerable damage can result from ground fault currents flowing in control conduits, reinforcing rods, and other paths of reasonably low impedance but limited cross section. Wherever equipment housings are in contact with the building structure, the possibility exists that fault current will flow in the wrong places to reach the ultimate ground mass. However, the danger of trouble is reduced to a minimum if a low impedance path of adequate current-carrying capacity is furnished to the ground mass from every point to which a flashover can occur. One of the advantages of metal housings over masonry housings is that a flashover must occur to the housing before it can reach any part of the building. It is then comparatively easy adequately to ground the metal housing. In a masonry bus structure, especially if the insulator bases are not grounded, fault current may get into the reinforcing rods and thence into the building where control conduit and reinforcing rods are usually buried. Adequate grounding of such a bus structure is difficult to accomplish. In designing the ground connections, it should be remembered that reactance is just as important as resistance in obtaining low impedance, so that merely furnishing a large cross section of copper is not sufficient. Proper location of grounding conductors and parallel paths for the ground current are necessary to minimize the reactance of the path to ground. We have felt that the steel framework of the usual station furnishes a very good ground network, and, in general, connect our copper grid ground network to the building steel at frequent intervals.

E. S. Fields (The Cincinnati Gas and Electric Company, Cincinnati, Ohio): In the modernization of the switch houses covered by this group of papers, considerable emphasis has been placed on the isolation of bus sections by gas-tight barrier walls, and in most every case there has been included a comprehensive ventilating system to facilitate clearing of smoke and fumes in the event of fire. Such ventilating systems are usually of high capacity and in order to get them to function promptly in the event of fire some of the systems have been made to start automatically.

It has been our observation that most of the severe switch-house and substation fires have had considerable pressure associated with them and that the results of the fire have been most severe in cases where the equipment involved was confined in a small space. Based on this observation, it has been our most recent practice in Cincinnati to isolate groups of switching and other equipment by barrier walls and install ventilating systems much in the same fashion as described in this group of papers. At the same time, however, we attempt to have oil-filled equipment facing at least one outside building wall with loose panels or hinged doors or windows in the wall that will blow open to the outside in the event of extra pressure caused by fire or explosion. It is believed that this means of relieving the pressure to prevent spread of smoke, fire, and gases is an effective one and worth consideration in new design and in modernization programs where it is practical to apply it.

R. L. Frisby (Kansas City Power and Light Company, Kansas City, Mo.): The four papers presented on the subject of reconstruction and modernization of switch houses and switching equipment have covered about every phase of the problem in an excellent manner. The subject is of interest to all operating men and operating companies, more especially to those concerned with operating systems that were built about 20 years ago.

The activity at this particular time on reconstructing and modernizing was undoubtedly brought about not only by the expansion of these systems, but due to the fact that several very serious outages and complete or partial shutdowns of systems have occurred in the past three or four years. To prevent a reoccurrence of similar accidents it is very evident from the papers presented that a great deal of time, study, and money have been spent in modernizing the several systems described.

There is, however, another side to the question which may be given some consideration. We are well aware of the fact that a vast improvement has been made in oil circuit breakers, and from records and data collected I am informed that no oil fires have resulted in the past two years caused by the failure of any modern breaker or modernized breaker installed within the past five years. If this is true, can we justify the expenditure necessary completely to rebuild or reconstruct our bus structures and switch houses in order that fireproof walls may be built to segregate groups of busses and switching equipment? True, our old bus structures may not lend themselves as we should like and desire to the installation of additional current transformers necessary for the relaying and segregation of bus sections or groups; however, several relaying schemes are available and I believe it is possible for the engineer to use some one of these schemes on his existing structures and system, provided he can make up his mind which of these relaying schemes will best fit his layout and give the protection desired, without adding too great a hazard due to faults in the operation of the relaying set-up.

Granting that it is possible to modernize our circuit breakers and install a relaying scheme which will give us reasonable protection and segregation of busses, then if we go through our switch houses with a fine-tooth comb and correct the weak spots, mechanically as well as electrically, install fire-fighting equipment, and last but not least, not overlook the house auxiliary system, which may be an equally or an even greater hazard than the main bus layouts, so far as causing prolonged outages are concerned, are we not justified in taking some gamble on an outage which may or may not occur once in 20 years?

Carl M. Gilt (Consolidated Edison Company of New York, Inc., New York, N. Y.): One of the outstanding trends in the modernizing of the older switch galleries as well as the construction of new galleries is the more extensive use of sectionalization and segregation, both electrical and physical. The necessity for reducing the magnitudes of short circuits, because of limitations in circuit breaker interrupting capacities, the difficulty in withstanding the magnetic forces and thermal destruction of enormous currents, has been only one factor in this trend. Were this the only factor, the results could be secured by a judicious use of reactors and breakers without physical separation.

However, experience with failures on station busses of large capacity and voltages of 11,000 and higher demonstrates that a failure may not be confined to its point of origin, even with fast-operating breakers. In the older stations of smaller capacity and particularly with voltages of 6,600 or less, arcs frequently went out with the drop in voltage even though no automatic protection was provided for the busses or generators, and the damage was more or less local. A failure on a large-capacity bus is so explosive in nature and the ionized gases spread with such rapidity that ordinary breaker compartments and doors and bus compartments offer no guarantee against a cascading of failures. Smoke from burning insulation such as tape or oil, will rapidly deposit soot on insulators, frequently with resulting flashovers.

The trend, therefore, has been to separate physically one bus section from another to prevent the spread of damage. Bus sections must be relatively small so that two adjacent busses may be lost simultaneously without too seriously crippling the supply to the load, for the bus failure may very well involve or originate in a bus tie breaker. Double breakers, or a section of bus normally dead with adequate fire walls, are very instrumental in reducing the likelihood of a failure involving adjacent sections. Fire walls between important sections should be complete without being weakened by a door that might be blown off its hinges. Double doors are an improvement, but still better are solid walls with no direct passage between sections, openings being through fire doors into a corridor or intermediate room.

Ventilation must be adequate and sectionalized to insure that smoke can be rapidly scavanged and will not spread from one section to another. Also the sectionalizing of control and conduits must be carefully followed as a high-voltage failure readily may result in destructive currents and potentials on control conduits and cables. Adequate drainage of high capacity must be provided to prevent the spread of such currents and nullify the whole protective system.

Perhaps most important of all is the keeping of a watchful eye for what may appear to be some unimportant detail that may jeopardize a fine one-line diagram or fundamental floor plan. Pieces of cable which look like a feeder but are really part of a bus have a queer way of getting into the wrong room. Bus sections well separated by breakers, disconnects, and barriers on one side of a wall may have only a thin piece of asbestos lumber between them on the other side of the wall. Control conduits may be well separated and protected for their whole run and then come together in a pull box with only a thin sheet metal between them and some circuit breaker. Some ventilating duct may be air-tight as long as it is there but when it melts down a fine chimney is provided from one section to another. Such things occur in building a new station. They are more apt to happen in modernizing an old station where existing space limitations result in odd contortions, but how frequently they sneak into an old station when one is trying to put 100,000 kw where 25,000 kw belong and three extra feeders must be got out of a station already full. And, of course, with one eye always on the dollar sign, we find that the other eye, perhaps too conveniently, does not always see all that it should.

H. H. Rudd (Railway and Industrial Engineering Company, Greensburg, Pa.): My remarks apply to that part of Mr. de Bellis' paper which relates to the bus and disconnecting switches.

When my company as manufacturers knew that the Consolidated Edison Company wanted to modernize and install equipment of a very rugged character, we had no preconceived notions of how big or how strong such equipment should be or how it should be designed. The specifications given us were rigid—far more so than any we had ever encountered. Among the requirements were:

1. A low temperature rise for bus and switches.
2. The bus to be insulated with wrapped varnish cambric insulation and, in addition, to have insulators in compression holding the bus at four points.
3. The strength of the supports to be 10,000 pounds in any direction at right angles to the center line of the conductors.
4. Each phase conductor to be enclosed in its own walled metal housing with air space between the housings isolating one phase from the other.
5. The housings to be tight to prevent dirt and powdered coal dust from getting in on the porcelain and insulated conductors, so as to reduce the outage time for inspection and cleaning.
6. Baffles to be placed in the bus runs to seal off between the bus and housing in much the same way as fire walls are used in the building to seal off one part of the station from another.
7. A ground bus to be run so that every bus frame and sheet of housing be substantially grounded.
8. Switch clips and blades to be held in a similar way to that of the bus by four insulators in compression.

Our designs in general followed these requirements as set by the Consolidated Edison Company to meet their particular conditions. In making design studies we found that the form of bus and housing tended toward some specific patterns, which might be interesting.

1. Because of the rigid requirements for wrapped insulation, the form of conductor chosen was a round tube. The splices, expansion joints, ells, tees, and crosses were all of a circular cross section for ease in applying the varnished cambric insulation.
2. The bus support frame was made in the form of a ring. This gave a construction of maximum stability to support the insulators and obtain the required strength, no matter in which direction the stress might be. This round frame also permitted a machining to give a smooth surface against which the housing plates are bolted and also a machined groove in which gaskets are placed to make the joint tight. The frames are cast bronze.
3. Mounting feet cast integral with these rings provide the means for fastening to wall, ceiling, or supporting framework. Any short-circuit stresses are carried directly to the supporting members and are not carried through any part of the housing sheets.
4. The housing in two semicylindrical halves is clamped down bridging the space between two adjacent frames and thus forming the enclosure. Gaskets are provided to seal the space between the two halves of the housing. There are no louvers in the frame or housing. The rounded form of the housing makes a very effective surface in getting rid of the losses by convection as it is streamlined compared to a flat rectangular construction. The rounded form also minimizes the sheath or housing losses.
5. For straight bus runs the housing is made of manganese steel which is nonmagnetic. For the ells, tees, etc., where it is necessary to cut the sheets in specific forms for welding into the special shapes required, the manganese steel is too hard and, therefore, a silicon bronze such as Olympic bronze or Everdur is used which can be cut easily and welded to the desired form.
6. The simple units of structure, bus support frames, conductor, and housing permit an easy method of erection and quick assembly at low cost.

In the preceding remarks I have attempted briefly to show the nature of the design which was used to meet the rigid requirements of the Consolidated Edison Company. For equal requirements it is less expensive as judged by previously accepted standards which do not incorporate the features that the Consolidated Edison Company now has. I think it is only fair to state that the Consolidated Edison Company's needs and experience since the first installation seem to justify such rugged construction and such high factors of safety.

We have, I am glad to state, found that where requirements are less severe, a similar construction can be made which, though not so rugged, embodies a number of the advantages of the Edison construction.

With less severe short-circuit stresses, lighter frames and lighter insulators can be used. If the requirement against dirt is not severe, then it is possible to omit gasketing and the machining for gasketing and still provide a tightness comparable with conventional concrete cell and door work, or comparable with flat overlapping sheet work. The plates are thinner but they still act as an effective housing.

If there are no requirements for covering the bus with an insulating material, then open forms of conductors can be used such as flat bar bus, or channel and angles, or ventilated square tubing, and advantage taken of the better ventilation of the conductor and more simple methods of making splices, etc.

The Consolidated Edison Company uses a ground bus. The same form of supports and housing can be used for a system using a fault bus. Insulation readily can be placed under the feet of the bus support frames.

The general form of construction supplied to the Consolidated Edison Company is flexible enough in design to give the ruggedness and factors of safety that it required and, at the same time, it has features that can be incorporated to advantage in bus work where less severe conditions are encountered.

F. W. Gay (Public Service Electric and Gas Company, Newark, N. J.): The method of bus construction described by Mr. de Bellis has been worked out with great care and we believe should find a place of extensive usefulness in switch-house design. While metal bus structures have certain desirable features, the insulating advantages of a properly constructed concrete bus should not be overlooked. The insulating value of concrete in bus and cell structure has been known for a generation. A recent failure at Essex generating station on a new bus led to the making of tests to determine the value of concrete as an insulator in such structures when new and when seasoned. It is believed that all the material available on the insulating value of concrete could well be collected and presented to the Institute in a paper.

Two feeder busses at Essex generating station which had been installed for over ten years were scheduled to be demolished. The doors were taken off, leaving the cell structure open, and windows were left open for several days during drizzling weather to allow moist air to blow through the busses. These bus structures were made of well-washed sand and gravel and fresh cement. Each bus support comprised a porcelain insulator, bronze top, and malleable iron base bolted to a cast iron insert cast in a concrete slab, in turn built into the masonry bus structure.

The busses were excited to 8,000 volts through a transformer connected to the 440-volt auxiliary power bus, and the 440-volt breaker was set to trip at 1,000 amperes. One end of a flexible cable was attached to the insert and the bus support insulator was short-circuited by touching the other end to the hot bus.

After a number of supports were tested, it was found that the arc was very feeble (not over one-half inch long) where the bus support insert had an insulation to ground in excess of 200,000 ohms and thereafter all tests were confined to the lower-resistance inserts. The maximum length of arc on any insert at 8,000 volts was three-quarters of an inch and there was no evidence of any increase in length when it was held on for approximately 15 minutes.

It was decided to test the lowest-resistance insert to destruction at 15,000 volts instead of 8,000, as obviously very little advantage would be obtained by using normal voltage, and much time would be wasted. The insert tested was in the bottom row, mounted in a slab cast directly on the floor. After the voltage had been on for approximately five minutes, the arc which had started with a purple streak approximately one inch long had lengthened to $1^1/_2$ inches. At the end of $8^1/_2$ minutes the arc had lengthened making it possible to draw it down across the insulator so that it held across the porcelain to the base, and from then on it was allowed to play across the insulator. At the end of $10^1/_2$ minutes, the current was sufficient to trip the circuit breaker and it was found that the concrete around the cast-iron insert was smoking hot and had cracked and was slightly spalled. After the slab was removed from the floor, the floor was not in any way damaged.

Tests to determine the distribution of voltage around the cast-iron insert showed that the major part of the voltage drop between insert and ground was confined to a region of not more than one inch from the insert.

It is proposed to remodel the Marion generating station in the near future and the insulation resistance through the masonry structure to ground has been taken for the concrete inserts under the bases of every bus support in the bus structures of this station. Ninety-seven-and-one-half per cent of these inserts showed an insulation resistance to the station ground grid in excess of 100,000 ohms and 75 per cent in excess of 1,000,000 ohms. The lowest resistance measured was 20,000 ohms and the highest 20,000,000 ohms.

H. L. Wallau (Cleveland Electric Illuminating Company, Cleveland, Ohio): Modernization of electrical switch houses is an effort to improve reliability at a minimum of cost, using existing buildings and equipment unchanged to a maximum degree compatible with the end in view. Such changes as are made generally involve reductions in maintenance costs as well.

Because of the limitations which confront the designer under these conditions, limitations much more onerous than those met with when designing absolutely new facilities, the problems encountered are much more difficult of solution.

In general the following criteria must be realized to the greatest degree possible:

1. A type of construction which will minimize the possibility of faults developing which are internal to the structures.
2. Fast relaying to initiate the clearing of such faults as may occur, in minimum time.
3. Limiting the fault currents which will pass through the breakers to values within their interrupting abilities.
4. Physical isolation of equipment individually or in groups to confine such damage as may occur to restricted areas.
5. Electrical isolation to prevent a fault in one section from involving another section through damage to a single sectionalizing device.
6. Suitable means of ventilation, preferably automatic, to clear any area in trouble of fumes and gases in minimum time, to allow quick safe access to the area.
7. Suitable means of trapping or disposing of oil which may issue from damaged apparatus coupled with means of fighting the electrical fires which may occur.

In addition there should be,

8. Proper correlation of the supply capacity to the load capacity of each section, preferably so that the loss of one of several sources of supply when provided to a given section will not curtail its output.
9. Proper correlation of loads connected to the various bus sections so as not to cause a partial loss of system load in the event a bus section is lost.

How well these criteria have been met in the modernization plans for the four stations under discussion is obvious. The design engineers are to be congratulated on the manner in which they attacked the problems, and on the unique metal-clad bus construction which they developed.

H. R. Summerhayes (General Electric Company, Schenectady, N. Y.): All of the modernizing layouts presented in the papers recognize the importance of physical and electrical segregation of bus sections, normal ventilation and special ventilation for scavenging the products of combustion, means for preventing the passage of incandescent gases from one section to another, and modern fire-fighting equipment.

In some of those installations it appears to me that ground-fault protection for the bus sections might have been used to advantage.

Tradition and custom have had a certain influence on the station designers preference for the older and better established differential protection; nevertheless, in the ten years or so since the ground-fault protection scheme was introduced, it has been adopted to a considerable extent, as shown by the report on "Bus Protection" by the relay subcommittee of the AIEE protective devices committee (AIEE TRANSACTIONS, volume 58, 1939, pages 206–11, May section).

This committee gives 197 installations of the fault-bus protection reported by 11 companies as against 803 installations of the full differential protection, or nearly 20 per cent of the total. Also reported are 132 partial differential relay schemes using distance relays, etc.

The difficulties and complications encountered in applying full differential protection are described in a paper by R. M. Smith, W. K. Sonnemann, and G. B. Dodds (AIEE TRANSACTIONS, volume 58, 1939, pages 243–9, June section).

In new structures where metal enclosure for the bus bars and switchgear can be used the installation of ground-fault bus protection is comparatively easy, and would appear to be cheaper and far simpler than the full differential protection.

In new structures utilizing concrete or masonry enclosures the designer can draw on past experience for the precautions necessary to prevent diversion of the ground current from the fault bus at other paths in the structure. These precautions should be taken anyway whether ground fault or differential protection is used, as admitted in the Corney-Edson paper. Also in either case there must be furnished a substantial ground bus of low impedance and adequate current-carrying capacity, to which are connected the bases of the bus insulators, circuit breakers, disconnects, and other points to be grounded.

Therefore, the expense of these measures should not be charged against the fault-bus protection, in comparing it with differential, and taking this into consideration the fault bus, with only one current transformer per bus section appears simpler and cheaper than the full differential scheme, and offers advantages in saving of space and elimination of many secondary connections.

The ground-fault protection practically eliminates phase-to-phase faults, especially where fast switching and relaying are used, since a phase-to-phase fault cannot take place without involving ground.

Ground-fault protection is also well adapted to outdoor switchgear installations, as proved by the experience of a number of companies using it.

The severity of faults, the amount of destruction, spread of metal vapors, etc., should be reduced by the ground-fault scheme, since the current in the fault is limited by the neutral grounding resistor.

Incidentally, the severity of bus faults in new layouts, whether differential or ground-fault protection be used, should be reduced by the high operating speed of the circuit breakers and relays used in modern switchgear.

Neutral grounding plans must be studied carefully as to the adequacy of all equipment in the neutral both as to insulation and current-carrying capacity. The reduction of the amount or elimination of oil in circuit breakers should not give a sense of false security, since there may still be oil in other equipment, and there will usually be other combustible insulation, so that fire extinguishing and ventilation will still be necessary.

John G. Noest (Brooklyn Edison Company, Inc., Brooklyn, N. Y.): In two of the four papers presented, and in their subsequent discussion, it was admitted that difficulties were encountered in the economic justification of the expenditures involved in the modernization programs. In these cases engineering considerations prompted the outlay of capital for improvement in reliability, improvement in expected continuity of service, and to minimize damage to equipment in case of severe disturbances. In short, these expenditures were made to prevent plant shutdown, or to keep a service interruption which might occur, to a minimum of time.

There are in general three divisions into which plant shutdowns may be classed. First: failure of equipment due to insufficient capacity; second: failure of equipment to perform as designed; and third: improper operation of equipment.

The first and third classes are a matter of design. The first class includes all cases, where due to increase of capacity on a system, or due to installation of system ties the short-circuit duty on breakers and busses exceeds their interrupting or carrying-capacity respectively. This condition, as well as the segregation of equipment to limit damage, is a function of design and the modernizations carried out are remedial measures for it.

The third class is also a matter of design. Improper operations of equipment occur not as the result of intention, but as the results of error. The probability of errors made by operating personnel is not a function of the salary classification nor of the length of service of an operator. Improper operations just happen. However, means are available to the plant designer, in the forms of interlocking systems, to enforce proper operating procedure in most cases. If extensive modernization of equipment can be justified it seems that the comparatively small additional cost of interlocking devices should be easier to justify.

It is perhaps the second class to which most of the past plant shutdowns may be charged. Failure of equipment to operate as designed is a function of maintenance. As an example: a circuit breaker, designed for an interrupting capacity of 1,000,000 kva may be incapable of interrupting 100,000 kva if its opening stroke has been slowed down by excessive friction due to corrosion or lack of lubrication. The circuit breaker may not open at all on account of a small cotter pin being sheared off, on account of a small bolt broken off, or on account of a little rust in a strategic place. The most inclusive differential relay protective scheme may be put out of commission, unknown to anyone, by a short circuit in the current transformer, or its secondary wiring. Such conditions may never be discovered and will not be found out in a post-mortem after the fire department is through with the plant. When an insulator flashes over, something has happened to its insulating strength before it flashed over. After a flashover the evidence is usually destroyed. If only a small percentage of the thinking done by the engineers responsible for design of equipment or the engineers responsible for the plant layout was spent in preventive maintenance the history of power-plant failures would surely lack many a page.

It seems that along with the modernization of equipment, modernization of maintenance methods are in order. And not only is modernization of maintenance methods in order but the establishment of closer ties along which experience and information may flow from the maintenance organization through the engineering organization to the manufacturer and back. Modernization of maintenance procedure is not an alternative to modernization of equipment but a corequisite. Its cost is generally small.

H. A. P. Langstaff (West Penn Power Company, Pittsburgh, Pa.): The program of modernization described by A. M. de Bellis of the Consolidated Edison Company of New York is so thorough, complete, and general in its scope as to make it difficult to formulate specific comments. We have witnessed the manufacture and assembly of the metal-enclosed bus equipment, such as is being used at Waterside Number 2 station as pictured in figures 6 and 7. It is so strongly constructed mechanically, electrically and thermally that it is difficult to see how there can be any service failure resulting from it or from any external influence other than complete structural wreck. We have also seen during manufacture and assembly a sort of junior class of this metal-enclosed bus equipment, wherein by using aluminum enclosures the weight is considerably reduced with bus insulation values retained at the same high level. The enclosure is not mechanically as strong as that which Mr. de Bellis describes, but it appears fully satisfactory for general application. Tribute should be paid to the engineers of the Consolidated Edison Company for their courage and ability in developing this outstanding new type of power station bus.

In the paragraph giving the seventh step of those considered most important effectively to confine damages for equipment failures, Mr. de Bellis makes reference to the power supply system for station auxiliaries and for generator excitation. I wish to emphasize that this step should be given equal weight with other steps in attaining the results desired and that it should not be regarded as an auxiliary or secondary step. Careful analysis should be given and rigid inspection made of the station auxiliary systems lest a bottleneck occur which might obviate the apparent foolproof construction of a major power station facility. Emergency transfer equipment may be provided in many places and if so care should be taken that the transfer equipment itself does not bring to a vulnerable focal point the two supply sources. A failure in the transfer equipment can very well cause both power sources to become inoperative. The same care should be given to the minor electrical system as is given to the major electrical system.

Proper segregation must be carefully maintained of control supplies and circuits to adjacent sections of main power busses. Interconnection of current transformers located on different sections of the main bus should be made with due care that faults occurring on one section of bus cannot be carried to another through the secondary wires of the interconnected current transformers. Vigilance must be exercised to ascertain where bottlenecks are located and to prevent them from developing through hurry or falsely economical engineering practices.

T. W. Trice (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): At a time when switch-house modernization is receiving particular attention, the paper by H. E. Strang and W. M. Hanna summarizing the means which have been used, or are available to carry out a modernization program is of special interest. There are, however, certain items that logically might be included along with those covered by the authors.

The problem of isolation of bus sections and equipment usually results in the construction of compartments to contain this equipment. Where this type of construction is required, which is usually the case with rebuilt switch houses, some attention should be given to the pressures that may be built up in the event of an explosion. Our experience with switch-house explosions back in 1927, where confinement was not particularly great, revealed that considerable pressures can be built up with rather severe effects, and we believe should be given due recognition in any switch-house design.

Where compartments are used care should be taken to see that an operator cannot be trapped if a failure takes place while he is in the compartment. A means of mitigating this would be to have exit openings at either end of the compartment, one of which may be used only as an emergency exit.

Where isolation of sections and equipment is produced by compartment walls some form of forced ventilation frequently is required to scavenge smoke and gases out of the compartment subsequent to a failure in that compartment. This ventilation equipment should be of sufficient capacity to rapidly dilute smoke or gas but, most important, it should not be destroyed by the original failure and the source of power should be such as to be independent of any switch-house failures, thereby assuring its readiness for service when needed.

The question of fire protection should also be given attention in any switch-house modernization program. This attention should be directed not only toward having the necessary fire-fighting equipment available, but assure that it is possible to combat any fire without danger from toxic gases or live electrical equipment. In this connection it appears that water in the form of a spray has the greatest all-around possibilities and if we could look into the future we might see the weatherproof type of equipment used on indoor installations with water-spraying facilities permanently installed.

In the fore part of the paper, several factors were listed as being included in reliability of operation. It would appear that this list might be extended to include "ability to restore service in the event of an interruption."

There are included six causes, any one or more of which may be the basis of a modernization program. It does not appear that reliability of service is given sufficient weight, for in the final analysis the primary object of any modernization is to improve reliability of service. This improvement may be secured by carrying out one or more of the items listed, these methods being only a means to an end.

The use of photoelectric tubes has been mentioned as having possibilities for bus protection. In addition to lack of experience another disadvantage of this type of protection is that it presupposes the existence of an arc. Incorrect switching of ground disconnects or failure to remove grounding devices may result in a solid ground fault. To insure against this the photoelectric-tube installation would have to be backed up with another type of bus protection which could just as well be designed to handle the trouble initially.

D. H. Johnston, Jr., and E. F. Wolf (Consolidated Gas Electric Light and Power Company of Baltimore, Md.): The paper by D. W. Taylor presents a rather complete résumé of reconstruction details, which should be of real interest to the many concerned with the modernization of generating-station and switching facilities. Reference to a permanently piped-foam system on four 3,750-kva station-auxiliary power transformers at Essex prompts a note of warning on the use of such systems.

Tests were recently conducted by the Consolidated Gas Electric Light and Power Company of Baltimore to ascertain the relative effectiveness of fire-extinguishing equipment for transformers. The results of these tests conclusively demonstrate that the use of piped-foam and piped-carbon-dioxide systems, unless properly applied, increases rather than reduces the fire intensity. It is essential that the injection of foam or gas shall produce no agitation of the oil either by velocity contact or by deflection from projecting steel bracing in the tank or under the cover. In several of the tests the introduction of these extinguishing agents converted a comparatively small fire into one of major proportions with flame and spraying oil reaching an estimated height of 80 feet.

A report is being prepared on these tests, which will be publicized in the near future.

A. M. de Bellis: The discussions reflect a great deal of interest in plant modernization.

Similar points having been raised by

various discussers, a general reply seems to be in order, supplemented by some answers to specific questions.

All the discussers agree in principle as to the necessity of some form of station modernization in order to safeguard against major shutdowns.

The question has been raised, however, of the necessity of extensive segregation of the switching equipment, provided circuit breakers of a modern design and some method of relay protection were used.

It is difficult, of course, to reply very specifically to this general query. There are probably no two cases of station design which lend themselves to exactly the same treatment and I think that for each individual case, the answer to how far to go in station sectionalization and reconstruction must be left to the judgment of the engineers charged with the responsibility of protecting against service interruptions and of justifying capital investments. I would like to point out, however, that operating experience is responsible to a great extent for the present trend toward extensive sectionalization of the electrical equipment in a switch gallery.

During the last 15 years approximately 25 major station shutdowns have occurred in this country.

Approximately half of this number were attributed to causes other than circuit-breaker trouble and as a matter of fact practically all of the most spectacular station shutdowns were due to causes other than operating failures of circuit breakers.

Modern oil circuit breakers have indeed a very good operating record and the trend toward the reduction or the elimination of oil in circuit breakers and the installation of bus differential relays are steps in the right direction.

Yet, I think that it would be rash to assert of any modern type of electrical equipment, be it a circuit breaker, a relay, or just a bus, that it represents the last word and that no precautions, such as sectionalization, need be taken to protect against its failure when such a failure might have disastrous results.

In reply to Mr. Reid's questions relative to differential-relay operation, we have no experience with the installation of relay-shunting devices as a means of by-passing the direct current on through short circuits.

The relay pickup for through faults varies from 10 per cent to 15 per cent of the maximum through-fault current.

I agree with Mr. Field's suggestion of the desirability of providing, whenever possible, some means of relieving the pressure resulting from equipment failures.

The questions raised by Mr. Fugill regarding the relative merits of various types of bus construction are interesting and very much to the point.

Ordinarily, I think it would be logical to expect to have to pay a higher price for a type of equipment which, as Mr. Fugill agrees, offers several advantages over the more conventional type of metal-enclosed equipment.

In the case of Waterside and Sherman Creek, however, comparative estimates based on actual construction costs and on manufacturers' quotations indicated a lower over-all cost for the type of bus construction used as compared to the cost of conventional types of masonry construction or metal-enclosed equipment.

Probably the most important factors affecting the over-all cost of a bus structure are the amount and type of labor required.

The bus structure developed for our stations consists of a simplified type of design which reduces to a minimum such labor requirements in both the manufacture as well as the installation of the equipment.

I agree with Mr. Fugill's reasoning for keeping the number of insulators to a minimum, particularly if Mr. Fugill refers to the conventional type of insulator design.

Mr. Fugill's statement regarding the usefulness of two of the insulators used at each point of support in our bus structures is not quite correct.

I think it is generally recognized that mechanical forces of great magnitude may be caused during short circuits by mechanical oscillations set up in the busses and bus supports which are mechanically clamped to the busses and which are subject to cantilever stresses.

These cantilever stresses may be caused not only by electromagnetic forces acting on busses or at bus taps and bends but also by electromagnetic forces caused by line to ground faults or by the longitudinal tension set up along the busses and stressing the insulators in a direction at right angles to that of the electromagnetic forces.

One of our major considerations in the development of the bus structure was to obtain a type of bus support which would not be subjected to cantilever stresses.

No heads being used with the porcelain insulators, it is obvious that either three or four insulators are required at each point of support to hold the busses in place.

Porcelain insulators of simple and standardized design and of very low cost compared to that of the conventional type of insulator are used.

This arrangement, I think, removes guesswork from the design and application of bus supports and by working the porcelain the way it should be worked, that is in compression, not only increases the safety factor of the entire bus structure but results in a more rational and more economical use of this material.

Mr. Fugill's assumption that the bus insulation would be carried through the insulator heads is, of course, correct. In our case, however, as I have already stated, no insulator heads are used.

As regards the degree of safety of this type of metal-enclosed bus, I would refer Mr. Fugill to that part of my paper which describes the operating experience in the case of a fault during the early stages of the Waterside installation.

I may add that this type of bus design was chosen primarily because, in our opinion, it offers marked advantages as regards service reliability and safety to personnel.

We share the opinion of Mr. Hobbs and Mr. Summerhayes that further consideration of the fault-bus scheme of protection, particularly in connection with metal-enclosed equipment, is warranted.

We fully agree with Mr. Langstaff and other discussers that the supply systems for station auxiliaries, generator excitation, and controls are of paramount importance and that in plant modernization programs they should not be regarded merely as an auxiliary or secondary step.

D. W. Taylor: In regard to Mr. Reid's first question, we have had no experience with the operation of low-resistance high-reactance iron-core reactors connected across the coils of differential relays to by-pass the direct current. As to his inquiry concerning the ratio between the minimum current setting on the differential bus relays and the maximum through fault current, we have used ratio differential relays of the *CA* type, probably for the reason that prompted Mr. Reid's question. The minimum current setting varies with the load or through current and our bus relays are set for 40 per cent unbalance.

Commenting on the masonry reactor cubicles, Mr. Hobbs stated that metal enclosures have been used in many installations. The elimination of the heating problems was not the only reason for the adaption of the masonry cubicles, for in addition they were more economical than metal enclosures, both as to first cost and maintenance.

The masonry cell and bus structures, in answer to Mr. Fugill's inquiry, are in general without any reinforcing rods. In a few large horizontal slabs, rods are necessary, and in these cases, the rods are well separated from each other, run in one direction only, and have several inches of concrete between outside steel or other structures. Building steel and building reinforcing is grounded at numerous points, conduit is grounded at its termination in the cell structure, and the conduit banks tied together and grounded to the building steel at frequent intervals. Oil circuit breakers and instrument transformers are heavily grounded; the bases of all disconnecting switches are grounded; the bases of lead and bus supports are not grounded. However, approximately 75 per cent of the leads consist of fully insulated tubing on porcelain supports, and this includes leads in the instrument-transformer and disconnecting-switch compartments. The horizontal busses are bare copper, in separate compartments free of any apparatus. Mr. Gay has spoken of the insulating value of properly constructed concrete. The value of this insulation was shown in the last few years when at Marion generating station two electrocuted rats and one cat were found in the bus structure, none causing a fault. As rodents are almost always present in stations, the insulated bus structure is beneficial in this respect. In regard to stray fault currents, a heavy interconnected grid or station counterpoise, that joins all apparatus, conduit, and building steel to all station neutrals, so that no great potential gradients are obtained under fault conditions, is of importance. Where troubles have been experienced in the past, we believe the difficulty can be traced to a lack of such an interconnecting grid. If we were constructing a new station of similar character to Essex, we would probably use outdoor switching; however, should indoor structures be necessary, our years of satisfactory service with the insulated type of masonry structure, particularly as a fire barrier, or a second line of defense, would probably cause the adaption of this type of structure. We would probably use completely insulated bus.

Mr. Frisby points out that with the improved reliability of modern oil circuit breakers, the extensive rebuilding of structures and erection of fire barriers may not be justified. In the case of the Essex generating station, the main breakers could not be rebuilt for sufficient interrupting capacity; neither could they be replaced, breaker for breaker, due to insufficient space. In order to install modern breakers of the proper interrupting capacity, the removal of large portions of the existing bus structure would have been necessary, with the accompanying rerouting and reconnecting of leads, which in itself amounted to an extensive rebuilding job, but to limit this to just providing space for the larger breakers would have resulted in excess footage of bus and leads, and close clearances and crowded arrangements in many portions of the station. In addition, as mentioned by Mr. Noest, relay schemes do not always function, and breakers may jam or small parts break. Also, burning insulation on cables or leads may cover the insulators with soot and smoke, and fumes fill the switch galleries, so that even with breakers of adequate interrupting capacity and modern reliability, it was thought that a second line of defense, in the form of segregation and barrier walls, was essential.

H. E. Strang and **W. M. Hanna:** Mr. Reid has asked questions regarding bus differential relays which because of variation in conditions cannot be answered in general terms. It is necessary that the settings must be below the minimum secondary current for a short circuit in the protected area considering both the current available and the errors in the current transformers. The setting must also be above the error in the current transformer carrying the maximum current to a fault outside the areas. This usually means using exceptionally good current transformers, delaying the relaying until the transient producing errors in the current transformers is over, or blocking the relays temporarily if excessive error exists. His suggestion of a high-reactance low-resistance coil to shunt the d-c component is interesting because of the well-known fact that this component cannot be accurately reproduced in the secondary circuit. The real trouble caused by this component is the saturation produced by it in the current-transformer core, which produces errors in the ratio of the a-c component. Saturation also introduces harmonics in the secondary current and a relay is available which shunts these harmonics through a restraining coil which acts to prevent relay operation due to current-transformer saturation. The failure of the current transformer to reproduce accurately the d-c component may be of less concern with this type of relay.

Mr. Gay's tests on bus insulators mounted on concrete are very interesting and we believe show that the probability of getting a short circuit to ground through foreign objects such as rats is reduced, by not grounding the base of the insulator. However, his tests did show progressively increasing arcing after an arc had started. Any arc started by a transient overvoltage condition therefore would probably hang on and increase. It seems to us then that it is well worth while to ground the insulator bases to improve relaying and provide a positive path to ground so the short-circuit currents will not wander in devious and destructive ways on its road to ground.

Mr. Trice's comment that reliability of service should have been given more consideration is important. In fact, it was the intent of the authors that all other points covered by the paper were to attain this end.

Mr. Trice also points out that if no arc exists, photoelectric protection will not work, which is true. He points out that this type of fault can occur if an operator forgets to open the ground switch after working on the bus. We believe that an operator in energizing a bus section after a shutdown will do so with his eyes on his instruments and will trip the breaker if trouble is indicated. As a fault not involving an arc is not so damaging as an arcing fault the decay due to manual instead of automatic clearing should not be serious in this case.

Values of the Bessel Functions ber x and bei x and Their Derivatives

H. B. DWIGHT
FELLOW AIEE

THE FUNCTIONS ber x and bei x and their derivatives are frequently used in electrical-engineering problems connected with heavy conductors and with wires at radio frequencies. For instance, the current distribution and the resistance loss in a round conductor, or in a coreless induction furnace, are often computed by means of these functions.

One of the most widely used tables of values is that published by A. G. Webster.[1] This covers the range of $x = 0$ to 10, at intervals of 0.1. The polar forms of these quantities were given in the "Report of the British Association for the Advancement of Science," 1923, page 293, by A. E. Kennelly and P. L. Alger.[2]

In the present paper there are given tables for $x = 0$ to 20, at intervals of 0.1 to five significant figures.

When more than five significant figures are desired, see references 1 to 4. For a table at intervals 0.01 of x, for $x = 0$ to 6, with four significant figures, see reference 5.

The new values for the tables in the present paper were computed by students at Massachusetts Institute of Technology, who were engaged in work under the National Youth Administration during the past three years. Those who did a considerable amount of work in this project include H. A. Burr, D. R. Erb, D. Gleason, L. A. King, J. J. Novak, A. Rosenfeld, and H. Schaevitz.

Precautions against errors have been taken by checking the work thoroughly and also by using differences. The computations were carried out to more places than have been tabulated. The changes in bei′ x for $x = 6.5$ to 10 given by H. G. Savidge[6] have been included, with the resulting values of the polar forms. Note that in reference 1, bei′ 3.7 should be 0.131 486 760 and bei 8.9 should be −28.002 867 538.

Since one of the results most frequently required in this type of work is the effective resistance of a round wire, it may be noted that this has been very completely tabulated in reference 7.

It is desired to acknowledge valuable suggestions from Doctor L. J. Comrie, recently secretary of the committee on calculation of mathematical tables of the British Association for the Advancement of Science.

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Paper number 39-9, recommended by the AIEE committee on basic sciences, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted June 27, 1938; made available for preprinting November 29, 1938.

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Table I

x	ber x	bei x	ber' x	bei' x	x	ber x	bei x	ber' x	bei' x
0	1.00000	0	0	0	8.0	20.974	−35.017	38.311	−7.6603
.1	1.00000	.002500	−.0000625	.050000	8.1	24.957	−35.667	41.353	−5.2855
.2	.99998	.010000	−.0005000	.099999	8.2	29.245	−36.061	44.415	−2.5296
.3	.99987	.022500	−.0016875	.14999	8.3	33.840	−36.159	47.472	.63410
.4	.99960	.039998	−.004000	.19997	8.4	38.738	−35.920	50.492	4.2318
.5	.99902	.062493	−.007812	.24992	8.5	43.936	−35.298	53.442	8.2895
.6	.99798	.089980	−.013498	.29980	8.6	49.423	−34.246	56.281	12.832
.7	.99625	.12245	−.021433	.34956	8.7	55.187	−32.714	58.967	17.883
.8	.99360	.15989	−.031989	.39915	8.8	61.210	−30.651	61.451	23.465
.9	.98975	.20227	−.045537	.44846	8.9	67.469	−28.003	63.682	29.598
1.0	.98438	.24957	−.062446	.49740	9.0	73.936	−24.713	65.601	36.299
1.1	.97714	.30173	−.083082	.54581	9.1	80.576	−20.724	67.145	43.583
1.2	.96763	.35870	−.10781	.59352	9.2	87.350	−15.976	68.246	51.460
1.3	.95543	.42041	−.13697	.64034	9.3	94.208	−10.412	68.831	59.936
1.4	.94008	.48673	−.17093	.68601	9.4	101.10	− 3.9693	68.821	69.012
1.5	.92107	.55756	−.21001	.73025	9.5	107.95	3.4106	68.132	78.684
1.6	.89789	.63273	−.25454	.77274	9.6	114.70	11.787	66.674	88.940
1.7	.86997	.71204	−.30484	.81310	9.7	121.26	21.218	64.353	99.763
1.8	.83672	.79526	−.36118	.85093	9.8	127.54	31.758	61.070	111.12
1.9	.79752	.88212	−.42384	.88574	9.9	133.43	43.459	56.720	122.99
2.0	.75173	.97229	−.49307	.91701	10.0	138.84	56.370	51.195	135.31
2.1	.69869	1.0654	−.56906	.94418	10.1	143.63	70.534	44.384	148.03
2.2	.63769	1.1610	−.65200	.96661	10.2	147.67	85.987	36.171	161.08
2.3	.56805	1.2585	−.74202	.98361	10.3	150.81	102.76	26.438	174.38
2.4	.48905	1.3575	−.83920	.99443	10.4	152.90	120.87	15.066	187.82
2.5	.39997	1.4572	−.94358	.99827	10.5	153.77	140.32	1.9344	201.30
2.6	.30009	1.5569	−1.0551	.99426	10.6	153.23	161.12	−13.076	214.69
2.7	.18871	1.6557	−1.1738	.98149	10.7	151.09	183.25	−30.083	227.85
2.8	.065112	1.7529	−1.2903	.95897	10.8	147.14	206.68	−49.202	240.59
2.9	−.071368	1.8472	−1.4314	.92566	10.9	141.17	231.35	−70.544	252.75
3.0	−.22138	1.9376	−1.5698	.88048	11.0	132.95	257.21	−94.212	264.12
3.1	−.38553	2.0228	−1.7141	.82230	11.1	122.25	284.14	−120.30	274.46
3.2	−.56438	2.1016	−1.8636	.74992	11.2	108.81	312.06	−148.90	283.54
3.3	−.75841	2.1723	−2.0177	.66214	11.3	92.383	340.80	−180.08	291.07
3.4	−.96804	2.2334	−2.1755	.55769	11.4	72.707	370.21	−213.89	296.76
3.5	−1.1936	2.2832	−2.3361	.43530	11.5	49.517	400.08	−250.37	300.29
3.6	−1.4353	2.3199	−2.4983	.29366	11.6	22.543	430.18	−289.55	301.32
3.7	−1.6933	2.3413	−2.6608	.13149	11.7	−8.4832	460.25	−331.41	299.48
3.8	−1.9674	2.3454	−2.8222	−.052527	11.8	−43.828	489.97	−375.92	294.37
3.9	−2.2576	2.3300	−2.9807	−.25965	11.9	−83.753	519.00	−423.01	285.58
4.0	−2.5634	2.2927	−3.1347	−.49114	12.0	−128.51	546.95	−472.57	272.67
4.1	−2.8843	2.2309	−3.2818	−.74817	12.1	−178.34	573.38	−524.46	255.18
4.2	−3.2195	2.1422	−3.4200	−1.0319	12.2	−233.48	597.82	−578.51	232.62
4.3	−3.5679	2.0236	−3.5465	−1.3433	12.3	−294.11	619.72	−634.46	204.50
4.4	−3.9283	1.8726	−3.6588	−1.6833	12.4	−360.42	638.51	−692.03	170.30
4.5	−4.2991	1.6860	−3.7537	−2.0526	12.5	−432.56	653.56	−750.87	129.49
4.6	−4.6784	1.4610	−3.8280	−2.4520	12.6	−510.62	664.17	−810.58	81.534
4.7	−5.0639	1.1946	−3.8782	−2.8818	12.7	−594.69	669.61	−870.67	25.889
4.8	−5.4531	.88366	−3.9006	−3.3422	12.8	−684.75	669.07	−930.59	−37.992
4.9	−5.8429	.52515	−3.8911	−3.8331	12.9	−780.78	661.72	−989.72	−110.65
5.0	−6.2301	.11603	−3.8453	−4.3541	13.0	−882.65	646.64	−1047.3	−192.61
5.1	−6.6107	−.34666	−3.7589	−4.9046	13.1	−990.17	622.87	−1102.7	−284.38
5.2	−6.9803	−.86584	−3.6270	−5.4835	13.2	−1103.1	589.42	−1154.8	−386.45
5.3	−7.3344	−1.4443	−3.4445	−6.0892	13.3	−1221.0	545.22	−1202.7	−499.28
5.4	−7.6674	−2.0845	−3.2064	−6.7199	13.4	−1343.4	489.19	−1245.3	−623.27
5.5	−7.9736	−2.7890	−2.9070	−7.3729	13.5	−1469.8	420.18	−1281.5	−758.77
5.6	−8.2466	−3.5597	−2.5410	−8.0454	13.6	−1599.5	337.04	−1309.9	−906.08
5.7	−8.4794	−4.3986	−2.1024	−8.7336	13.7	−1731.5	238.57	−1329.2	−1065.4
5.8	−8.6644	−5.3068	−1.5855	−9.4333	13.8	−1865.0	123.55	−1337.7	−1236.9
5.9	−8.7937	−6.2854	− .98438	−10.139	13.9	−1998.7	−9.210	−1333.9	−1420.5
6.0	−8.8583	−7.3347	−.29308	−10.846	14.0	−2131.3	−160.94	−1316.1	−1616.1
6.1	−8.8491	−8.4545	.49429	−11.547	14.1	−2261.3	−332.82	−1282.3	−1823.5
6.2	−8.7561	−9.6437	1.3835	−12.235	14.2	−2387.1	−526.02	−1230.7	−2042.3
6.3	−8.5688	−10.901	2.3802	−12.901	14.3	−2506.8	−741.65	−1159.1	−2272.0
6.4	−8.2762	−12.223	3.4899	−13.536	14.4	−2618.2	−980.75	−1065.4	−2511.6
6.5	−7.8669	−13.607	4.7174	−14.129	14.5	−2719.1	−1244.3	−947.37	−2760.4
6.6	−7.3287	−15.047	6.0675	−14.670	14.6	−2806.8	−1533.1	−802.69	−3016.9
6.7	−6.6492	−16.538	7.5442	−15.146	14.7	−2878.6	−1847.9	−628.96	−3279.9
6.8	−5.8155	−18.074	9.1510	−15.543	14.8	−2931.6	−2189.2	−423.74	−3547.4
6.9	−4.8146	−19.644	10.891	−15.847	14.9	−2962.3	−2557.4	−184.56	−3817.5
7.0	−3.6329	−21.239	12.765	−16.041	15.0	−2967.3	−2952.7	91.056	−4087.8
7.1	−2.2571	−22.848	14.774	−16.109	15.1	−2942.8	−3374.9	405.59	−4355.5
7.2	−.67370	−24.456	16.918	−16.033	15.2	−2884.8	−3823.6	761.47	−4617.5
7.3	1.1308	−26.049	19.194	−15.792	15.3	−2789.0	−4298.1	1161.1	−4870.4
7.4	3.1695	−27.609	21.600	−15.367	15.4	−2651.0	−4797.3	1606.6	−5110.3
7.5	5.4550	−29.116	24.130	−14.736	15.5	−2466.1	−5310.6	2100.3	−5332.7
7.6	7.9994	−30.548	26.777	−13.875	15.6	−2229.3	−5863.1	2644.1	−5533.0
7.7	10.814	−31.882	29.532	−12.763	15.7	−1935.5	−6425.3	3239.6	−5705.8
7.8	13.909	−33.092	32.382	−11.373	15.8	−1579.6	−7003.1	3888.3	−5845.4
7.9	17.293	−34.147	35.314	−9.6806	15.9	−1156.1	−7593.0	4591.3	−5945.6

Table I—Continued

x	ber x	bei x	ber′ x	bei′ x
16.0	−659.50	− 8190.7	5349.3	−5999.5
16.1	− 84.364	− 8791.2	6162.6	−6000.0
16.2	574.85	− 9388.7	7030.8	−5939.2
16.3	1323.6	− 9976.7	7953.1	−5808.8
16.4	2167.2	−10548	8927.9	−5600.2
16.5	3110.8	−11094	9952.9	−5303.9
16.6	4159.4	−11605	11025	−4910.4
16.7	5317.2	−12072	12140	−4409.5
16.8	6588.5	−12483	13292	−3790.6
16.9	7976.7	−12826	14476	−3043.0
17.0	9484.5	−13087	15683	−2155.5
17.1	11114	−13252	16905	−1116.9
17.2	12866	−13305	18132	84.10
17.3	14740	−13230	19351	1459.0
17.4	16735	−13007	20550	3018.9
17.5	18849	−12619	21711	4774.9
17.6	21076	−12045	22819	6737.5
17.7	23410	−11265	23854	8916.7
17.8	25843	−10255	24795	11322
17.9	28365	−8992.4	25618	13961
18.0	30962	−7454.3	26298	16841
18.1	33619	−5616.0	26807	19968
18.2	36317	−3452.4	27115	23345
18.3	39034	−938.55	27189	26975
18.4	41746	1950.9	26993	30857
18.5	44423	5241.1	26492	34987
18.6	47033	8956.4	25645	39360
18.7	49539	13121	24411	43967
18.8	51901	17757	22745	48793
18.9	54072	22886	20602	53821
19.0	56003	28527	17934	59029
19.1	57640	34697	14691	64390
19.2	58921	41409	10823	69871
19.3	59782	48674	6279.0	75433
19.4	60152	56497	1006.0	81030
19.5	59957	64879	−5048.2	86609
19.6	59115	73816	−11935	92111
19.7	57540	83297	− 19706	97468
19.8	55143	93303	−28411	102600
19.9	51826	1038 10	−38095	107430
20.0	47489	114780	−48803	111860

Table II

$$\text{ber}\, x + i\, \text{bei}\, x = J_0(x i^{3/2}) = M(\cos\theta + i \sin\theta)$$

x	M	θ Degrees
0	1.0000	0
.1	1.0000	.143
.2	1.0000	.573
.3	1.0001	1.289
.4	1.0004	2.291
.5	1.0010	3.579
.6	1.0020	5.152
.7	1.0037	7.007
.8	1.0064	9.141
.9	1.0102	11.550
1.0	1.0155	14.226
1.1	1.0227	17.160
1.2	1.0320	20.340
1.3	1.0438	23.750
1.4	1.0586	27.373
1.5	1.0767	31.188
1.6	1.0984	35.172
1.7	1.1242	39.299
1.8	1.1544	43.545
1.9	1.1892	47.883
2.0	1.2290	52.290
2.1	1.2741	56.743
2.2	1.3246	61.221
2.3	1.3808	65.708
2.4	1.4429	70.188
2.5	1.5111	74.651
2.6	1.5855	79.090
2.7	1.6665	83.498
2.8	1.7541	87.873
2.9	1.8486	92.213
3.0	1.9502	96.518
3.1	2.0593	100.791
3.2	2.1760	105.032
3.3	2.3009	109.245
3.4	2.4342	113.433
3.5	2.5764	117.599
3.6	2.7280	121.745
3.7	2.8894	125.875
3.8	3.0613	129.991
3.9	3.2443	134.096
4.0	3.4391	138.191
4.1	3.6464	142.279
4.2	3.8670	146.361
4.3	4.1018	150.439
4.4	4.3518	154.514
4.5	4.6179	158.586
4.6	4.9012	162.657
4.7	5.2029	166.726
4.8	5.5242	170.795
4.9	5.8665	174.864
5.0	6.2312	178.933
5.1	6.6197	183.002
5.2	7.0338	187.071
5.3	7.4752	191.140
5.4	7.9457	195.209
5.5	8.4473	199.279
5.6	8.9821	203.348
5.7	9.5523	207.418
5.8	10.160	211.487
5.9	10.809	215.556
6.0	11.501	219.625
6.1	12.239	223.694
6.2	13.026	227.762
6.3	13.865	231.830
6.4	14.761	235.898
6.5	15.717	239.965
6.6	16.737	244.031
6.7	17.825	248.098
6.8	18.986	252.163
6.9	20.225	256.229
7.0	21.548	260.294
7.1	22.959	264.358
7.2	24.466	268.422
7.3	26.074	272.486
7.4	27.790	276.549
7.5	29.622	280.612
7.6	31.578	284.674
7.7	33.666	288.736
7.8	35.896	292.798
7.9	38.276	296.859
8.0	40.818	300.920
8.1	43.531	304.981
8.2	46.429	309.042
8.3	49.524	313.102
8.4	52.829	317.162
8.5	56.359	321.222
8.6	60.128	325.282
8.7	64.155	329.341
8.8	68.455	333.400
8.9	73.049	337.459
9.0	77.956	341.518
9.1	83.199	345.577
9.2	88.799	349.635
9.3	94.782	353.693
9.4	101.17	357.752
9.5	108.00	1.810
9.6	115.30	5.867
9.7	123.10	9.925
9.8	131.43	13.983
9.9	140.33	18.040
10.0	149.85	22.098
10.1	160.02	26.155
10.2	170.88	30.212
10.3	182.49	34.269
10.4	194.91	38.326
10.5	208.17	42.382
10.6	222.35	46.439
10.7	237.51	50.496
10.8	253.71	54.552
10.9	271.02	58.608
11.0	289.54	62.665
11.1	309.33	66.721
11.2	330.48	70.777
11.3	353.10	74.833
11.4	377.28	78.889
11.5	403.13	82.945
11.6	430.77	87.000
11.7	460.33	91.056
11.8	491.93	95.112
11.9	525.71	99.167
12.0	561.84	103.222
12.1	600.48	107.278
12.2	641.79	111.333
12.3	685.97	115.388
12.4	733.21	119.443
12.5	783.74	123.499
12.6	837.77	127.554
12.7	895.56	131.609
12.8	957.36	135.663
12.9	1023.5	139.718
13.0	1094.2	143.773
13.1	1169.8	147.828
13.2	1250.7	151.883
13.3	1337.2	155.937
13.4	1429.7	159.992
13.5	1528.7	164.046
13.6	1634.6	168.101
13.7	1747.9	172.155
13.8	1869.0	176.210
13.9	1998.7	180.264
14.0	2137.3	184.318
14.1	2285.7	188.373
14.2	2444.4	192.427
14.3	2614.2	196.481
14.4	2795.9	200.535
14.5	2990.3	204.589
14.6	3198.2	208.643
14.7	3420.7	212.697
14.8	3658.8	216.751
14.9	3913.5	220.805
15.0	4186.1	224.859
15.1	4477.7	228.913
15.2	4789.8	232.967
15.3	5123.7	237.021
15.4	5481.0	241.075
15.5	5863.4	245.128
15.6	6272.6	249.182
15.7	6710.5	253.236
15.8	7179.1	257.289
15.9	7680.5	261.343
16.0	8217.2	265.397
16.1	8791.6	269.450
16.2	9406.2	273.504
16.3	10064	277.557
16.4	10768	281.611
16.5	11522	285.664
16.6	12328	289.718
16.7	13191	293.771
16.8	14115	297.824
16.9	15104	301.878
17.0	16163	305.931
17.1	17296	309.984
17.2	18508	314.038
17.3	19806	318.091
17.4	21196	322.144
17.5	22683	326.198
17.6	24275	330.251
17.7	25979	334.304
17.8	27804	338.357
17.9	29756	342.410
18.0	31847	346.463
18.1	34085	350.516
18.2	36481	354.570
18.3	39046	358.623
18.4	41791	2.676
18.5	44731	6.729
18.6	47878	10.782
18.7	51247	14.835
18.8	54854	18.888
18.9	58716	22.941
19.0	62851	26.994
19.1	67277	31.047
19.2	72017	35.099
19.3	77091	39.152
19.4	82524	43.205
19.5	88341	47.258
19.6	94570	51.311
19.7	101240	55.364
19.8	108380	59.417
19.9	116020	63.469
20.0	124210	67.522

Table III

$$\mathrm{ber}' x + i\,\mathrm{bei}' x = \frac{d}{dx} J_0(xi^{3/2}) = M(\cos\theta + i\sin\theta)$$

x	M	θ Degrees	x	M	θ Degrees	x	M	θ Degrees	x	M	θ Degrees
0	0	90	5.0	5.8091	228.551	10.0	144.67	69.275	15.0	4088.8	271.276
.1	.050000	90.072	5.1	6.1794	232.534	10.1	154.54	73.310	15.1	4374.3	275.320
.2	.10000	90.286	5.2	6.5745	236.518	10.2	165.09	77.344	15.2	4679.9	279.364
.3	.15000	90.645	5.3	6.9960	240.504	10.3	176.37	81.379	15.3	5006.9	283.409
.4	.20001	91.146	5.4	7.4456	244.492	10.4	188.42	85.414	15.4	5356.9	287.453
.5	.25004	91.790	5.5	7.9253	248.481	10.5	201.31	89.449	15.5	5731.5	291.497
.6	.30010	92.578	5.6	8.4371	252.472	10.6	215.09	93.485	15.6	6132.3	295.542
.7	.35022	93.509	5.7	8.9831	256.465	10.7	229.82	97.521	15.7	6561.4	299.586
.8	.40043	94.582	5.8	9.5656	260.459	10.8	245.57	101.558	15.8	7020.5	303.631
.9	.45077	95.798	5.9	10.187	264.455	10.9	262.41	105.594	15.9	7512.0	307.676
1.0	.50130	97.156	6.0	10.850	268.452	11.0	280.42	109.631	16.0	8038.0	311.721
1.1	.55209	98.655	6.1	11.558	272.451	11.1	299.67	113.669	16.1	8601.0	315.766
1.2	.60323	100.295	6.2	12.313	276.452	11.2	320.26	117.706	16.2	9203.6	319.811
1.3	.65482	102.074	6.3	13.119	280.454	11.3	342.27	121.744	16.3	9848.6	323.856
1.4	.70698	103.991	6.4	13.978	284.457	11.4	365.81	125.782	16.4	10539	327.901
1.5	.75985	106.045	6.5	14.896	288.463	11.5	390.98	129.820	16.5	11278	331.947
1.6	.81358	108.232	6.6	15.876	292.469	11.6	417.89	133.859	16.6	12069	335.992
1.7	.86837	110.551	6.7	16.921	296.477	11.7	446.68	137.898	16.7	12916	340.037
1.8	.92441	112.999	6.8	18.037	300.487	11.8	477.46	141.937	16.8	13822	344.083
1.9	.98192	115.572	6.9	19.228	304.498	11.9	510.38	145.976	16.9	14792	348.129
2.0	1.0412	118.266	7.0	20.500	308.510	12.0	545.59	150.015	17.0	15831	352.174
2.1	1.1024	121.077	7.1	21.858	312.523	12.1	583.25	154.055	17.1	16942	356.220
2.2	1.1659	124.001	7.2	23.308	316.538	12.2	623.52	158.095	17.2	18132	.266
2.3	1.2321	127.030	7.3	24.856	320.554	12.3	666.60	162.135	17.3	19406	4.312
2.4	1.3012	130.161	7.4	26.509	324.571	12.4	712.67	166.175	17.4	20770	8.358
2.5	1.3736	133.387	7.5	28.274	328.589	12.5	761.95	170.215	17.5	22230	12.404
2.6	1.4498	136.701	7.6	30.159	332.608	12.6	814.67	174.256	17.6	23793	16.450
2.7	1.5300	140.098	7.7	32.171	336.628	12.7	871.05	178.297	17.7	25466	20.496
2.8	1.6148	143.570	7.8	34.321	340.649	12.8	931.37	182.338	17.8	27258	24.542
2.9	1.7046	147.110	7.9	36.617	344.670	12.9	995.89	186.379	17.9	29175	28.588
3.0	1.7999	150.713	8.0	39.070	348.693	13.0	1064.9	190.420	18.0	31229	32.635
3.1	1.9011	154.372	8.1	41.689	352.716	13.1	1138.7	194.462	18.1	33427	36.681
3.2	2.0088	158.080	8.2	44.487	356.740	13.2	1217.7	198.503	18.2	35780	40.727
3.3	2.1236	161.832	8.3	47.476	.765	13.3	1302.2	202.545	18.3	38300	44.774
3.4	2.2458	165.622	8.4	50.669	4.791	13.4	1392.6	206.587	18.4	40997	48.820
3.5	2.3763	169.445	8.5	54.081	8.817	13.5	1489.3	210.629	18.5	43885	52.867
3.6	2.5155	173.296	8.6	57.725	12.844	13.6	1592.8	214.671	18.6	46978	56.914
3.7	2.6640	177.171	8.7	61.619	16.871	13.7	1703.5	218.714	18.7	50289	60.960
3.8	2.8227	181.066	8.8	65.779	20.900	13.8	1821.9	222.756	18.8	53834	65.007
3.9	2.9920	184.978	8.9	70.224	24.928	13.9	1948.6	226.799	18.9	57629	69.054
4.0	3.1729	188.905	9.0	74.974	28.957	14.0	2084.2	230.842	19.0	61693	73.101
4.1	3.3660	192.842	9.1	80.049	32.987	14.1	2229.2	234.885	19.1	66045	77.148
4.2	3.5722	196.790	9.2	85.473	37.017	14.2	2384.5	238.928	19.2	70704	81.195
4.3	3.7924	200.744	9.3	91.269	41.048	14.3	2550.5	242.971	19.3	75694	85.242
4.4	4.0274	204.705	9.4	97.463	45.079	14.4	2728.2	247.014	19.4	81036	89.289
4.5	4.2783	208.671	9.5	104.08	49.111	14.5	2918.4	251.057	19.5	86756	93.336
4.6	4.5460	212.641	9.6	111.16	53.143	14.6	3121.9	255.101	19.6	92881	97.383
4.7	4.8317	216.615	9.7	118.72	57.176	14.7	3339.6	259.145	19.7	99440	101.430
4.8	5.1366	220.591	9.8	126.80	61.208	14.8	3572.6	263.188	19.8	106460	105.477
4.9	5.4619	224.570	9.9	135.44	65.242	14.9	3821.9	267.232	19.9	113980	109.525
									20.0	122040	113.572

Aluminum-Nickel-Cobalt Brake Magnets for Watt-Hour Meters

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MEMBER AIEE

IN 1932 a new aluminum-nickel permanent-magnet material was announced. It may desirably contain a small percentage of cobalt and in that form is commercially available in castings from several dependable suppliers. A typical analysis is aluminum 12 per cent, nickel 20 per cent, cobalt 5 per cent, with the rest iron. As compared with previously available materials, it has such a high coercive force as to affect design radically. No discussion of the material itself will be given as this has been presented elsewhere.[1–3]

This article deals with the application of the new alloy to watt-hour meters. Permanency and resistance to the effect of transient magnetic disturbances are the most important considerations. Application of the material requires new features of design and provides radical economies in space and weight. In discussing the design for the new material, a comparison with the present chromium-steel damping unit will be made because much of the knowledge accumulated with this widely used and successful form of magnet can be applied with benefit to the problem of utilizing the high-coercive material.

General Structure

Figure 1 gives typical second-quadrant values of the hysteresis loops of various permanent-magnet materials. Opposite these curves, in the first quadrant are drawn the corresponding energy-product curves for the respective materials. Watt-hour-meter magnets have previously been made of a chromium steel represented by curve *A* while curve *D* is typical for the aluminum-nickel-cobalt material. For watt-hour meters, materials *B* and *C* have been restricted in general use because of high inherent cost. The new damping unit must be different from the old in size, cross section, and shape. No simple substitution of materials is possible.

Figure 2 shows the general form of the chromium-steel damping-magnet unit. It is formed of two C-shaped magnets having a developed length as compared with air-gap length of at least 60 to 1.

Figure 3 shows the general form of the new unit. It has a single horseshoe-shaped magnet on one side of the disk with a soft-iron armature opposite. The ratio of the magnet developed length to the combined length of the two air gaps may be as low as 15 to 1. The disk-gap length in the average unit approximates 0.100 inch. A bidirectional field form with the pole tips approximately 0.35 inch apart is the most effective for acting upon a meter disk and it will be noted that both units produce this form of field but that the new unit is simpler, doing it with only one magnet instead of two. The double gap in the flux circuit is an ideal way of utilizing the high coercive force of the new material. Ninety-five per cent by weight of the constituent elements in the new magnet are inexpensive. Cost resides in the difficulty of fabrication. The small horseshoe magnet is easy to cast. All of the material in it is effectively used. The resultant design, because of its simplicity, becomes economically possible in the watt-hour-meter field where cost is critical.

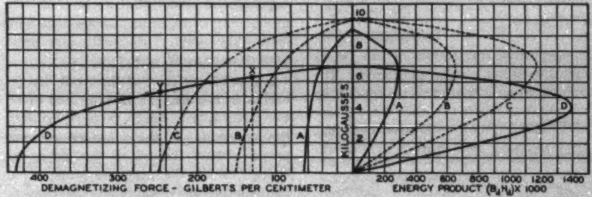

Figure 1. Second-quadrant hysteresis loops and energy-product curves of permanent-magnet materials

A—3.5 per cent chromium steel
B—17.0 per cent cobalt steel
C—36.0 per cent cobalt steel
D—Nickel-aluminum-cobalt steel

Figure 4 shows a photograph of the two chromium-steel magnets and the equivalent new magnet taken together. Both units are suitable for damping the moving element of a modern watt-hour meter having a torque range of from 46 to 54 gram-millimeters at rated load and a nominal rated speed of 30 rpm. The weight of the two chromium-steel magnets is 351 grams as compared with 95 grams for the new magnet. This is a reduction in weight of 73 per cent and an even greater decrease could have been made by designing to as close limits for the new material as for the old. Instead of this, the new unit was made with an excess of strength which makes it possible to give it a liberal "knockdown," to be discussed in greater detail, and which also allows a somewhat greater gap for the disk than for the chromium-steel unit, which is an operating and manufacturing advantage.

The shape of the magnet was chosen after a comparison of the performance of a series of experimental models. It is tapered to give a maximum cross section at the midpoint where both useful and leakage flux must be carried. The slight departure from 90 degrees on the pole faces is for structural convenience in the meter and has no magnetic significance. Figure 5 shows the complete new damping unit on the frame of a single-phase meter of modern design. The magnet and armature are clamped in their relative positions on a small aluminum die casting. An advantage of this is that the gap between magnet poles and armature can be accurately adjusted. Figure 6 is a disassembled view giving a better idea of the clamping washer and screw as well as the temperature-compensating member between the magnet and the clamp washer.

Adjustment as to magnitude of damping (commonly known as full-load adjustment of the meter) is made by forming the armature in three pieces and moving one of them with reference to the other two in

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Paper number 39-74, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted November 14, 1938; made available for preprinting December 27, 1938.

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1. For all numbered references, see list at end of paper.

order to vary the reluctance of the flux path through the armature. Figure 7 is a view of the full-load unit with the armature, but without the magnet, in two positions—(*a*) with the full-load-adjustment piece all the way out which gives greatest reluctance and least damping, and (*b*) with the full-load-adjustment piece all the way in which, in effect, gives a solid armature and greatest damping. It is easy to make the adjustment bar operable from the front with a screwdriver and it can be depended upon for an adjustment range of ±20 per cent as referred to meter registration. Constructional variations, especially as to adjustment features, will occur to many. These could be made without departing from the general plan in which the horseshoe magnet is used.

Temperature Compensation

For the particular damping unit described in the new material, somewhat less temperature compensation is required than for the chromium-steel unit. Over the range from −20 degrees centigrade to 60 degrees centigrade, the coefficient of the uncompensated damping unit is +0.11 per cent per degree centigrade and refers to percentage meter registration. By compensation this coefficient can be reduced to as low a value as desired. The compensation is applied on the side of the magnet in the form of a thin sheet of one of the alloys having a negative coefficient of permeability with respect to temperature. Such a compensating piece is shown in figure 6 and needs no discussion as its application is generally understood.

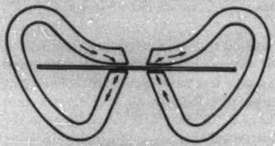

Figure 2. General form of chromium-steel damping unit showing flux paths

Permanency

For many magnet uses a change in strength of a few per cent in a few years would be unimportant. In an instrument magnet it would be highly objectionable. Small changes can be most conveniently detected in terms of meter registration with a magnet unit on a watt-hour meter. Since braking power varies as the square of the flux, small changes in registration, expressed in per cent, will closely approximate double the actual percentage braking-flux changes which cause them. First an example will be given of appreciable-time aging on a group of improperly treated magnets to show that the small changes are readily detected by this method. A group of properly treated chromium-steel units which are substantially nonaging will then be compared with a corrresponding group of the new damping units.

With the familiar chromium-steel meter magnets it has always been necessary to age them artificially by a baking treatment. Curve *A* of figure 8 shows what happened to a group of 20 such chromium-steel magnet units of standard construction but with the baking purposely omitted. This curve and all following aging data are in terms of meter registration as against elapsed time in months. The curve shows the increase in average meter registration for the group over a period of 36 months. Increase in registration means a faster meter and hence less damping or a decrease in magnet strength. After three years the curve flattens out at a two-per-cent increase in registration. Individual meters in the group may increase as much as 3.5 per cent while some of the units age very little or none at all.

About 65 per cent of the total change occurs in the first 12 months and after two years approximately 90 per cent of it has occurred. If the magnets are not properly heat treated or are going to change at all, the first two years after manufacture may, therefore, be considered as an initial critical period which once left behind insures constancy where time is the only consideration.

With the chromium-steel magnet this two-year period can be eliminated by a suitable artificial "aging" process after tempering, which is accomplished by baking. Curve *B* in figure 8 shows the results for another 20 similar magnet units which have been baked for 120 hours at 105 degrees centigrade, which is a standard aging treatment for one manufacturer. The increase in average registration of 0.15 per cent over the period of three years is so small as to leave doubt as to whether limitations in the methods of measurement may not be the cause rather than any real magnet change.

Comparison With New Material

Data are most conveniently presented on the basis of the initial two-year critical period although some of the new units have been on test three years or longer and observations on many units will be continued for a long time.

Table I presents the critical 24-month-initial-period results for three groups of meters. Group *A* is for the same 20 meters used in determining the upper curve of figure 8. Group *B* is for the properly treated 20 chromium-steel units comprising the lower curve of figure 8. Group *C* is for 20 of the new aluminum-nickel-cobalt magnets. Units in all groups are unselected.

It can be seen that there is little to choose, as regards permanency, between either the properly treated chromium-steel unit or the unit of the new material. Both are perfect within the limits of measurement. It must be remembered that the variations include changes which could occur from all possible contingencies, such, for example, as a change in torque of an electromagnet driving unit. They also include the inevitable characteristic errors of the type of measurement involved as well as personal error. A laboratory wattmeter with a clock-controlled timing circuit was used throughout the tests.

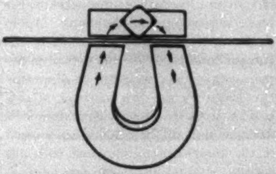

Figure 3. General form of new unit showing flux paths

Table I has its limitations in picturing the minute variations which it is necessary to try to detect so that figure 9 is given to show the performance of another small group of five new units, this time over a period of 30 months. The variations appear large because of the choice of scales but everything shown is happening within a zone less than one-per-cent wide. Although the variations are annoying, they do not preclude consideration of a number of meters together and thus, from averages, a more reliable result may be obtained than with one unit alone. By testing a large number of magnets it is possible to get more representative results and to check samples from different heats and from various sources of supply. This was consistently done.

From a study of the available experimental data, only a small portion of which has been included, it seems clear that within the first two or three years the change in the new damping units is too small to detect. There is no reason to suppose that there will be any change after that time.

Appendix I details the composition, heat treatment, and dimensions of the new units on which permanency data are given. Appendix II gives the composition, heat treatment, and processing of the chromium-steel magnets on which permanency data are given.

Effect of Baking

One reason advanced for the change in untreated chromium steel is that the final operation in hardening is quenching from a high temperature, which leaves portions of the magnet under strain. These strains increase the magnetic qualities but they tend to relieve themselves gradually over a period of time, causing slight weakening of the magnet. As shown in figure 8, curve *A* they are substantially all relieved in three years and 90 per cent of them in two years after the tempering process. The amount of strain, and possible magnetic change, left in a magnet depends to an extent upon the forging process and hence upon shape, but even a straight bar is subject to some change. Curve *A* of figure 8 holds for the shape of chromium-steel magnet shown in figure 2. The long bake at 105 degrees centigrade stabilizes the material by accelerating the changes so that they are complete in 120 hours. It makes no difference whether this bake is given the magnets before or after magnetization. In practice it is convenient to give it before.

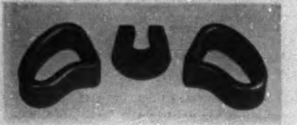

Figure 4. View of two chromium magnets and equivalent new magnet taken together

It could be argued that the new magnetic material is also full of strains and that a similar or even more severe bake would be required to stabilize it. To investigate this, baking tests were made on three chromium-steel bars and three bars of the aluminum-nickel-cobalt material.

Values of coercive force and residual flux were measured for all samples initially and at intervals during a bake lasting 1,000 hours at 105 degrees centigrade. For the chromium steel, coercive force decreased 9.5 per cent and residual flux increased 4.9 per cent as determined from average values for the three samples. Nearly all this change occurred in the first 200 hours. For the new aluminum-nickel-cobalt steel, coercive force decreased only 1.1 per cent and residual flux changed only 0.4 per cent. These latter values are so small as to throw doubt on whether any change really occurred because of limitations in the accuracy of measurement. This test indicates that no reasonable amount of baking at as low a temperature as 105 degrees centigrade will have any effect on the new alloy. Various other baking cycles and temperatures were tried, ranging as high as 315 degrees centigrade for 38 hours and 150 degrees centigrade for 120 hours, all without having any detectable effect on stability. These results were suspected from the start in that the new magnet is a product of precipitation hardening instead of rapid quenching from a high temperature and in addition as a last step, the makers hold it in a furnace at a 1,250 degrees Fahrenheit for 15 minutes. It is possible that even this latter heat treatment is not necessary for time stability, but additional exhaustive tests would be necessary to determine this. In the meantime, in view of the small added expense of the 1,250-degree treatment, it should be continued for instrument magnets.

Magnetizing

Magnets as received from the foundry are cleaned by tumbling and sandblasting. Each magnet, after tumbling, is individually "rung" by hitting it lightly with a small hammer and any cracks or flaws can be detected by the sound. A protective coating, such as enamel, can be omitted as the new material falls within the classification of corrosion-resistant alloys. Although the magnets are brittle, a negligible loss through breakage is suffered during the tumbling process. The elimination of paint on magnets is an operating advantage as difficulty has been experienced in the past in getting it to stick to magnets over a long period of time.

Table I. Natural Aging of Magnet Units Over an Initial 24-Month Period

		Change From Initial Meter Registration in Per Cent		
Group	Description	Maximum Slow	Maximum Fast	Group Average
A	Untreated chromium-steel units	−0.20	+3.25	+1.87
B	Chromium-steel units baked 120 hours at 105 deg C	−0.20	+0.70	+0.15
C	Aluminum-nickel-cobalt units, various treatments	−0.27	+0.59	+0.14

Twenty magnet units are in each group.

The maximum slow and maximum fast values are those found for the extreme individual units in each group. An increase in meter registration would indicate an apparent decrease in damping unit strength.

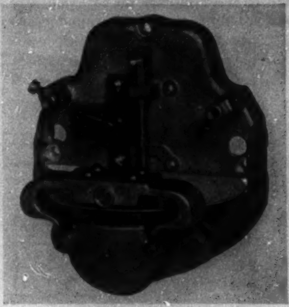

Figure 5. View of complete new damping unit on frame of a single-phase meter

The magnets are magnetized by placing them on a magnetizing fixture and applying at least 5,000 ampere turns per inch acting through their flux circuit to saturate them thoroughly. In production it is desirable to make this current more than sufficient, even by twice the amount, and to keep the apparatus cool by allowing the current to flow for only a fraction of a second.

When the magnet is taken off the magnetizing apparatus, a large gap is interposed in its flux circuit and this on the average magnet has the effect of bringing the material to approximately the point marked *X* on curve *D* of figure 1. Later on by an a-c demagnetizing process or "knockdown" the material may be still further brought down to a point which may be represented at about *Y* on curve *D*. The amount of additional a-c "knockdown" to reach the point *Y* may be set at any desired minimum depending upon the liberality of design in the unit. Since the magnet under discussion is tapered and of varying cross section, the points *X* and *Y* of figure 1 while approximately correct for the average cross section of the average unit, should be considered as subject to some variation.

A-C "Knockdown"

The value of an a-c "knockdown" in stabilizing instrument magnets has been discussed before.[3] The unusually large amount of knockdown in gilberts per centimeter resulting from the a-c demagnetization for the average aluminum-nickel-cobalt magnet can be seen in figure 1 as the abscissa of point Y minus the abscissa of point X, or 120 oersteds. The general effect is to "immunize" the magnet to the susceptibility of further weakening from another demagnetizing force when subsequently encountered in service up to as great a value as the knockdown force. In addition to this protective action against magnetic disturbances, the knockdown has other stabilizing tendencies. As an example, a meter equipped with one of the new magnet units with no knockdown may gain in registration as much as one per cent in the first 24 hours. With knockdown, no change can be detected. Also, as will be seen, adequate knockdown insures stability of the magnet regardless of the effect of temperature cycles.

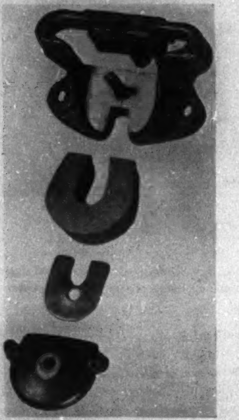

Figure 6. View of disassembled magnet unit

Effect of Temperature Cycles

Watt-hour meters, especially with modern outdoor installations, are subject to continued cycles of heat and cold. These may affect magnet strength unless proper precautions are taken in the magnet processing. Tests were made on this subject with results too voluminous to record in detail but it is desirable to summarize the conclusions. A very severe temperature cycle was used from −40 degrees centigrade to 90 degrees centigrade, which exceeds anything encountered in actual service.

(*a*). Chromium steel magnet units, even though properly aged and given an ample a-c "knockdown," may weaken by as much as one per cent (expressed in terms of meter registration) as a result of three temperature cycles. Ninety per cent of this weakening occurs as a result of the first cycle and more specifically as a result of the first heating. Succeeding cycles above three have no effect. For this reason it has been standard practice to give chromium-steel units an additional short bake at 105 degrees centigrade as a last step in their processing. This is in addition to the long bake for artificial-aging purposes. This latter bake takes away the effects of future temperature cycles.

(*b*). The new high-coercive magnets, if given no "knockdown," will weaken as a result of temperature cycles. To determine this, a number of the new units were magnetized with the armature in place and an iron vane occupying the normal disk-gap space. After assembly this vane was pulled out. Under these conditions the group of units averaged 1.8 per cent increase in meter registration as a result of three temperature cycles. As with the chromium steel, the first cycle did the greater part of the damage and additional cycles beyond three had no effect.

(*c*). Different groups of the new magnets with the proper a-c "knockdown" were then tested by passing them through not less than three temperature cycles. No change in braking flux could be observed as a result of such temperature cycles. If a change had occurred, it could have been eliminated by an additional bake as with the chromium-steel units but such a bake is not necessary providing that the a-c knockdown is properly applied. By proper application of the a-c knockdown is meant that at least one-half of it must be applied by passing the alternating current through the inside of the horseshoe with an armature in place so that all portions of the magnet flux circuit are subjected to the demagnetizing force.

(*d*). As a matter of academic interest, tests were made to see if temperature cycles applied to the magnets before magnetization would immunize them from the effects of such cycles after magnetization. The results indicated that the previous temperature cycles were of little use. This result is in contrast to the condition with respect to the "aging" bake which does just as much good before magnetization as after.

Effect of Magnetic Transients on Meters

Watt-hour meters like other electrical apparatus suffer from undesirable magnetic transients. Weakened magnets are the result of such disturbances when they are great enough to overcome the immunity of the magnets conferred by their "knockdown."

Three sources of magnetic transients can be listed:

(*a*). Surges through the potential coil of the meter as a result of overvoltage or of lightning.

(*b*). Surges through the current coil as a result of either short circuits or of lightning.

(*c*). Current surges through any wire passing near, below, or in back of the meter such as a neutral wire.

Disturbances under (*a*) are least capable of damaging the magnets. The inductance of the meter potential coil is high and repeated surge tests have demonstrated that with even a moderately steep wave front, a breakdown will occur to ground in the case of lightning before enough current can be forced through the coil to damage the magnets.

Under (*b*) it is possible to weaken the chromium-steel magnets of a good modern meter slightly but only under the most extreme short circuits of from 5,000 to 7,000 amperes, peak value. With the usual meter these values can be obtained only in the short-circuit laboratory as the impedance of modern distribution circuits will not allow such currents. Furthermore, power surges of such high value cannot be obtained in service on the usual single-phase meter as used with 30- or 60-ampere fuses. The fuse opens the circuit before these extreme current values are attained. If a lightning surge, instead of a-c power current acts on the current windings, its steep wave front tends to make it flash across the coil rather than through it, if the current value is high,

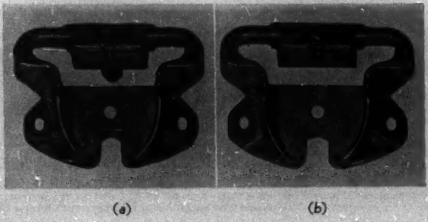

Figure 7. Unit without magnet, in two positions

(*a*)—Minimum damping

(*b*)—Maximum damping

and thus fall under (*c*). Magnet weakening is possible under (*b*) but as a factor it is unimportant as compared with the next classification.

Under (*c*) the current surge may be caused as a result of lightning. It usually is in the neutral wire of a three-wire grounded-neutral system although it could be in one of the outside wires on occasions. All bad lightning disturbances result in condition (*c*) in that if the surge is strong enough, most of it flashes over the coils in the meter and goes directly to ground along some wire passing near the meter. Condition (*c*) can be simulated in the surge laboratory by a straight wire behind and near the meter. In contrast with the first two cases, magnets can be damaged with severe surges. Condition (*c*) is the most severe and consequently comparative tests were made under this condition.

Resistance to the Effect of Surges

A number of meters were tested to determine the relative resistance to the effect of surges under condition (*c*). To do this a circular loop was used which was 40 inches in diameter and had three turns. By placing a meter close to and on one side of the loop, a vertical straight conductor behind the meter could be closely approximated. Placing the meter close to and at the bottom of the loop simulated a straight horizontal conductor behind the meter.

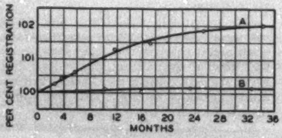

Figure. 8. Natural aging curve on chromium-steel units over three years

(*A*)—Untreated (*B*)—Treated

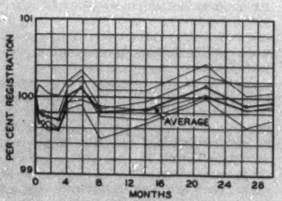

Figure 9. Five aluminum-nickel-cobalt units over 30 months

Mean effective ampere turns in the loop could be adjusted from 1,500 to 15,000. Duration of surges in the loop was controlled by blowing a fuse of predetermined size and oscillograph records were taken on each surge. Duration of surges was in no case less than three full cycles. Demagnetizing effect depends upon crest values but for convenience results are here reported in terms of root-mean-square values.

As a result of the tests on meters equipped with the new aluminum-nickel-cobalt damping units, it was found that the least resistant units would withstand a-c values of 13,000 ampere turns vertically and directly behind the meter without being affected. Some of the stronger units were able to withstand up to 15,000 ampere turns. The wire vertical and behind the meter, for which the results are given, proved to be more damaging than horizontal, although tests were made in both positions, the horizontal test being made first.

On a few meters having chromium-steel magnets, the surge-current ampere turns that could be withstood without damage ranged from 2,300 to 3,500. It should be realized that these are laboratory surge values expressed in root-mean-square figures and that even the lower amounts as measured for the chromium units are satisfactory in normal service.

Effect of Abnormal Surges on Chromium Steel Units

More contrasting laboratory tests in determining the merit of the improved damping unit are possible by subjecting a few meters having magnets of the chromium-steel variety to surges of 13,000 ampere turns. Although this surge will not harm the new unit, it increased the registration of the chromium-steel meters under test from a minimum of 100 per cent to a maximum of 430 per cent. It must be emphasized that these changes are laboratory effects as produced by the 13,000-ampere surge and that the crest values of this surge were at least 18,000-amperes. The large range of variation can be accounted for partly by the fact that all the chromium-steel units had not been subjected to the same "knockdown" in manufacture. Also there is some variability between tests because the magnitude of the transient current peaks may have different values dependent upon exactly when the circuit is closed.

The surge-test setup in the laboratory did not simulate correctly the variations in locations and spacings of conductors to be found in the field, but conditions in this setup were the same for both the chromium-steel and the new high-coercive unit. The results should, therefore, have a comparative value and a valid meaning.

Summary

Application of the powerful new aluminum-nickel-cobalt permanent magnet steels to watt-hour meters requires complete redesign of damping units. No mere substitution of the new material is possible. Permanency in the new design is demonstrated. Great savings in weight and space result and the new design can be made much more resistant to transient magnetic disturbances than the common form of chromium steel damping magnet unit which has preceded it.

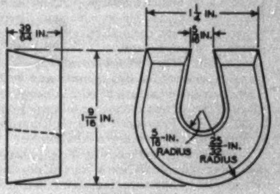

Figure 10. Dimensions of high-coercive damping magnet

Appendix I

Approximate analysis of the new magnets for which permanency data are given is nickel, 20 per cent; aluminum, 12 per cent; cobalt, 5 per cent; carbon, low; iron, remainder. Dimensions of the magnet are given in figure 10. Materials in the alloy were melted under close metallurgical control in a high-frequency induction furnace. Magnets were cast from this furnace into sand molds. After cooling and cleaning, individual magnets were heat treated.

Heat treatment consisted in slowly heating the magnets to 2,000 degrees Fahrenheit and holding at this temperature for a sufficient time to soak the castings thoroughly. Castings were then removed from the furnace and cooled at a predetermined rate in an air blast to 1,250 degrees Fahrenheit. Time required for this cooling ranged from $1^3/_4$ to $2^1/_4$ minutes. They were then placed in a second furnace and held at 1,250 degrees Fahrenheit for 15 minutes, after which they were allowed to cool naturally in air.

After magnetization to saturation, an a-c "knockdown" of not less than 250 ampere turns, mean effective value, was applied around the flux circuit of each magnet with the armature removed. Soft-iron armatures were used separated from the magnet poles 0.120 inch to 0.130 inch. Magnet units were then placed on permanency test.

Production units on single-phase meters have disk gaps averaging 0.100 inch. Permanency figures were, therefore, taken on units with longer gaps and slightly more severe conditions than standard.

Appendix II

Approximate analysis of the chromium-steel magnets for which permanency data are given is carbon, 0.95 per cent; chromium, 3.50 per cent; silicon, 0.25 per cent; iron, remainder. Developed length of the magnet is six inches with a gap of 0.095 inch. After forging to shape, magnets were brought up from room temperature over a period of about 20 minutes to 1,520 degrees Fahrenheit. They were allowed to soak at this temperature for two minutes and were then quenched in a bath of oil. Absorption speed of the oil was about four minutes from 1,520 degrees Fahrenheit to a temperature of approximately 100 degrees Fahrenheit.

After cleaning and grinding the gap, magnets were baked 120 hours at 105 degrees centigrade. They were then surface finished and assembled in brass holding clamps and magnetized to saturation. "Knockdown" by alternating current was applied using at least 180 ampere turns mean effective value for each magnet. A final baking of from four to six hours at 105 degrees centigrade was given.

Magnets in group *B* of figure 8 had both baking steps mentioned above.

Magnets in group *A* of figure 8 had both of these baking steps omitted.

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Discussion

J. P. Larkin (nonmember; Crucible Steel Company of America, Harrison, N. J.): Mr. Green's excellent paper is of great interest to the writer as a representative of one of the licensed manufacturers of Alnico magnets and I would like the opportunity of making a few comments.

As a matter worthy of note in this particular application of Alnico magnets I can say that our company has supplied many thousands of the magnets Mr. Green refers to, over a period of almost three years, to his company. Every magnet shipped has been individually tested magnetically prior to shipment. These magnets are retested before assembling into the meters by a "registration test" and to date no magnets have been returned to us because of low magnetic properties.

Such a record is of interest to the user of magnets because cast magnets of the Alnico type are always furnished by the manufacturer in the heat-treated condition. In the past it has been the practice of many large users of formed or forged magnets to fabricate and heat-treat these magnets in their own plants which always has entailed certain hazards. With the use of Alnico magnets such hazards, of whatever magnitude they may be, are eliminated and the user of these magnets is guaranteed to receive satisfactory and uniform quality because the manufacturer tests each magnet shipped under conditions simulating its actual application in the magnetic circuit.

As stated by Mr. Green, Alnico is melted in high-frequency induction furnaces in relatively small heats. Test bars are poured with each heat and are tested by means of a Leibing model 2 permeameter to establish the B_r and H_c values of each heat. This permeameter and its associated fluxmeter are of quite special design, and used in our laboratory for magnetic standardization work. With this permeameter, $H_{\max}$ values up to 3,000 oersteds can be obtained without undue heating, and it is so designed that accurate demagnetization curves can be taken on the finished magnets, for about 90 per cent of the various shapes and sizes which our company is called upon to produce.

By means of permeameter tests and further checks in the customers magnetic circuit, standards can be set for the various magnet designs. Such standards are the basis of production testing of magnets which tests are made either by means of slip coils, search coils, or special testing machines in a circuit with a fluxmeter.

Some questions have arisen regarding the brittleness of Alnico alloys. These alloys are very brittle. Alnico magnets, however, are used generally in tachometers, magnetos, and other moving mechanisms which are subject to impacts and severe reversals of mechanical stress. It is a matter of record that tachometers with rotating Alnico magnets have been in successful use for a period of more than two years when mounted on steam locomotives and aircraft engines. In both cases there is severe impact and vibration, and no mechanical failures have been reported. The same is true for thousands of magnetos used on tractors, stationary engines, and aircraft engines.

For best results Alnico magnets should be designed in the simplest shapes possible. Care should be exercised by the designing engineer to avoid unnecessary constrictions in the sectional area, such as holes or slots for which no allowance has been made. This is detrimental not only from a magnetic standpoint, but also structurally, due to possible weakening of the casting.

Regarding the permanency of Alnico, tests over a period of years indicate that its stability under any given condition is at least as good as the other magnet steels such as cobalt, chromium, or tungsten magnet steel. Its resistance to the demagnetizing effects of temperature and repeated shock or impact is greatly superior to the other magnet steels.

Based on three years of our experience in the manufacture of Alnico magnets under increasingly closer control and inspection, it is safe to say that on production runs of magnets very close uniformity to accepted standards will be maintained. By close and constant co-operation between the research and laboratory staff with the production department better methods of processing are being developed continuously which will result in still higher quality and uniformity than now commercially obtainable.

The variation in either B_r or H_c can be maintained within plus or minus three per cent of the accepted standard when tested in a given type of permeameter. In addition to this range of variation in actual magnetic quality, a range of plus or minus one per cent should be allowed for due to variation in magnet size or weight (particularly on small castings), making the maximum variation in flux to be expected on finished magnets to be in the order of plus or minus four per cent maximum. In discussing variations of this magnitude it should be borne in mind that various standardizing laboratories do not guarantee the accuracy of H_c measurements to be closer than five per cent on high-coercive magnetic alloys.

A. B. Craig (Boston Edison Company, Boston, Mass.): Mr. Green's paper on the design and application of aluminum-nickel-cobalt brake magnets will be read with interest by meter engineers. Research and new developments always make for constructive progress and as such are always welcomed by the utility industry. It is only reasonable that careful and thorough study must be made of the application of this new material, not only in the laboratory but also in the field under service conditions.

Mr. Green has established that under laboratory conditions the permanence of this new magnet material is entirely comparable with that of the existing magnet material. From the data contained in his paper, it appears reasonable to expect that field conditions will confirm this permanence.

Mr. Green has also demonstrated that under laboratory conditions the new magnet material has a greater resistance to the effect of transient 60-cycle magnetic disturbances. As Mr. Green has pointed out in his paper, 60-cycle disturbances of the magnitude required to affect watt-hour meter magnets are rarely encountered in service. No data have been presented to demonstrate that the new material is superior when subjected to a transient with a steep wave front.

A study of the operating experience of utilities discloses that for one company the record of over 100,000 tests shows only 1.0 per cent of all meters tested were over 102 per cent on full load and only 0.3 per cent were found over 104 per cent. Likewise the records indicate that 1.2 per cent of the meters were less than 98 per cent and 0.5 per cent were less than 96 per cent. These results are consistent with the records of a large number of utilities and include weak-

Equivalent Circuits of Transformers and Reactors to Switching Surges

L. V. BEWLEY
MEMBER AIEE

THE CALCULATION of switching surges occasioned by circuit-breaker operations requires that transformers, reactors, generators, and other apparatus be replaced by their equivalent circuits. To be of much practical use these equivalent circuits must be simple, as well as reasonably exact. Great accuracy, however, is not required—a 20-per-cent departure from exact values being regarded as tolerable. But the approximate circuit must be very simple; for it is merely one element in a network which may consist of many other elements, all of which enter into the equations which describe the switching surge.

Now a transformer, reactor, or other distributed winding, even in the idealized case, consists of a complicated network of self and mutual inductances, ground and shunt capacitances, series resistances, etc. This circuit is entirely too involved for use in the calculation of switching surges; so it therefore becomes expedient to examine the possibility of a much simpler circuit which will react to switching in essentially the same manner as the apparatus which it replaces. It is, then, the object of this paper to make a theoretical enquiry concerning suitable approximate equivalent circuits of this nature, to compare their behavior with test results, and to suggest how they may be used in switching-surge calculations. The scope of the paper is specifically restricted, however, to transformers and reactors.

Switching surges are calculated on the assumption that the circuit breaker interrupts the current at a 60-cycle "current zero," and the switch operation is simulated mathematically by suddenly applying an equal, but opposite, 60-cycle current which cancels the steady-state value thereafter, and thus, in effect, opens the circuit. Now for all practical purposes this "cancellation current" may be regarded as a current with a linear rate of rise; for during the first few hundred microseconds after a 60-cycle current zero the sinusoid is essentially a straight line. It is to current-surges of this nature that equivalent circuits are to be determined.

Reactors

A reactor, under steady-state conditions, is simply an inductance; but when subjected to a suddenly applied surge of either voltage or current it may exhibit a violent oscillation due to its capacitances to ground and between turns. These capacitances are small, and therefore the natural frequencies of oscillation are high—of the order of 10^5 to 10^6 cycles per second.

The idealized equivalent circuit for a uniformly distributed winding, such as a reactor coil on an iron core, is shown in figure 1, in which the distributed capacitance to ground is represented by C, the capacitance between coil sections by K, and the inductance by L (which includes the effect of the mutual inductance between turns).

Of course in the conventional air-core current-limiting reactor the capacitance to ground, C, is not entirely uniformly distributed along the stack, but no great error is committed by assuming it to be so.

On the basis of certain simplifying assumptions regarding the distribution of the mutual inductance between coil sections, the circuit of figure 1 is solved for current surges in appendix I for both a grounded and isolated "neutral" (the end opposite to which the surge is applied). While the complete solution, equation 23, contains an infinite number of harmonics, it is found that only the fundamental is of any practical importance. The line terminal response of a grounded neutral winding to an uniformly rising current surge of the form

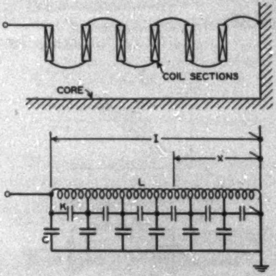

Figure 1. Complete idealized equivalent circuit of the transformer to high-frequency transients

$$i = It \qquad (1)$$

is essentially a pure sinusoidal oscillation

$$e = I\frac{L}{3}\left(1 - \cos\frac{\pi^2\alpha t}{2\sqrt{LC(4\alpha^2 + \pi^2)}}\right) \qquad (2)$$

This equation contains three parameters: the effective inductance L of the winding under high-frequency conditions; the capacitance C of the winding to ground; and the characteristic initial distribution factor $\alpha = \sqrt{C/K}$ where K is the series

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Paper number 39-76, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 4, 1938; made available for preprinting December 31, 1938.

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ening of the magnets from all causes. These figures do not indicate any serious meter troubles.

The use of aluminum-nickel-cobalt brake magnets for watt-hour meters may, if Mr. Green's predictions are justified by field experience, prove another step in the steady development and improvement in watt-hour meters.

Stanley Green: Since first drafts of this paper were prepared, months have gone by during which a continuation of the permanency tests on the new high-coercive magnets has been made. A great many of these magnets have now been on exacting permanency tests for over three years without a single instance being observed to indicate anything contrary to the conclusions of the paper that the new magnet units are perfect as regards permanency with respect to time.

The immunity of the new high-coercive units with respect to other types of disturbances, such as magnetic or mechanical, is a still further insurance of permanency with respect to other factors than time.

The dearth of discussion on the paper may be taken to indicate, I believe, a substantial agreement of readers with respect to the conclusions reached rather than as any lack of interest in the general subject of the application of these new high-coercive magnets to watt-hour meters. As was so kindly mentioned by one of those who discussed the paper, the new magnets are a definite advance in the watt-hour-meter art.

capacitance from end to end of the winding.

It is clear from the form of the equation that after the transient oscillation has died out that the voltage is simply

$$e = I\frac{L}{3} \tag{3}$$

and therefore $L/3$ must represent the total inductance of the winding.

Consider, now, the parallel $L' - C'$ circuit of figure 2 subjected to the current surge of equation 1. Its response voltage is

$$e' = IL'\left(1 - \cos\frac{t}{\sqrt{L'C'}}\right) \tag{4}$$

Comparison of (2) and (4) shows that the simple circuit of figure 2 is equivalent to that of figure 1, under switching surge conditions, if

$$L' = \frac{L}{3} = \text{over-all inductance of the winding} \tag{5}$$

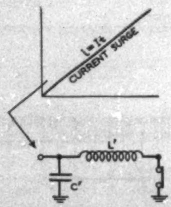


Figure 2. Simplified equivalent circuit of a grounded-neutral transformer to current surges

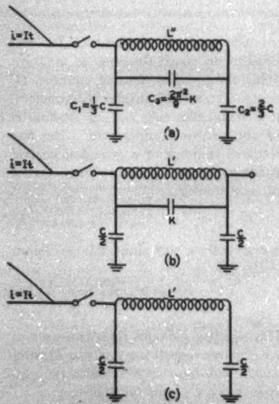


Figure 3. Equivalent circuit of the isolated neutral winding to a current surge

$$C' = \frac{48}{\pi^4}\left(1 + \frac{\pi^2}{4\alpha^2}\right)C = \frac{48}{\pi^4}\left(C + \frac{\pi^2}{4}K\right)$$

$$\cong {}^1/_3\,(\text{capacitance to ground}) + (\text{series capacitance}) \tag{6}$$

Thus the uniformly distributed winding with grounded neutral oscillates with a single frequency about its final voltage as an axis; and behaves as though it were a simple $L'-C'$ circuit in which L' is the inductance of the winding, and C' is the sum of half its capacitance to ground and its series capacitance along the stack.

If the neutral is isolated, it is shown in the appendix that the equivalent circuit is that of figure 3*a*. This, however, is an unsymmetrical circuit, the inductance is not directly identifiable, and it is not possible to reduce it to the grounded neutral case by the closing of a switch. The symmetrical circuit of figure 3*b*, in which half the capacitance-to-ground is lumped at each end of the winding, and the inductance is that of the winding, has the same qualitative characteristics as figure 3*a*, and upon grounding the open terminal reduces to the grounded neutral case of figure 2.

It is therefore proposed to adopt figure 3b as the equivalent circuit for a reactor.

The capacitance-to-ground, C, for air-core current-limiting reactors varies, depending upon their size and voltage rating, from 50 to 300 micromicrofarads, but the bulk of them have a capacitance between 100 and 200 micromicrofarads. Placing a reactor in a metal cell will increase the capacitance some 25 per cent. The series capacitance, K, cannot be measured directly, since the turns are continuous, but the calculated values lie between 5 and 25 micromicrofarads; thus may be ignored in comparison with C, and the equivalent circuit reduced to that of figure 3*c*.

A typical oscillogram of the current surge and resulting voltage transient on a grounded neutral reactor is shown in figure 4. This oscillogram was taken with a transient analyzer developed by C. M. Foust and G. W. Dunlap to study circuit transients of this nature by the "current injection method."

Transformer

A transformer differs from a reactor in having two or more windings, and its simplest distributed network would consist of two circuits such as shown in figure 1 superposed. But the solution of the differential equations for two mutually coupled (both magnetically and electrostatically) circuits of this nature is a rather formidable job; and to effect a solution at all necessitates introducing such simplifying assumptions regarding the flux leakages as may seriously invalidate the results. Also, such a solution would hold only if the windings were all on one core leg. For these reasons it is better to build up an equivalent circuit for a transformer by inference, taking advantage of both theory and test in its construction.

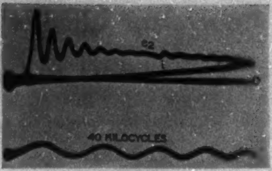


Figure 4. Current-surge and recovery-voltage transient of a reactor (C. M. Foust and G. W. Dunlap)

The circuit of figure 5 appears to fulfill the requirements. It is a double-frequency circuit, just as would be expected from the superpositions of two single-frequency circuits (figures 1 and 2). It reduces to a single-frequency circuit if the secondary winding is grounded throughout its length, or if the two halves of the winding are on different legs of the core. The accessible terminals correspond to the actual terminals of the transformer. In this circuit $C_3 = C_1/2$ is *half* the total capacitance between windings; $C_4 = C_2/2$ is *half* the total capacitance from the secondary to ground (the core). The terminal voltage corresponding to a switching surge, as given in appendix II, is

$$e = LI\,(1 - A\cos\omega t - B\sin\Omega t) \tag{7}$$

in which A and B are fractions whose sum is unity, and L is the leakage reactance between the two windings. Equation 7 is also the solution to figure 5*b*, and this simpler circuit may therefore be regarded as the equivalent circuit of the grounded-neutral transformer with short-circuited secondary.

When the leakage reactance between the two halves of the secondary becomes very large, as when the winding is in series on two legs of the core, it is shown in appendix II that the transformer equivalent circuit reduces to the single-frequency circuit of figure 2, in which L' is then the leakage reactance between primary and secondary windings. Thus it is to be expected that when the windings are all on one core leg, that the voltage transient will have a double frequency, as shown by the oscillograms of figure 6. But when the windings are on two legs,

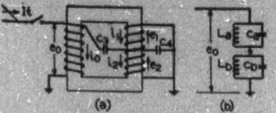


Figure 5. Equivalent circuit of a two-winding transformer to switching surges

Table I

	Bushing Rating (Kilovolts)									
	15	25	37	50	73	92	115	138	161	230
Standard bushing	150	125	100	100	100	140	180	180	180	220
Capacitance bushing							225	225	225	300

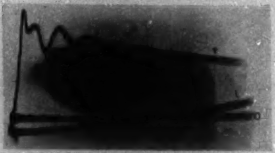


Figure 6. Current-surge and recovery-voltage transient of a transformer—all windings on one leg (C. M. Foust and G. W. Dunlap)

there is essentially only one frequency present, as shown in figure 7. In the former case the oscillatory component was considerably reduced by the shunt resistor used as an oscillograph divider.

While sufficient experimental data are not at hand to permit drawing a blanket conclusion, yet it is believed that *it is permissible to regard figure 2 as the equivalent circuit of a grounded neutral, short-circuited-secondary transformer to switching surges.*

The capacitances C and K and the initial distribution factor α may be accurately calculated from the design sheet.

Average values of the capacitance to ground, C, of single-phase core-type concentric-winding power transformers are given in figure 8. The corresponding values per phase for three-phase transformers are approximately half the values of figure 8. It will be observed that the capacitance increases as the voltage rating decreases, for the reason that the insulation distances are less for the lower voltages. However, as the voltage reaches the lower levels there is very little change in the capacitance, because the insulation distances at these lower voltages are fixed by thermal (oil ducts) and mechanical (cylinders and spacers) considerations, rather than by voltage stresses. Figure 8 also shows that for the higher voltage ratings, the capacitance does not change much with either kilovolt-amperes or kilovolts. These curves show the general trends, but individual transformers may deviate considerably from them.

The α factor for unshielded single-phase core-type, concentric-winding power transformers is given as a band in figure 9. While this factor has a very definite tendency to decrease with an increase in voltage rating, there does not appear to be any correlation with kilovolt-ampere ratings, as evidenced by the crisscrossing of the kilovolt-ampere rating lines inside the band. The band, however, is sufficiently narrow so that no great error is committed by taking a median value. The α factors for three-phase transformers are given in figure 10. In this case there is a distinct difference between those transformers having single-circuit and those having parallel circuit windings, but otherwise the tendency is the same as for the single-phase case. It is to be emphasized that the α factors given in figures 9 and 10 represent the *average* initial distribution throughout the winding, and are *not* an index of the steepness of the initial distribution at the line end corresponding to an abrupt voltage surge. Actual transformers depart from the idealized one analyzed in the appendix, in that their initial distributions do not follow the theoretical α curves near the line end, but exhibit a greater concentration of voltage there; unless this is corrected by shielding.

The series capacitance K is easily estimated from the curves of figures 8, 9, and 10 by the formula

$$K = \frac{C}{\alpha^2} \tag{8}$$

For example, a 30,000-kva 115-kv single-phase transformer has a capacitance to ground, according to figure 8, of C = 4,500 micromicrofarads. A reasonable α factor from figure 9 is $\alpha = 16$. Therefore, the series capacitance would be of the order of

$$K = \frac{4{,}500}{16^2} = 18 \text{ micromicrofarads}$$

thus negligibly small compared to the capacitance to ground C.

The approximate capacitance in micromicrofarads for standard bushings is given in table I.

If the transformer secondary, instead of being short-circuited, is connected to some external impedance, then in the equivalent circuit that impedance is reflected to the primary side by the square of the turn ratio, in the usual way, as shown in figure 11.

EXAMPLE

What is the equivalent circuit of a 6,667-kva 115/23-kv single-phase grounded-neutral transformer connected to a 500-ohm-surge-impedance transmission line on the 115-kv side, and faulted to ground near the 23-kv terminal? The situation is shown in the top diagram of figure 12. The transformer has eight per cent reactance.

The inductance element is simply the leakage inductance of the transformer referred to the 23-kv side and is

$$L' = \frac{10}{2\pi f} \text{ (per cent IX) } \frac{(\text{kv})^2}{(\text{kva})}$$

$$= \frac{10 \times 8 \times 23^2}{377 \times 6{,}667 \times 3} = 0.0056 \text{ henry}$$

The surge impedance of the transmission line, referred to the 23-kv side, is

$$n^2Z = \left\{\frac{23}{115}\right\}^2 500 = 20 \text{ ohms}$$

From figure 8 the winding capacitance to ground of a 6,667-kva 23-kv transformer would be

C = 4,900 micromicrofarads

From figure 9 the α factor would be of the order of $\alpha = 28$ and the series capacitance therefore

$$K = \frac{C}{\alpha^2} = \frac{4{,}900}{28^2} = 6$$

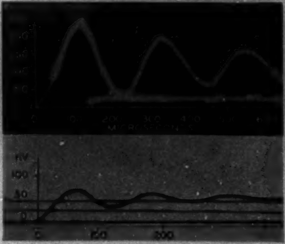


Figure 7. Cathode-ray oscillograms of recovery voltage on power transformers (W. F Skeats)

From the table of bushing capacitances, for a 23-kv bushing

C_b = 125 micromicrofarads

Therefore, the equivalent capacitance is

$$C' = \frac{C}{2} + K + C_b = \frac{4{,}900}{2} + 6 + 125 = 2{,}600 \text{ micromicrofarads}$$

The complete equivalent circuit is shown in the bottom diagram of figure 12.

It is clear from the relative values of the capacitances as calculated for this example, that the series capacitance K and the bushing capacitance C_b are of little consequence compared with the ground capacitance C_g and may be ignored in comparison therewith.

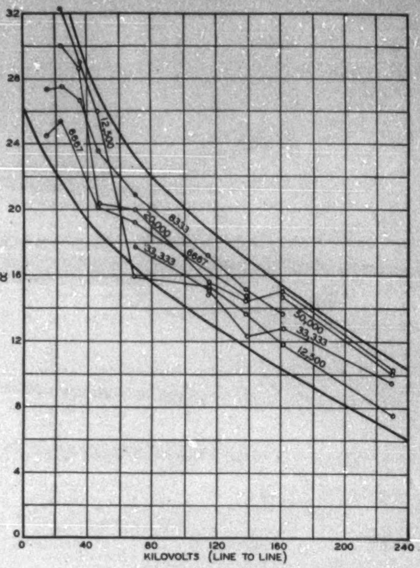


Figure 9. Average α factors for single-phase core-type concentric-winding transformers

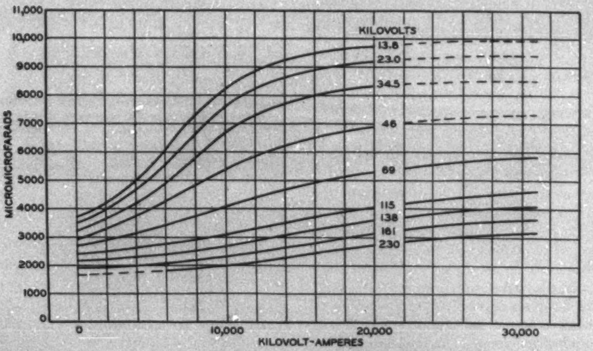
TESTS

There are two methods of making switching-surge tests: (1) actual circuit-breaker operation, recording with a cathode-ray oscillograph, and (2) the "current injection" method with a specially designed transient analyzer.

The two oscillograms of figure 7, furnished by W. F. Skeats, show the result of switching-surge tests on large power transformers. In both of these cases the cathode-ray oscillograph divider was a capacitance at the terminal of the transformer, the effect of which was to slow down the natural frequency. It will be noticed that in these oscillograms the oscillations are essentially single frequency, and in the second case it is a pure sinusoid as near as can be told by inspection. The equivalent circuit of figure 2 would therefore be ample for these cases.

The oscillograms of figures 4 and 6 were taken by C. M. Foust and G. W. Dunlap with a modified transient analyzer similar to that developed by K. J. R. Wilkinson[1] for studying switching surges by the "current injection" method. This method consists of injecting current surges into the circuit under test at intervals sufficiently close together so that a steady image is projected onto the screen of the oscilloscope, which image may be photographed for a permanent record. The current surge is a straight line starting at zero and rising at a uniform rate, and therefore simulates a switching-surge current. The oscillogram of figure 6 was taken on the outside winding of a single-phase transformer, the inner winding being short-circuited and grounded. Both windings were entirely on the center core leg. There was, therefore, inductive coupling between the two halves of the inner winding, and in accordance with the theory, the recovery-voltage transient shows the characteristic double-frequency oscillations. Unfortunately, the voltage divider of the transient analyzer was a shunt resistor of such low value that the oscillations were severely reduced, the high-frequency component being nearly critically damped.

Figure 8. Capacitance between windings of power transformers



Appendix I

Grounded-Neutral Single-Winding

The differential equation for the idealized transformer of figure 1, under high-frequency transient conditions, and neglecting the losses, is[2]

$$\frac{\partial^4 e}{\partial x^4} - LK\frac{\partial^4 e}{\partial x^2 \partial t^2} + LC\frac{\partial^2 e}{\partial t^2} = 0 \qquad (9)$$

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1. For all numbered references, see list at end of paper.

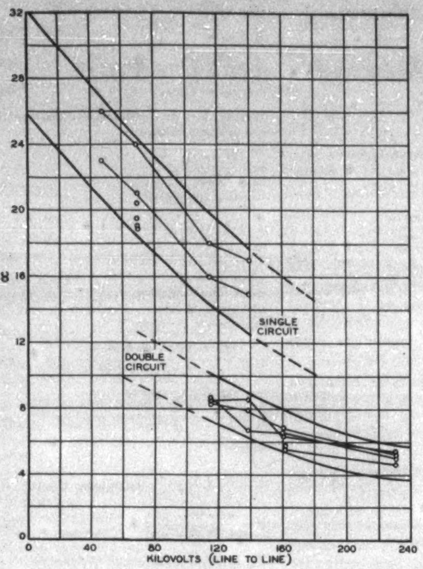

Figure 10. Average α factors for three-phase core-type concentric-winding transformers

A solution, suitable for the present purposes, is

$$e = \Sigma\,(A \sin \lambda x \cdot \sin \omega t + B \sin \lambda x \cdot \cos \omega t + C \cos \lambda x \cdot \sin \omega t + D \cos \lambda x \cdot \cos \omega t) \qquad (10)$$

Substitution of (10) in (9) yields the following relationship between the space wave length λ and the time wave length ω,

$$\omega = \frac{\lambda^2}{\sqrt{L(C + K\lambda^2)}} \qquad (11)$$

At the grounded neutral of the transformer, $x = 0$, the voltage is zero for all time, and (10) then gives

$$0 = \Sigma(C \sin \omega t + D \cos \omega t), \text{ or}$$
$$C = D = 0 \qquad (12)$$

At the line end of the winding, $x = 1$, the voltage is initially ($t = 0$) zero, since a finite current cannot instantaneously build up a voltage across a capacitance network. Therefore by (10)

$$0 = \Sigma B \sin \lambda \cdot \cos \omega t, \text{ or } B = 0 \qquad (13)$$

The voltage equation thus reduces to

$$e = \Sigma A \sin \lambda x \cdot \sin \omega t \qquad (14)$$

The total current flowing through the transformer is[3]

$$i = C \int \frac{\partial e}{\partial t}\, dx$$
$$= -C \sum \frac{\omega}{\lambda} A \cos \lambda x \cdot \cos \omega t + I(t) \qquad (15)$$

in which $I(t)$ is an integration constant with respect to x, and is therefore a possible function of t. At the line end, $x = 1$, the current is the applied current I, so that

$$I = I(t) - C \sum \frac{\omega}{\lambda} A \cos \lambda \cdot \cos \omega t \qquad (16)$$

from which it is clear that

$$I(t) = I \text{ and } \lambda = \frac{s\pi}{2}\,;\; s = 1, 3, 5, \ldots \qquad (17)$$

That part of the through current confined to the inductance is

$$i_L = i - i_k = i - K \frac{\partial^2 e}{\partial x\, \partial t}$$
$$= I - \sum \frac{\omega}{\lambda} (C + \lambda^2 K) A \cos \lambda x \cdot \cos \omega t \qquad (18)$$

Initially this current must be zero, since time is required to build up a current in an inductive circuit. Thus at $t = 0$

$$0 = I - \sum \frac{\omega}{\lambda} (C + \lambda^2 K) A \cos \lambda x \qquad (19)$$

Expressing I as a cosine series on the wave length λ,

$$\frac{4}{\pi} I \sum \frac{\sin \frac{s\pi}{2}}{s} \cos \frac{s\pi x}{2} = \sum \frac{\omega}{\lambda} (C + \lambda^2 K) A \cos \lambda x \qquad (20)$$

Therefore, substituting for λ from (17), the A coefficients are found to be

$$A_s = I \sqrt{\frac{L}{C}}\, \frac{16\, \alpha \sin \frac{s\pi}{2}}{s^2 \pi^2 \sqrt{4\, \alpha^2 + s^2 \pi^2}} \qquad (21)$$

where

$$\alpha = \sqrt{C/K} \qquad (22)$$

is the characteristic initial distribution factor[2] of the transformer.

The voltage equation now becomes, explicitly:

$$e = I \sqrt{\frac{L}{C}} \sum_{1,3,5}^{\infty} \frac{16\, \alpha \sin \frac{s\pi}{2}}{s^2 \pi^2 \sqrt{4\, \alpha^2 + s^2 \pi^2}} \times \sin \frac{s\pi x}{2} \sin \frac{s^2 \pi^2 \alpha t}{2\sqrt{L\, C(4\, \alpha^2 + s^2 \pi^2)}} \qquad (23)$$

If the applied current surge, instead of being a "unit function," rises linearly at a uniform rate $i = It$, then the application of Duhamel's theorem to (23) gives

$$e = I\, L \sum_{1,3,\ldots}^{\infty} \frac{32}{s^4 \pi^4} \sin \frac{s\pi}{2} \sin \frac{s\pi x}{2} \times \left[1 - \cos \frac{s^2 \pi^2 \alpha t}{2\sqrt{L\, C\,(4\, \alpha^2 + s^2 \pi^2)}}\right] \qquad (24)$$

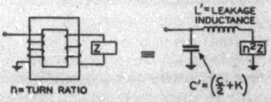

Figure 11. Equivalent circuit with external impedance in secondary circuit

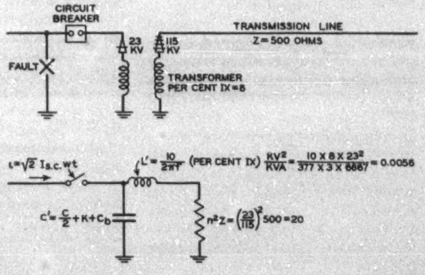

Figure 12. Example illustrating use of the simplified equivalent circuit of a transformer

Due to the presence of s^4 in the denominator of this equation it is clear that only the fundamental is of importance. For example, the next harmonic—the third—is only $1/3^4 = 0.012$ of the fundamental. Therefore, for all practical purposes

$$e \cong IL\frac{32}{\pi^4}\sin\frac{\pi x}{2}\times \left[1-\cos\frac{\pi^2\alpha t}{2\sqrt{LC(4\alpha^2+\pi^2)}}\right] \qquad (25)$$

and at the line terminal of the transformer this becomes

$$e = I\frac{L}{3}\left[1-\cos\frac{\pi^2\alpha t}{2\sqrt{LC(4\alpha^2+\pi^2)}}\right] \qquad (26)$$

Single-Phase Isolated Neutral

When the neutral is isolated, there must be a continual increase in voltage for an applied unit function current, for charge is indefinitely accumulating on the capacitance to ground. Assume a solution of the form

$$e = Et + \Sigma D\cos\lambda x\cdot\sin\omega t \qquad (27)$$

substitution of which in the differential equation 9 again yields (11) as the relationship between the space and time harmonics. The total current flowing along the stack is

$$i = C\int\frac{\partial e}{\partial t}dx = I(t) + CEx + C\sum D\frac{\omega}{\lambda}\sin\lambda x\cdot\cos\omega t \qquad (28)$$

The current flowing in the inductance is

$$i_L = i - i_k = i - K\frac{\partial^2 e}{\partial x\partial t} = I(t) + CEx + \sum D\frac{\omega}{\lambda}(C+K\lambda^2)\sin\lambda x\cdot\cos\omega t \qquad (29)$$

The boundary conditions are:

$$\left.\begin{aligned} e &= 0 \text{ at } t = 0\\ i &= 0 \text{ at } x = 0\\ i &= I \text{ at } x = 1\\ i_L &= 0 \text{ at } t = 0 \end{aligned}\right\} \qquad (30)$$

These are satisfied by putting

$$CE = I,\quad I(t) = 0,\quad \lambda = s\pi \qquad (31)$$

and

$$0 = Ix + \sum D_s\frac{\omega_s}{\lambda_s}(C+K\lambda_s^2)\sin\lambda_s x \qquad (32)$$

Multiplying (32) by ($\sin\lambda x\cdot dx$) and integrating between the limits of 0 and 1 there results

$$D_s = \frac{2I}{s^2\pi^2}\sqrt{\frac{L}{C+Ks^2\pi^2}}\cos s\pi \qquad (33)$$

The voltage equation now becomes

$$e = I\frac{t}{C} + 2I\sum_1^\infty{}^s\sqrt{\frac{L}{C+Ks^2\pi^2}}\times\frac{\cos s\pi}{s^2\pi^2}\cos s\pi x\cdot\sin\omega_s t \qquad (34)$$

If the applied current increases at a uniform rate, an integration of (34) between the limits $t = 0$ and $t = t$ gives

$$e = I\frac{t^2}{2C} + 2IL\sum_1^\infty\frac{\cos s\pi}{s^4\pi^4}\times(1-\cos\omega_s t)\cos s\pi x \qquad (35)$$

The fundamental at the line end ($x = 1$) is

$$e_1 = I\left[\frac{t^2}{2C} + \frac{2}{\pi^4}L(1-\cos\omega_1 t)\right] \qquad (36)$$

The circuit of figure 3*a* subjected to a uniformly rising current surge responds as

$$e'' = I\left[\frac{t^2}{2(C_1+C_2)} + \left(\frac{C_2}{C_1+C_2}\right)^2 L''\times\left(1-\cos t\sqrt{\frac{C_1+C_3}{L''(C_1C_2+C_1C_3+C_2C_3)}}\right)\right] \qquad (37)$$

Equations 36 and 37 are of the same form and become identical if

$$C_1 = \frac{1}{3}C,\quad C_2 = \frac{2}{3}C,\quad L'' = \frac{9}{2\pi^4}L,\quad C_3 = \frac{2\pi^2}{9}K \qquad (38)$$

and therefore suggests figure 3*a* as the appropriate equivalent circuit.

Appendix II

The canonical equations for the three-winding transformer of figure 5*a* are[3]:

$$e_1 - n_1e_0 = Z_{11}i_1 + Z_{12}i_2 \qquad (39)$$

$$e_2 - n_2e_0 = Z_{21}i_1 + Z_{22}i_2 \qquad (40)$$

$$-\ i_0 = n_1i_1 + n_2i_2 \qquad (41)$$

in which, since windings number 1 and number 2 are alike and symmetrical,

$n_1 = n_2 = n =$ (turns of number 1 or number 2)/(turns of number 0)

$Z_{11} = Z_{22} = n^2Z_{0-1} = n^2pL' =$ leakage impedance, number 0 to number 1

$Z_{12} = \frac{n^2}{2}(Z_{0-1} + Z_{0-2} - Z_{1-2})$

$= n^2pL' - \frac{n^2}{2}pL''$

= leakage impedance number 1 to number 2

The constraints of the circuit are:

$$e_1 + e_2 = 0 \qquad (42)$$

$$i_1 = i_2 + C_4pe_2 - C_3p(e_0 - e_2) \qquad (43)$$

$$i = It = C_3p(e_0 - e_2) + i_0 \qquad (44)$$

Solving these six simultaneous equations for the terminal voltage:

$$e_0 = \frac{C_4+C_3}{C_3C_4}\times\frac{[p^2 + 4/n^2L''(C_3+C_4)]I}{p^4 + \left(\frac{4}{n^2L''C_4} + \frac{C_3+C_4}{LC_3C_4}\right)p^2 + \frac{4}{n^2L''C_3C_4}} \qquad (45)$$

where $L = (L' - L''/4) =$ leakage impedance from number 0 to number 1 and number 2 in series. Equation 45 may be expressed in the form

$$e_0 = \frac{C_4+C_3}{C_3C_4}\,\frac{p^2+w^2}{(p^2+\omega^2)(p^2+\Omega^2)}I \qquad (46)$$

The solution to (46) is

$$e_0 = LI(1 - A\cos\omega t - B\sin\Omega t) \qquad (47)$$

where

$$A = \frac{\Omega^2}{w^2}\left(\frac{\omega^2-w^2}{\omega^2-\Omega^2}\right) \text{ and } B = \frac{\omega^2}{w^2}\left(\frac{\Omega^2-w^2}{\Omega^2-\omega^2}\right) \qquad (48)$$

The solution to the circuit of figure 5*b* is

$$e = (L_a+L_b)I\left(1 - \frac{L_a}{L_a+L_b}\times\cos\frac{t}{\sqrt{L_aC_a}} - \frac{L_b}{L_a+L_b}\cos\frac{t}{\sqrt{L_bC_b}}\right) \qquad (49)$$

Thus figure 5*b* is equivalent to figure 5*a* if

$$L_a = AL;\quad L_b = BL;\quad C_a = \frac{1}{\omega^2AL};\quad C_b = \frac{1}{\Omega^2BL} \qquad (50)$$

If the leakage inductance L'' between the two halves of the secondary winding is very large ($L'' \rightarrow \infty$), as is the case when the windings on the two legs of the core are in series, then the terms of (45) containing L'' drop out and its solution becomes the single-frequency transient

$$e_0 = LI\left(1 - \cos\sqrt{\frac{C_3+C_4}{LC_3C_4}}\,t\right) \qquad (51)$$

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The Role of the Ionosphere in Radio Wave Propagation

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I. Introduction

WHEN Marconi electrified the world in 1901 by sending radio signals across the Atlantic Ocean, he incidentally proved that the upper atmosphere is electrified. Diffraction was insufficient to explain the bending of the electromagnetic waves around the 30 degrees of the earth's curved surface. The waves could not penetrate the earth. So there was only one way they could go the incredible distance, and that was by reflection from one or more conducting strata in the atmosphere. So reasoned Professor A. E. Kennelly, and he published[1] the idea a few months after Marconi's demonstration. Oliver Heaviside reached the same conclusion and published[2] it at nearly the same time; his paper however mentioned only a single layer. This postulated layer was known for a number of years as the Kennelly-Heaviside layer, and the entire ionized region in the upper atmosphere which affects the transmission of radio waves is now called the ionosphere. Radio waves transmitted by means of it are called sky waves, in contradistinction to the ground waves, those which are propagated along the earth's surface.

While it might at first be astonishing that the extremely rare upper atmosphere could contain enough material to affect the passage of radio waves, yet the very thinness of the air is what permits this to happen. At a height of 100 kilometers (62 miles) above the earth's surface, for example, the air pressure is probably much less than a millionth of what it is at the earth's surface, or the equivalent of a vacuum in an electric-discharge vacuum tube. The air particles are separated so far from one another that collisions are far less frequent than in the lower atmosphere, and when an atom is ionized by solar radiation it remains ionized for a considerable time. Therefore at any given time a large proportion of the air particles are in an ionized condition. When a radio wave passes through such ionized air the electric force of the wave accelerates the ions, which take on a certain amount of motion at the wave frequency, this accelerated motion in turn giving rise to radiation. There is thus an interchange of energy between the ionized air particles and the radio waves, with a net effect of reflection or refraction of the waves.

It has been found from radio experiments that this ionized condition does not increase uniformly as the air pressure decreases with altitude. Because of varying distribution of chemical composition of the air with height, and because of the different gases' differing capability of absorption of solar radiation of different frequencies, there are certain strata or layers in the air in which a maximum of ionization exists, that is, the ionization is greater than it is either above or below the layer.

The ionization of the ionosphere layers is principally due to ultraviolet radiation from the sun. The detailed processes by which the ionization is produced and maintained at any given level, are obscured by the almost complete lack of precise knowledge of the composition, state of dissociation, and temperature at those levels. The ionization produced in the daytime is carried over into the night by chemical, electrical, or other reactions which are at present unknown. Some speculation has appeared in print on some of the details, but that is outside the scope of this paper, which is essentially a presentation of the known facts. These facts are fairly complicated, and it is noteworthy that while some of them have been explained by some of the speculative theory, such theory has in each case been developed to fit the facts after the facts had been discovered.

IONOSPHERE STRUCTURE

Since the distribution of energy in the spectrum of radiation from the sun, and likewise possibly the chemical composition and temperature of the air at different heights, vary at different times of the day and year, the ionized layers in the atmosphere do not remain always at the same height but vary diurnally, seasonally, and otherwise, in both height and ionization. There may be a considerable number of such layers at a given time. Of these, two are permanent, and two others are semi-permanent. The two permanent ones are called the *E* and *F* layers. The *E* layer is at a height of 90 to 140 kilometers at different times, usually about 110 kilometers. The term *F*-layer is ordinarily reserved for the other layer as it exists at night; in the daytime during most of the year it divides into two layers which are called the F_1 and F_2. The night *F* layer is at a height of 180 to 350 or more kilometers. The F_1 layer exists in the daytime (except in winter), at a height of 130 to 250 kilometers. The F_2 layer exists every day, at a height of 250 to 350 or more kilometers in the summer, dropping to about 150 kilometers in the winter day. (The "virtual" heights, defined later, are higher than these values.) The fourth layer, which is semipermanent, is the *D* layer; it exists only in the daytime and its height is of the order of 50 to 90 kilometers. Little has been done on the determination of the quantitative characteristics of the *D* layer, its effects being largely inferred rather than directly

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Paper number 39-47, recommended by the AIEE committee on communication, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted November 23, 1938; made available for preprinting December 29, 1938.

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[1] "On the Elevation of the Electrically Conducting Strata of the Earth's Atmosphere," A. E. Kennelly, *Electrical World and Engineer*, volume 39, March 15, 1902, page 473.

[2] "Telegraphy," Oliver Heaviside, Encyclopedia Britannica, tenth edition, volume 33, December 10, 1902, page 215.

observed. Existing knowledge covers mainly the E, F, F_1, and F_2 layers.[2a]

The structure of the ionosphere may be visualized in an elementary way from figure 1, which is for a typical summer daytime condition, the F_1 and F_2 layers both being present as well as the E. This is drawn to scale, so the angles of reflection of radio waves from the layers may be estimated correctly. The three layers are shown as mere thin lines, for simplicity. The layers have in fact a certain thickness, and the density of ionization varies somewhat in this thickness. At the right of the diagram is a rough illustration of a possible distribution of ionization density with height.

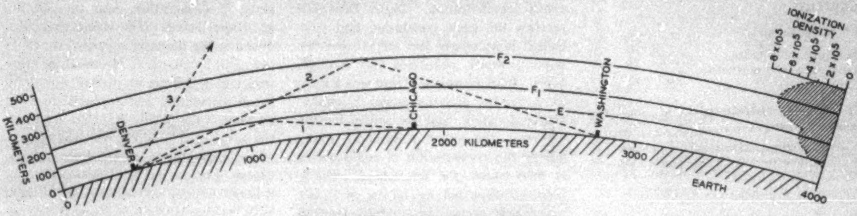


Figure 1. The principal layers of the ionosphere, drawn to scale for a particular time of day and year

Dotted lines indicate two of many possible paths of radio waves from a transmitter to a receiver as transmitted by reflection from the ionosphere layers. This picture, simple as it is, does in fact represent the basic mechanism of radio-wave transmission over long distances. When we consider the variations of ionization and height of the layers with time, and the effects of the ionization upon the received intensity and the limits of transmissible frequency at any particular time, the picture loses its simplicity. The purpose of the present paper is to outline the principal known facts and implications of this complicated situation. We shall see that the phenomena of long-distance radio transmission may be completely explained by the ionosphere.

IONOSPHERE CHARACTERISTICS

There are three principal ionosphere characteristics which determine radio transmission. These are the height, the ionization density, and the energy absorption capability of each of the ionosphere layers. Since each layer has a certain thickness it is necessary to define the sense in which the term height is used. A ray or wave train starts to bend toward the ground immediately upon entering the layer, as illustrated in figure 2, and follows a curved path in the layer until it emerges at the same vertical angle at which it entered. It has been shown[3] that the time of transmission along the actual path BCD in the ionized layer is the same as would be required for transmission along the path BED if there were no ionized particles present. The height h, from the ground to E, the intersection of the two projected straight parts of the path, is called the virtual height of the layer. This is the important quantity in all measurements and applications.

The virtual height of a layer is measured by transmitting a radio signal from A, and receiving at F both the signal transmitted along the ground and the echo, or signal reflected by the ionosphere, and measuring the difference in time of arrival of the two. The signal is a special, very short pulse in order that the two may be separated in an oscillograph, as the time differences are mere thousandths of a second. The difference between the distance ($AE + EF$) and AF is found by multiplying the measured time difference by the velocity of light. From this and the known distance AF, the virtual height h is calculated. It is usually convenient to make AF zero, that is, to transmit the signal vertically upward and receive it at the same place (and the term "virtual height," rigorously defined, is for this case).

The effectiveness of the ions in reflecting the waves back to earth depends on the number of ions present in a unit of volume, that is, the ionization density. It requires a greater density of ionization to reflect the waves back to earth, the higher the frequency. It has been shown that, for electron ionization, the relation[4] (for the ordinary ray) is

$$N = 0.0124 f^2 \qquad (1)$$

where N is the number of electrons per cubic centimeter and f is the highest frequency, in kilocycles per second, at which waves sent vertically upward are reflected back to earth. Waves of all frequencies higher than this pass on through the ionized layer. This frequency is called the critical frequency, and measurement of it is, with the equation just given, a means of measuring the maximum ionization density in an ionized layer.

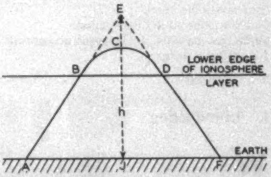


Figure 2. Diagram for explanation of virtual height of ionized layer

The critical frequency is thus the practical measure of the ionization density, the second of the principal quantities determining the role of the ionosphere in radio transmission. The way the critical frequency is measured is to determine the virtual height of the layer by the method mentioned above, using vertical or nearly vertical transmission (that is, with the transmitter and receiver not far apart), and keep increasing the frequency until signals are no longer received back from the layer. The highest frequency at which signals are received back from the layer is the critical frequency of that layer. (For some purposes this definition is not exact; see discussion of "sporadic *E*" in section IV.) The results of such a measurement are illustrated in figure 3. Starting at a frequency below 2,000 kilocycles per second, the virtual height is found to be about 110 kilometers, and remains at about this height until about 3,000 kilocycles per second. The critical frequency of the *E* layer at the time of this measurement is thus 3,000 kilocycles per second. At this frequency the waves penetrate the *E* layer and go on up to a higher layer, the F_2. The F_2 layer has a greater ionization density and so it reflects back waves of frequency greater than 3,000 kilocycles per second. It is not until frequencies greater than 12,000 kilocycles per second are used that the F_2 layer fails to reflect them, in the case illustrated.

[2a] The D, E, and F layers were first identified by E. V. Appleton (Papers of the URSI General Assembly, Washington, 1927, volume 1, part 1, page 2). The F_1 and F_2 layers were first identified by J. P. Schafer and W. M. Goodall (*Nature*, volume 131, 1933, page 804) and S. S. Kirby, L. V. Berkner, and D. M. Stuart (IRE *Proceedings*, volume 21, 1933, page 757). The D layer was first shown to serve as a reflecting layer by N. Smith and S. S. Kirby (*Physical Review*, volume 51, 1937, page 890).

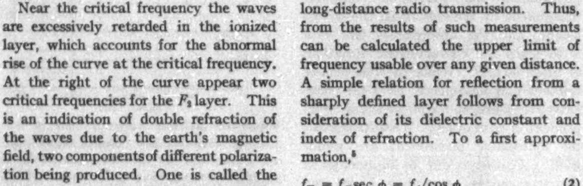
[3] Page 571 of article, "A Test of the Existence of the Conducting Layer," G. Breit and M. A. Tuve, *Physical Review*, volume 28, 1926, page 554.

[4] Relation evolved through the work of Eccles (1912); Larmor (1924); Breit and Tuve (1926); Appleton (1931); Gilliland, Kenrick, and Norton (1931); and given as equation 5a of "Studies of the Ionosphere and Their Application to Radio Transmission," S. S. Kirby, L. V. Berkner, D. M. Stuart, Bureau of Standards *Journal of Research*, volume 12, 1934, page 15 (research paper 632). A summary of the scientific aspects of the ionosphere is given in "The Physics of the Ionosphere," H. R. Mimno, *Reviews of Modern Physics*, volume 9, 1937, page 1; this article also presents a valuable bibliography, comprising 309 references (the literature is so extensive that even this list is far from complete).

Near the critical frequency the waves are excessively retarded in the ionized layer, which accounts for the abnormal rise of the curve at the critical frequency. At the right of the curve appear two critical frequencies for the F_2 layer. This is an indication of double refraction of the waves due to the earth's magnetic field, two components of different polarization being produced. One is called the ordinary ray and the other the extraordinary ray. The symbols *o* and *x* are used for these, respectively. The critical frequency of a layer *n* is represented by the symbol f_n, and to such symbol the *o* or *x* is added as a superscript. Thus the critical frequencies of the F_2 layer for the ordinary and extraordinary rays are indicated by the respective symbols, $f_{F_2}^o$ and $f_{F_2}^x$. In the case of the *E* layer, usually the ordinary ray predominates and the extraordinary ray is so weak it does not affect radio reception.

Automatic recording equipment is used by the National Bureau of Standards and a few other laboratories to record the virtual heights and critical frequencies of the ionosphere layers. A sample record is shown in figure 4. In this case (afternoon in May 1933) the *E*-layer critical frequency was 2,900 kilocycles per second and the F_1-layer critical frequency was 3,800 kilocycles per second. The dark curved line in the upper right corner of the figure of the same shape as the line below it, is a multiple reflection, that is, the wave was not only reflected back from the F_2 layer, causing the lower line, but was then reflected back up from the earth's surface and down again a second time from the F_2 layer.

Figure 3. Relation between observed virtual height and frequency of radio waves



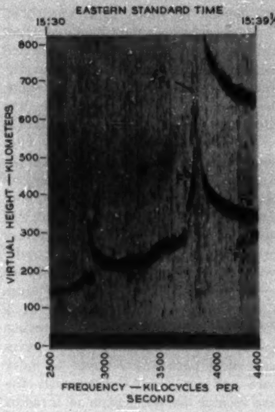


Figure 4. Example of frequency-height record as recorded by automatic multifrequency recorder of ionosphere echoes

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Measurements of the type just described are usually made at vertical incidence, that is, the waves are transmitted straight up to the ionosphere and received at a place not far from the transmitter. Strange as it may seem, such measurements tell us a great deal about long-distance radio transmission. Thus, from the results of such measurements can be calculated the upper limit of frequency usable over any given distance. A simple relation for reflection from a sharply defined layer follows from consideration of its dielectric constant and index of refraction. To a first approximation,[5]

$$f_m = f_c \sec \phi = f_c/\cos \phi \qquad (2)$$

where f_m is the maximum usable frequency over any distance, f_c is the critical frequency measured as above described at vertical incidence, and ϕ is the angle of incidence of the waves at the ionosphere, that is, the angle between the wave's line of travel *a* and the vertical line giving the virtual height *h* in figure 5. This is only a first approximation, as we shall see later, but it is close enough to permit estimating certain important effects and results. From figure 5, $\sec \phi = a/h$, *h* being the virtual height of the ionized layer. Also,

$$a = \sqrt{\frac{d^2}{4} + h^2}$$

d being the horizontal distance of transmission. Therefore,

$$f_m = f_c\sqrt{\frac{d^2}{4h^2} + 1} \qquad (3)$$

This is the approximate relation between the maximum usable frequency at a distance *d* between transmitter and receiver, and the two important characteristics of the ionosphere layer, the virtual height *h* and critical frequency f_c.

Skip Distance

An interesting consequence of this relation is: the distance at which a given frequency is the maximum usable is also the minimum distance over which that frequency is receivable, that is, there is a zone around the transmitter in which the frequency is not receivable but outside of which it is receivable. Thus figure 6 shows the calculated upper limit of frequency of waves reflected back by the ionosphere for various distances, for a particular time (midnight, December 1937, *F* layer). This was calculated by the simple formula (3) with the virtual height h = 310 kilometers and f_c = 4,180 kilocycles per second. The maximum frequency receivable at a distance of, say, 1,200 kilometers is 8,300 kilocycles per second, and this is higher than that receivable at any shorter distance. This means that radio waves of a frequency of 8,300 kilocycles per second are reflected by the *F* layer of the ionosphere and receivable at a distance of 1,200 kilometers or more, but at any shorter distance such waves pass on through the *F* layer to outer space and are lost. Consequently the zone around the transmitter of a radius of 1,200 kilometers is a skipped zone for the frequency of 8,300 kilocycles per second, and the distance of 1,200 kilometers is called the skip distance for that frequency at that time. There is good reception beyond the skip distance and none within it. This is subject to the qualification that there is a short distance close to the station in which reception by the ground wave is possible; this is of no interest in long-distance reception and is outside the scope of this paper. There is also some reception, usually weak and unsatisfactory, within the skipped zone due to scattered reflection of waves from irregular ionized patches in the ionosphere (see section IV hereinafter).

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[5] Relation following directly from Snell's law of refraction and Eccles-Larmor equations for index of refraction in an ionized medium. Derived by my colleagues in National Bureau of Standards in 1934; published in "Multifrequency Ionosphere Recording and Its Significance," by T. R. Gilliland, National Bureau of Standards *Journal of Research*, volume 14, 1935, page 283 (research paper 768); in document "Study of Question 7 Proposed for the Fourth Meeting of the C.C.I.R., Report Submitted by U.S.A.," 1936; and elsewhere, for example, in "Maximum Usable Frequencies for Radio Sky-Wave Transmission, 1933 to 1937," by T. R. Gilliland, S. S. Kirby, N. Smith, S. E. Reymer, National Bureau of Standards *Journal of Research*, volume 20, 1938, page 627 (research paper 1,096).

The reality of the skip distance is quite striking. It frequently happens that, in order to get radio messages through on a certain high frequency over a short distance, two stations not far apart communicate with one another by relaying their messages through a third station a great distance from both. For example, two stations in Pennsylvania 100 kilometers apart may be unable to send messages directly to each other on 15,000 kilocycles per second but both may easily communicate with a station in South America and relay messages through it to each other.

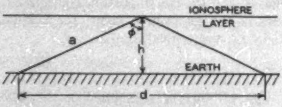

Figure 5. Simplified geometry of angle of incidence of radio waves on ionosphere layer

II. Regular Characteristics of the Ionosphere

Since the upper limits of frequency usable for radio transmission over a given distance depend directly upon the virtual heights and critical frequencies of the layers of the ionosphere, and since these are constantly varying with time of day, season, and other factors, it is particularly important to have definite data on the variations of these two ionosphere characteristics. Radio transmission conditions in general follow the critical frequencies; for example, when the critical frequencies are high the maximum usable frequencies and the best frequencies for radio communication are also high. In view of the known relations between radio transmission and ionosphere data, the facts of radio transmission can indeed be summarized in far more compact form in terms of ionosphere data than in terms of direct radio data.

A survey of the factual data of the ionosphere is in some respects like a survey of weather conditions. Like the predominant roles played in weather phenomena by atmospheric pressure, temperature, and humidity, we have the predominant roles played in ionosphere phenomena by the ionization, the virtual height, and the absorption. Our task is to learn the characteristic variations of these factors. While the two realms, weather and ionosphere, have this similarity, they are, so far as we know, independent of each other. Weather is local; the ionosphere phenomena are worldwide. Weather is due to happenings in the troposphere, extending about ten kilometers above the earth's surface, while the ionosphere phenomena occur at heights many times this. The seasonal effects in the ionosphere are synchronous with the sun's position, not lagging a month or two as do the seasons of weather. Ionosphere phenomena exhibit a greater regularity than weather phenomena and are in some respects better understood.

A large body of facts regarding the virtual heights and critical frequencies of the ionosphere layers is now available. Much less is known about the absorption. All three vary from hour to hour, from season to season, and from year to year. Each of these variations would be expected because of their dependence upon radiation from the sun. The ultraviolet radiations from the sun vary in an 11-year cycle. The last minimum was in 1933, and there were maxima in 1927 and 1938. The ionosphere was under study throughout this cycle, and we have consistent data particularly for the last six years. The present is a good time to summarize the facts of the ionosphere, for it is only now that we have come into comprehensive possession of such facts throughout the entire significant period of half a solar cycle.

The only place where the ionosphere is known to have been continuously under observation for this period is the National Bureau of Standards at Washington. The measuring technique[6] involves oscillographic observation of radio echoes of a sharp pulse. Similar work has been done from time to time by numerous observers and laboratories, and continuous observations are now in progress in many countries. Knowledge of the behavior of the ionosphere is coming to be so important in the operation of radio services that the National Bureau of Standards broadcasts information on the ionosphere characteristics and vagaries from its radio station WWV one day each week. These regular broadcasts were begun in 1937. It may be necessary in the future to have such service daily from a number of places, like the weather-forecasting service.

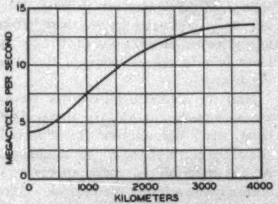

Figure 6. Relation between limit of frequency receivable and distance of transmission, calculated by simplified formula for a particular time (midnight, December 1937, F layer)

Virtual Heights and Critical Frequencies

A satisfactory way in which to present and use the data on ionosphere layer heights and critical frequencies is by means of graphs of monthly averages of these quantities as a function of time of day. The monthly average is significant because the variations from day to day are small, usually less than 15 per cent, except for disturbed days, which constitute a separate subject, treated in section IV. Typical examples of the monthly average graph are given in figure 7, showing summer and winter conditions. Similar curves have been published[7] by the National Bureau of Standards for every month from May 1933 to the present, and continue to be published in the *Proceedings* of the Institute of Radio Engineers each month. Such data are also published[8] in tabular form every quarter.

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[6] "A Test of the Existence of the Conducting Layer," G. Breit and M. A. Tuve, *Physical Review*, volume 28, 1926, page 554.
"Preliminary Note on an Automatic Recorder Giving a Continuous Height Record of the Kennelly-Heaviside Layer," T. R. Gilliland and G. W. Kenrick, Bureau of Standards *Journal of Research*, volume 7, page 783, 1931 (research paper 373); IRE *Proceedings*, volume 20, 1932, page 540.
"Note on a Multifrequency Automatic Recorder of Ionosphere Heights," T. R. Gilliland, Bureau of Standards *Journal of Research*, volume 11, page 561, 1933 (research paper 608); IRE *Proceedings*, volume 22, 1934, page 236.
"Studies of the Ionosphere and Their Application to Radio Transmission," S. S. Kirby, L. V. Berkner, and D. M. Stuart, Bureau of Standards *Journal of Research*, volume 12, 1934, page 15 (research paper 632); IRE *Proceedings*, volume 22, 1934, page 481.

The virtual heights shown in these graphs are the heights for the lowest frequencies used in the determinations. There is a slight variation of height with frequency, as suggested in figure 3, and as discussed in section III hereinafter, but the variation is small and can be neglected for many purposes. The vertical dashed lines on the graphs are for the times of sunrise and sunset at the ground, not the earlier sunrise time and later sunset time at those heights in the atmosphere where the ionosphere layers are located. While this is surprising at first sight, the ground times of sunrise and sunset are the logical ones to consider, because the sunlight arriving tangentially to the earth's surface cannot reach any atmospheric level above the dark hemisphere without having first traveled down through that and lower levels above the lighted hemisphere, and the ionizing radiation would be largely absorbed in those lower levels. Doubtless some of the ionizing radiation does reach the ionosphere somewhat before ground sunrise and after ground sunset, but it is by scattering, diffusion, or other special process.

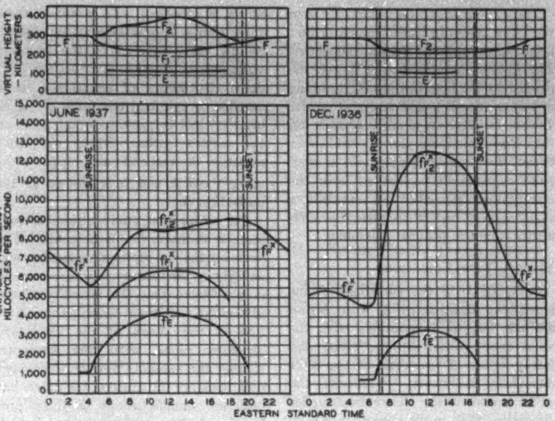

Figure 7. Typical summer and winter monthly averages of the diurnal variation of virtual heights and critical frequencies

The abscissas in figure 7 are time of day (Eastern standard time), on a 24-hour basis, for example, 0 = midnight, 14 = 2 p.m., etc. The following symbols are used:

f_E = critical frequency of the E layer. This is the critical frequency of the ordinary ray unless otherwise specified. When it is desired to specifically indicate it as that of the ordinary ray, the symbol is f_E^o.

$f_{F_1}^x$ = critical frequency of the F_1 layer. extraordinary ray.

$f_{F_2}^x$ = critical frequency of the F_2 layer, extraordinary ray. The night portions of the $f_{F_2}^x$ curve are for the F layer. The symbols for F and F_2 at night are used interchangeably.

Variations With Time of Day and Season

No regular diurnal or seasonal variation is observed in the virtual height of the E layer, which appears to be almost always between 110 and 120 kilometers. The critical frequency of the E layer has however a regular diurnal and seasonal variation. It varies synchronously with the altitude of the sun, reaching the diurnal maximum at noon, and having a higher diurnal maximum in the summer than in the winter. This behavior accords with the simple theory of ionization by ultraviolet radiation from the sun, according to which, assuming recombination of ions to be sufficiently rapid,[9]

$$f_E = K\sqrt[4]{\cos\psi} \qquad (4)$$

where ψ is the zenith angle of the sun, and K is a factor depending on the intensity of the solar radiation. This holds within fairly close limits for values of ψ not too close to 90 degrees, during the daylight hours of any one day.

No such simple relation characterizes the variation of the critical frequencies of the F, F_1, and F_2 layers. As may be seen from figure 7, the daytime critical frequencies of the F_2 layer are much higher in the winter than in the summer (although reports from the southern hemisphere indicate that the reverse is true there), and the night critical frequencies are somewhat lower in winter than in summer. The diurnal maximum of the F_2 critical frequency almost always occurs after noon, and is much later in the day in summer than in winter. The post-sunset drop in the night F critical frequency is much more rapid in the winter than in the summer; indeed in summer the midnight F critical frequency is sometimes as high as the noon F_2 critical frequency. The outstanding characteristics of the F_2 critical frequency are (1) a seasonal variation inverse to that of the E critical frequency, and (2) a diurnal lagging behind the altitude of the sun. The F and F_2 maximum critical frequency occurs in the winter day, and the minimum in the winter night. The day and night summer values are between these extremes.

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[7] "Multifrequency Ionosphere Recording and Its Significance," T. R. Gilliland, Bureau of Standards *Journal of Research*, volume 14, 1935, page 283 (research paper 760); IRE *Proceedings*, volume 23, 1935, page 1076.
"Characteristics of the Ionosphere and Their Application to Radio Transmission," T. R. Gilliland, S. S. Kirby, S. E. Reymer, and N. Smith, Bureau of Standards *Journal of Research*, volume 18, 1937, page 645 (research paper 1001); IRE *Proceedings*, volume 25, 1937, page 823.
"Characteristics of the Ionosphere at Washington, D. C., January to May, 1937," T. R. Gilliland, S. S. Kirby, N. Smith, and S. E. Reymer, IRE *Proceedings*, volume 25, 1937, page 1174.
"Characteristics of the Ionosphere at Washington, D. C., ...," published each month in IRE *Proceedings* for the second month before, starting with the September 1937 issue.

[8] "Averages of Critical Frequencies and Virtual Heights of the Ionosphere Observed by the National Bureau of Standards, Washington, D. C., 1934–36," T. R. Gilliland, S. S. Kirby, N. Smith and S. E. Reymer, *Terrestrial Magnetism and Atmospheric Electricity*, volume 41, 1936, page 379. Similar summaries in each succeeding quarterly issue.

[9] Relation given on page 251 of "Radio Observations of the Bureau of Standards During the Solar Eclipse of August 31, 1932," S. S. Kirby, L. V. Berkner, T. R. Gilliland, K. A. Norton, IRE *Proceedings*, volume 22, 1934, page 247.

The F_2 layer also differs markedly from the other layers in having much greater height in the summer than in the winter. The F_2 layer acts as though it were expanded by the heat in summer, resulting in higher virtual height and also greater volume for a given number of ions and thus lower ionization density and lower critical frequency. This is not a complete explanation, as the F_2 layer has many other anomalies, for example, a slight drop of critical frequency in midwinter from the seasonal maximum.

Year-to-Year Variations

Superposed on the variations of the ionosphere characteristics with season are the long-time variations with the solar cycle, caused by the rising and falling in an 11-year cycle of the sun's ultraviolet radiations. Practically the entire period of the observations under discussion, 1933 to the present, has been one of increasing solar radiation, as we are now at about the top of the cycle. There has consequently been an increase from year to year in the ionization and the critical frequency of all the layers. The trend reversed in 1938 and the critical frequencies will in general decrease until about 1944.

The heights of the various layers do not change appreciably from year to year. The chief change in respect to them is the disappearance of daytime *F*-layer stratification in the winter as the solar cycle advances. Thus, figure 7 shows no F_1 layer in December 1936, whereas in 1933 to 1935 there was a stratification into F_1 and F_2 layers in winter as well as in the summer although the stratification was much less marked in the winter than the summer.

Both seasonal and year-to-year changes of the *E*-layer critical frequency are shown in figure 8, curve *b*. This gives the average f_E at noon for each month. The full-line curve *b* shows the rise each summer and drop each winter, the climb to higher values each year, and also some minor fluctuations. To examine these three things separately, it is possible to redraw the curve with the seasonal variations eliminated, and this has been done. By means of equation 4, it is possible to calculate the value of the *E*-layer critical frequency at that point on the earth's surface where the sun's radiation is perpendicular; this is K, or $f_E/\sqrt[4]{\cos\psi}$, and it is plotted as the dashed curve.

For comparison, curve *a* gives average sunspot numbers for each month. The spots on the sun increase in number and activity as the solar cycle progresses, and it is believed that the intensity of the ultraviolet radiations causing the ionization of the layers of the ionosphere increases along with the sunspots. Neither the sunspots nor the ultraviolet radiations are the cause of the other, but both are manifestations of those profound activities within the sun which undergo the 11-year variation. It is not possible to compare directly against measured ultraviolet radiation from the sun because all the radiations which produce the ionosphere phenomena are absorbed in the ionosphere and do not reach the surface of the earth. The comparison with sunspot numbers is merely a comparison with a crude index of solar activity, but it is of some value.

The detailed correspondence of the dashed curve *b* with sunspot numbers is in fact impressive. It shows not only that the *E*-layer ionization follows the general trend of the sunspot numbers, but that the detailed changes from month to month show a fair amount of agreement. There is also fair week-to-week but not day-to-day correlation.

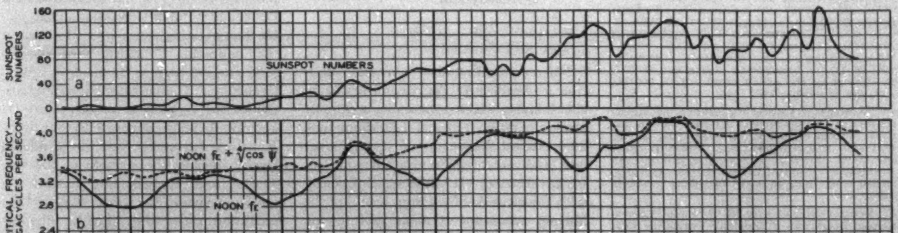
The F_2 layer presents a more complicated picture, since purely terrestrial influences, such as the temperature of the layer, play an important part. No such simple method therefore exists for eliminating diurnal and seasonal variations as may be done for the *E* layer. Figure 9 shows the variation, by average for each month, of the F_2 and *F* critical frequency for various parts of the day. The "midmorning" curve is for the time halfway between midnight and sunrise, and the "diurnal minimum" curve is for the time when the *F* critical frequency reaches its lowest value, about 30 minutes before ground sunrise. The midforenoon, midafternoon, and midevening curves are for times halfway between sunrise and noon, noon and sunset, and sunset and midnight, respectively. All of these curves show the general rise in critical frequencies superposed on the seasonal variation. The amplitude of the seasonal variation, as well as the average value, has risen each year, and is thus greater for greater solar activity. The high daytime values in winter, and high night values in summer, stand out. The midwinter dip in the daytime curves should also be noted. The diurnal minimum curve shows the least seasonal variation; this might be more or less expected, since the influence of the sun is least at this time.

Twelve-month running averages, which eliminate the seasonal variations, are given as dotted lines for the noon and the diurnal minimum values. They show more clearly the trend from year to year, and also make it easy to see the amplitude of the seasonal variations. It is interesting to compare them with the curve of sunspot numbers in figure 8.

The long-time trend of the ionosphere critical frequencies is shown more clearly in figure 10. The general trend parallel to that of the sunspot numbers is striking. All had their minimum in 1933 and maxi-

Figure 8. Variations of monthly average sunspot numbers and critical frequency of the E layer, 1933 to 1938

The solid curve (*b*) is the *E*-layer critical frequency as observed at Washington, and the dashed curve is the calculated *E*-layer critical frequency at the point where the sun's radiation is perpendicular



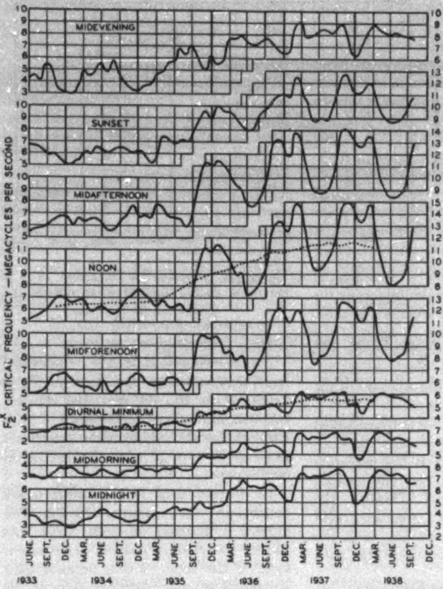


Figure 9. Variations of monthly average F_2 and F critical frequency, 1933 to 1938

mum in 1937. Again we see evidence that the sunspot numbers and the ultraviolet radiations which ionize the ionosphere have a close interrelation. The situation revealed in figure 10 is one of the most interesting of all the results yielded by radio-wave-transmission research.

Variations With Longitude and Latitude

The data presented in this paper are mostly those obtained in and near Washington, D. C., where the latitude is 39 degrees north. It is important to inquire how representative these are: are the ionosphere characteristics the same at other longitudes and latitudes? There is no a priori reason to expect any variation with longitude other than the large variations characteristic of the local time of day prevailing simultaneously at different longitudes. Ionosphere data from different longitudes in the same general range of latitude are limited. However, some information has been published in the past year, particularly from Japan,[10] and no material differences from the Washington data are found.

Latitude is quite another matter. It is to be expected that the varying angle of incidence of the solar radiations with latitude would lead to greater ionization in equatorial than polar regions. Data are limited; the available information[11] indicates that changes with latitude, other than those occasioned by the difference of season as the equator is approached or crossed, are not great. Measurements at latitude 12 degrees south indicate that the annual average critical frequencies (and thus the ionizations) are somewhat higher, and the virtual heights somewhat lower, than at Washington, 39 degrees north. There is relatively little seasonal variation near the equator. Measurements at latitude 70 degrees north, on the other hand, show lower average critical frequencies, and greater variations with season, than at Washington.

The variation with latitude is bound up with another effect, the occurrence of ionosphere disturbances, different types of which produce effects which are distributed differently in latitude. In particular, disturbances called "ionosphere storms," lasting a day or more and causing great changes in the F_2 layer height and ionization, are more intense and more frequent as the magnetic pole is approached. It is therefore important to separate days of this type from the undisturbed days, particularly when considering data from higher latitudes. This is discussed further in section IV.

III. Normal Radio Transmission

As explained in section I, some of the important facts of radio wave transmission over great distances are determinable from the characteristics of the ionosphere. We shall now inquire more carefully into the way in which this is done, and explain the application of these facts to practical radio-communication problems.

Relation of Oblique to Vertical Incidence

To compute long-distance radio data from ionosphere data is essentially to determine the relation between radio transmission incident obliquely on an ionosphere layer and transmission incident vertically on the layer. This is because ionosphere data are an expression of, and are determined by, radio transmission incident vertically on the ionosphere. When radio waves are transmitted over some horizontal distance, they are sent obliquely into the ionized layer, and as we have seen the ionized

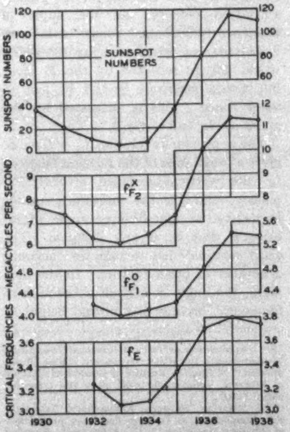


Figure 10. Comparison of annual averages of critical frequencies and sunspot numbers

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[10] "On the Long-Period Variations in the F_2 Region of the Ionosphere," K. Tani, Y. Ito, H. Sinkawa, *Report of Radio Research in Japan*, volume 7, 1937, page 91; also IRE *Proceedings*, volume 26, 1938, page 1340.
"Annual Variations in Upper-Atmospheric Ionization," K. Maeda, T. Tukada, T. Kamoshida, *Report of Radio Research in Japan*, volume 7, 1937, page 109.

[11] "Annual Variation of the Critical Frequencies of the Ionized Layers at Tromso During 1937," L. Harang, *Terrestrial Magnetism and Atmospheric Electricity*, volume 43, 1938, page 41.
"The Ionosphere at Huancayo, Peru, November and December 1937," H. W. Wells and H. E. Stanton, *Terrestrial Magnetism and Atmospheric Electricity*, volume 43, 1938, page 169.

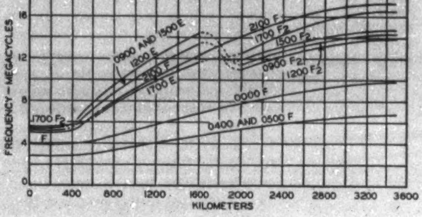

Figure 11. Average for June 1933 of maximum usable frequency at various distances and times of day

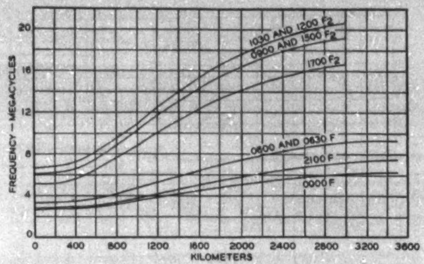

Figure 12. Average for December 1933 of maximum usable frequency at various distances and times of day

medium is able to reflect such waves of frequencies considerably in excess of the vertical-incidence critical frequency. As indicated by the approximate equation 2, $f_m = f_c \sec \phi$, a layer of given ionization density returns waves of frequencies which are higher, the greater the angle of incidence upon the layer. Thus, except as limited by the curvature of the earth, higher frequencies can be reflected the greater the distance of transmission and the lower the height of the reflecting layer.

In figure 1, for example, the *E* layer is shown reflecting a ray (1). Supposing the frequency to be the highest frequency which can be reflected by the *E* layer at that angle of incidence, then the ray (2) with a slightly smaller angle of incidence would not be reflected by the *E* layer but would penetrate it and go on up to the F_1 or F_2 layer and be reflected by one of them because of its greater ionization density. As the angle of incidence is decreased (angle of take-off increased), we arrive at a ray (3) which would not be reflected by the F_2 layer but would penetrate it and be lost to outer space. This is suggested by the arrow at the end of ray 3.

The approximate relation, $f_m = f_c \sec \phi$, gives a rough idea of the relation between f_c, the vertical-incidence critical frequency, and f_m, the maximum usable frequency, at any oblique angle of incidence, that is, at any distance. The exact relation[12] has to take into account a number of other factors. In the first place, the angle ϕ is itself a function of frequency, because the virtual height of the reflecting layer is a function of frequency. The higher the frequency the farther into the ionized layer do the radio waves penetrate. Thus, in figure 3 the virtual height rises slightly with frequency; this figure is for vertical incidence, and a similar rise appears in the relation for oblique incidence.

Secondly, the simple relation is modified because the earth's surface and the ionosphere layers from which the reflection takes place are not plane but curved, altering the geometry. Thirdly, the presence of the earth's magnetic field alters the relation, depending upon the length, direction, and location of the path of transmission.

All of these effects together make the calculation of maximum usable frequencies somewhat complicated. The National Bureau of Standards therefore publishes[13] curves of maximum usable frequencies as well as of critical frequencies in its monthly and other publications on the ionosphere. Examples are given in figures 11 to 14. Comparison of figure 14 with figure 6 shows how the simple secant relation or its equivalent, equation 1, is inadequate to represent the actual facts of radio transmission.

MAXIMUM USABLE FREQUENCIES AND SKIP DISTANCES

Figures 11 to 14 give the averages (for the days free from ionosphere storms) for the months shown, of the upper limits of usable frequencies, as a function of distance, for various times of day. The characteristic differences between summer and winter are shown, and also the striking increase of frequency with the advance of the cycle of solar activity during 1933 to 1937. This is what would be expected from the ionosphere changes depicted in figures 8 to 10. In fact, the diurnal, seasonal, and other effects shown in these curves follow directly from the ionosphere data of the type presented in section II of this paper.

In figures 11 to 14, the layer which determines the maximum usable frequency is marked on each curve. As shown, the *F* and F_2 layers determine it most of the time; this is because their ionization so much exceeds that of the lower layers. In the daytime in summer, however, the *E*-layer ionization is so great, and beyond a distance of about 500 kilometers the lower height of the *E* layer results in so much more oblique transmission, that the *E* layer is controlling. The curvature of the earth and of the *E* layer limits the reflection distance to about 1,700 kilometers; beyond that the F_2 layer is controlling as the curves show. The F_1 layer is seldom (summer day, for 2,000 to 3,000 kilometers) enough lower than the F_2 layer, and enough more ionized than the *E* layer, to determine the maximum usable frequency.

There are times when transmission does take place at frequencies higher than these critical frequencies, either because of "sporadic *E*" layer or scattered reflections. These special phenomena are discussed in section IV.

Each point on these curves not only gives the upper limit of frequency which is usable over the distance, but also conversely gives the lower limit of distance over which the frequency gives satisfactory sky-wave transmission. The curves thus serve the additional purpose of giving data on skip distance. Where skip distances are of primary interest, the relation between frequency, distance, and time of day may be more conveniently shown by curves of skip distance as a function of time of day, for various frequencies. Such a curve is shown in figure 15; it is for June 1937, and thus gives the same data as figure 13. Curves of skip distance against time, as in figure 15, are useful in selecting frequencies for a mobile radio station, such as an airplane or a ship, where the distance of transmission continually varies and it is necessary to determine a time and distance at which to change from one frequency to another.

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[12] "Application of Vertical-Incidence Ionosphere Measurements to Oblique-Incidence Radio Transmission," N. Smith, Bureau of Standards *Journal of Research*, volume 20, 1938, page 683 (research paper 1100).
"The Relation Between Atmospheric Transmission Phenomena at Oblique Incidence and Those at Vertical Incidence," G. Millington, Physical Society (Great Britain) *Proceedings*, volume 50, 1938, page 801.

[13] "Maximum Usable Frequencies for Radio Sky-Wave Transmission, 1933 to 1937," T. R. Gilliland, S. S. Kirby, N. Smith, and S. E. Reymer, Bureau of Standards *Journal of Research*, volume 20, 1938, page 627 (research paper 1096); IRE *Proceedings*, volume 26, 1938, page 1347.
"Characteristics of the Ionosphere at Washington, D. C.,....," published each month in IRE *Proceedings* for the second month before, starting with the September 1937 issue.

Another useful form in which the data may be put is a set of curves of frequency as a function of time of day, for various distances. The data for June 1937 are shown in this form in figure 16. The curve for zero distance is identical with the curve of vertical-incidence critical frequency. Curves of this type are given in the monthly ionosphere reports of the National Bureau of Standards.[13] Such curves are useful in the operations of radio stations communicating between fixed points, as they are an aid in selecting the frequencies to be used during various times of day, for communication over a given distance.

Since all the curves change with time of day, and since on any long radio transmission path (except north-south) the time of day is different at all parts of the path, it is important to select the proper time of day in using these curves. The proper time is that of the locality where the waves reach, and are reflected by, the ionosphere. For paths in which the waves go from transmitter to receiver by a single reflection from the ionosphere, the place of reflection is halfway between transmitter and receiver.

Transmission by Multiple Reflections

It may be noted that figures 11 to 16 give no distances greater than 3,500 kilometers. The maximum possible distance of transmission by a single hop, that is, reflection from any ionosphere layer, is limited by the geometry of the earth's surface and the layers, and also by absorption at the ground of those waves which are nearly tangential. It is found in practice that the minimum angle with the ground of the radio waves transmitted or received averages about $3^{1}/_{2}$ degrees over land. From the geometry it results that the maximum distance along the earth by a single hop is ordinarily about 3,500 kilometers for the F_2 layer, and about 1,700 for the *E* layer. These may be exceeded in particular cases.

While the curves are drawn for single-hop transmission, they are nevertheless available for solving problems of multihop transmission, that is, multiple reflections from the ionosphere, with intermediate reflections from the ground. Calculation of the maximum usable frequency for multihop transmission is necessarily somewhat complicated. In the first place, we have to consider the time of day of the locality where each reflection from the ionosphere layer takes place. To a first approximation, the maximum usable frequency is the lowest one of the several corresponding to the times of day at the several localities where reflection takes place, for the distance on the curves equal to the transmission distance divided by the number of hops.

For very long paths, if very widely different latitudes are involved, it may be desirable to use curves appropriate to the latitude of each hop. As mentioned in section II, changes of ionosphere characteristics with latitude are not great, and it follows that the effect of latitude is of minor importance in radio transmission, except for extreme differences of latitude. In particular, for transmission within an area the size of the United States, or Europe, differences of latitude do not need to be taken into account. In transmission between the United States and Europe, however, although the difference of latitude may be no greater than within either area, the ionosphere characteristics vary materially along the transmission path from another cause. This is the effect of propinquity to the north magnetic pole. Certain ionosphere disturbances, as mentioned in section II, are more intense and more frequent as the magnetic pole is approached. There may also be a steady or permanent set of ionosphere effects having a maximum at the magnetic pole. See in this connection section IV.

For very long paths in which widely different longitudes (that is, times of day) are involved, it sometimes happens that the waves travel different parts of the way by different layers. For such cases, it is necessary to take account of the

Figure 13 (below). Average for June 1937 of maximum usable frequency at various distances and times of day

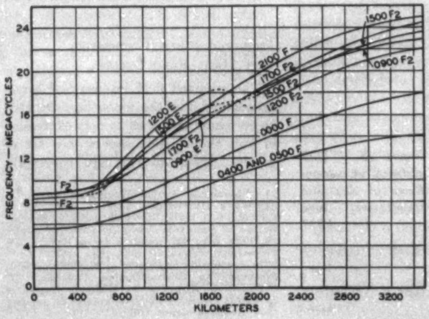
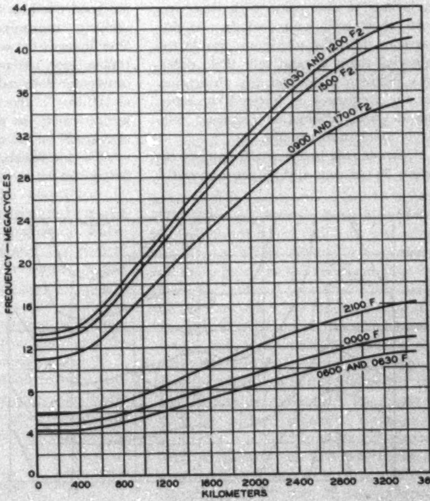


Figure 14. Average for December 1937 of maximum usable frequency at various distances and times of day



heights of the different layers to determine the lengths of the several hops, and also to employ separate curves of maximum usable frequency for each layer.

The waves reaching a given point may be a combination of waves having traveled by different numbers of hops. For each of these it is necessary to take account of the time of day, and to consider which layer is effective, for the locality where each reflection from the ionosphere occurs.

OPTIMUM FREQUENCIES

It is necessary to inquire whether the maximum usable frequency is the optimum frequency. It is found in practice that in general (especially in the daytime) the absorption is greater, that is, received intensities are less, as the frequency is lowered below the maximum usable frequencies. Thus it requires much greater power to get communication through on frequencies very much below the maximum usable. On the other hand, a frequency should be chosen somewhat below the values indicated in the curves of monthly averages because of the variability from day to day, which is generally within 15 per cent.

Fair efficiency of communication is usually provided in the daytime by frequencies down to about 50 per cent of the maximum usable frequencies, and at night by frequencies down to somewhat less than 50 per cent of the maximum usable frequencies. Definite limits cannot be set because there are large irregular variations of absorption with time over both short and long periods. At frequencies near the maximum usable frequencies there is relatively little difference between night and day absorption. As the frequency is lowered, however, the daytime absorption increases relatively much more rapidly.

A simple rule is to use a frequency between 50 per cent and 85 per cent of the monthly average maximum usable frequency for the given distance and time. It is not ordinarily possible to keep changing frequency continuously so some such range of choice is necessary. Below 50 per cent, the received waves are likely to be too weak for use, and above 85 per cent communication will be impossible on some days.

Interesting examples of the calculation of frequencies to be used for practical radio transmission, from the graphs of maximum usable frequency, have been published[14] recently.

Experimental confirmation of the validity of these calculations and these considerations of the relations between ionosphere characteristics and radio transmission conditions is given by extensive experience of radio stations and also by special experiments. The National Bureau of Standards continuously records the intensity of radio waves received from a number of stations at various frequencies and distances, in order to investigate this.

An example of the type of evidence given by these recordings is given in figure 17. The upper part of the figure is a continuous record of the field intensity of W6XKG, Los Angeles, Calif., on 25,950 kilocycles per second, at a distance of 3,700 kilometers. The lower part of the figure gives graphs of the calculated maximum usable frequency over this distance, for both one- and two-hop transmission. This distance is slightly beyond the ordinary maximum distance for good one-hop transmission, and the one-hop transmission is thus weak. The changes between one- and two-hop transmission are well marked; the ratio of average received intensity is as great as 100 to 1. This difference in intensity is due partly to the unfavorable angle of take-off for the single hop, and partly to the increased absorption over the flatter trajectory. It should be noted how the times of beginning and ending of two-hop transmission agree with the times the calculated maximum usable frequency passed through about 26 megacycles per second at the western and eastern hops, respectively: that is, the two-hop transmission began as soon as the western hop permitted and ended when the eastern hop failed. After the failure of two-hop transmission at 1,850 (evening), the station came in weakly by one hop. As the evening progressed (after 1,850) the intensity increased, owing to the departure of the daytime absorption and the rise in height of the layer, with a consequent more favorable angle of take-off. The failure of single-hop transmission at 2,030 is seen to agree with the time of diminution of the calculated maximum usable frequency through 26 megacycles per second. (The very low intensity after 2,030 is electrical noise, that is, atmospheric disturbances or "static.")

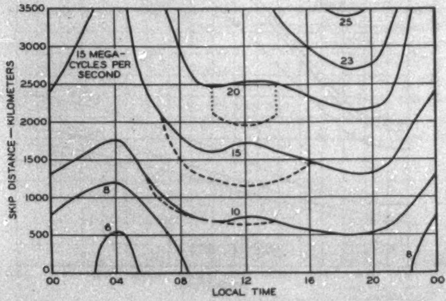


Figure 15. Skip distance at various times of day and frequencies, June 1937

Solid curves—*F* layer

Dashed curves—*E* layer

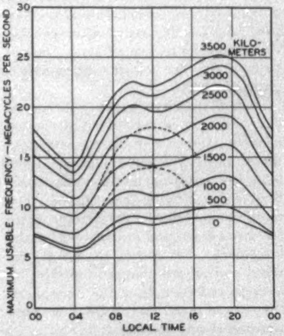


Figure 16. Maximum usable frequency at various times of day and distances, June 1937

Solid curves—*F* layer

Dashed curves—*E* layer

RECEIVED FIELD INTENSITIES AT HIGH FREQUENCIES

The received field intensity of waves propagated via the ionosphere depends on so many factors as to defy expression in any simple summary or any set of graphs. Data on this are constantly

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[14] "Maximum Usable Frequencies for Radio Sky-Wave Transmission, 1933 to 1937," T. R. Gilliland, S. S. Kirby, N. Smith, and S. E. Reymer, Bureau of Standards *Journal of Research*, volume 20, 1938, page 627 (research paper 1096); IRE *Proceedings*, volume 26, 1938, page 1347.
"The Application of Graphs of Maximum Usable Frequency to Communication Problems," N. Smith, S. S. Kirby, T. R. Gilliland, Bureau of Standards *Journal of Research*, volume 22, January 1939 (research paper 1167).

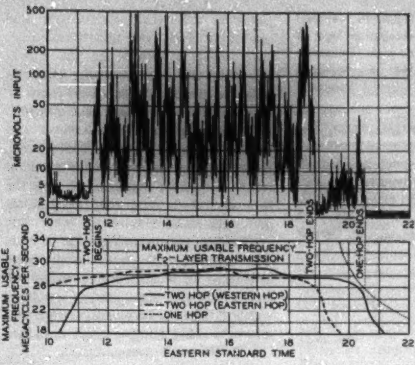

Figure 17. Example of effect upon received intensity of change of transmission from one to two hops and back to one hop

January 26, 1938; W6XKG, 25,950 kilocycles per second, Los Angeles, Calif.; 3,700 kilometers from Meadows, Md.

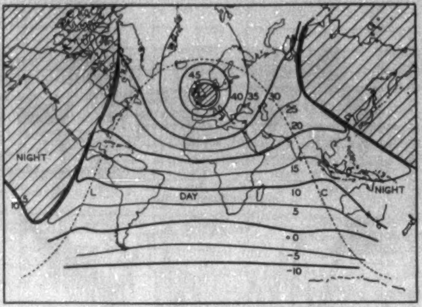

Figure 18. Sample diagram of lines of equal average received intensity for a particular frequency, time of day, season, epoch of solar cycle, and location of transmitter

The numbers are quasi-maximum field intensities expressed in decibels above one microvolt per meter, for one kilowatt radiated. The dotted line is the locus of sunrise (*L*) and sunset (*C*). The shaded areas are regions of zero reception

f = 18.8 megacycles per second (16 meters); winter, 1200 local time

being accumulated in the daily operations of radio stations and in such special recording laboratories as the National Bureau of Standards. As such data are analyzed they are found to be consistent with the ionosphere facts such as are reported in this paper, but detailed connections are limited because of the lack of direct measurements of ionosphere absorption. However, the facts of received field intensities are adding to our knowledge of the ionosphere, as just illustrated in the discussion of figure 17.

Data on the received field intensities of waves propagated via the ionosphere, as obtained in connection with the operations of radio stations, have been published in numerous articles.[15] Since sky waves exhibit great fading, the intensities constantly fluctuating over a great range, it is necessary to deal with averages. A convenient form of average for this purpose is the quasi-maximum, which is the value exceeded by the instantaneous value of the field intensity five per cent of the time.

An attempt was made to summarize a fairly large body of such information in the 1937 London Report of Committee on Radio Wave Propagation.[16] In a series of 18 figures, it gives contour lines of field intensity on a world map for two frequencies (8,600 and 18,800 kilocycles per second), for three times of day, for one epoch of the solar cycle, 1929–32, and for the transmitter (or receiver) located at London. For other locations of transmitter (or receiver) the data would be different, both because of different latitude and different distance from magnetic pole. The data given do not apply to times of ionosphere storms or other vagaries. One of these figures is reproduced here as figure 18. It gives received intensities for one kilowatt radiated from the transmitter. The curves are averages of values varying over a wide range. No set of graphs or tables would be adequate to give the facts of transmission for all frequencies, times, and other conditions.

Received intensities are largely dependent on the absorption of the wave energy in the ionized parts of the atmosphere below the ionized layer which reflects the waves. The absorption in general increases as frequency is decreased below the maximum frequency transmissible via a given layer. The absorption determines the minimum usable frequency and the maximum distance of communication, just as the ionization density or critical frequency determines the maximum usable frequency and the minimum distance (skip distance).

The absorption is found to vary with time of day, season, frequency, and length of path. It is usually greater during the day than during the night. It is greater during the summer day than during the winter day. A given frequency at large angles of incidence (long distance) behaves, with respect to absorption, like a much lower frequency at small angles of incidence (short distance).

Definite information about absorption and other characteristics of transmission via the different layers is given by continuous graphical records of received intensities. To illustrate the type of information given by many thousands of such graphs which have been recorded, two examples are given in figure 19. The top pair of graphs shows morning and evening intensities of reception from a station on 6,060 kilocycles per second at a distance of 648 kilometers. Shortly before 0700 (morning) there is a sharp increase in the general level of received intensity, and shortly before 1800 (evening) there is a sharp decrease and change in character of the received intensity. These are the times at which the radio transmission changed from *F* to *E* layer and vice versa. It follows that at these times the maximum usable frequency via the *E* layer for a distance of 648 kilometers was 6,060 kilocycles per second. The vertical-incidence critical frequency corresponding to this is 2,450 kilocycles per second. Determination of the times of change of layer from such records gives a means of determination of the vertical-incidence

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[15] A few examples are:
"Some Measurements of Short-Wave Transmission," R. A. Heising, J. C. Schelleng, G. C. Southworth, IRE *Proceedings*, volume 14, 1926, page 613.
"Short-Wave Wireless Telegraphy," T. L. Eckersley, IEE (London) *Journal*, volume 65, 1927, page 600.
"The Propagation of Short Radio Waves Over the North Atlantic," C. R. Burrows, IRE *Proceedings*, volume 19, 1931, page 1634.
"Attenuation of Overland Radio Transmission in the Frequency Range 1.5 to 3.5 Megacycles per Second," C. N. Anderson, IRE *Proceedings*, volume 21, 1933, page 1447.

[16] "Report of Committee on Radio Wave Propagation," IRE *Proceedings*, volume 26, 1938 page 1193.

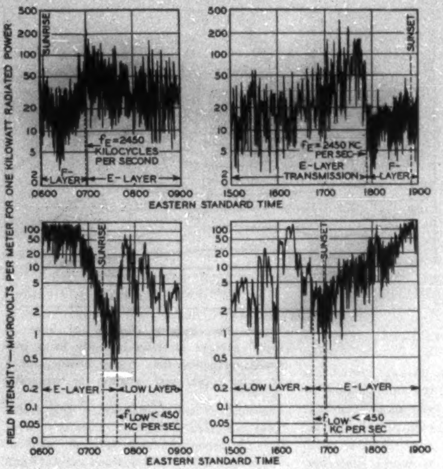


Figure 19. Typical changes of layer at a high frequency and a broadcast frequency

Top — W8XAL, Mason, Ohio; frequency 6,060 kilocycles per second, distance 648 kilometers; April 7, 1936

Bottom — WTIC, Hartford, Conn.; frequency 1,040 kilocycles per second, distance 480 kilometers; January 16, 1937

critical frequencies at particular times. This is the reverse of the process by which the graphs of maximum usable frequency described above were obtained.

Broadcast-Frequency Sky Waves

Referring further to figure 19, the lower pair of graphs shows the morning and evening intensities of reception from a broadcast station, on 1,040 kilocycles per second at a distance of 480 kilometers. At about 0740 in the morning there is a change of character and the start of a rise in intensity, and at about 1640 in the evening there is a marked change of character followed by a rise in intensity. These may be interpreted as the times at which the radio transmission changed from the *E* to a lower layer, and vice versa, and thus the times at which a layer below the *E* layer had a maximum usable frequency of 1,040 kilocycles per second for the distance of 480 kilometers. From this it may be calculated that the vertical-incidence critical frequency of the low layer is something less than 450 kilocycles per second; the exact value cannot be computed from this one observation because the height is not known. This shows that there is a layer below the *E* layer by which waves of broadcast frequency are reflected in the daytime. (In the summer the absorption is so great there is no transmission via the ionosphere at broadcast frequencies greater than about 1,000 kilocycles per second.)

This low layer may be called the *D* layer. Little is known about it, as ionosphere measurements have not been made directly upon it. Its properties are inferred from data of the kind shown in figure 19.

There is considerable empirical information on received sky-wave intensities at night on broadcast frequencies (which in Europe are from 150 to 1,500 kilocycles per second), for distances out to about 4,000 kilometers, and limited information for distances out to about 15,000 kilometers. For these frequencies the known facts can be expressed very simply, since to a first approximation the received intensity is the same for different frequencies and times of year, and at great distances is practically independent of ground conductivity. A curve of quasi-maximum values of received intensities at night on broadcast frequencies, for one kilowatt radiated from the transmitter, is given in figure 20. The same information on an expanded scale, for a part of the distance range, is given in figures 21 and 22. These two figures are for two typical cases of ground conductivity σ, stated on the figures in electromagnetic units.

The divergences of the curves in figures 21 and 22 for distances less than about 400 kilometers arise merely from the fact that at those distances the ground wave contributes appreciably, while at greater distances the received intensity is due entirely to the sky wave, that is, the wave propagated by reflection from the ionosphere.

The curve splits into two for distances beyond about 1,400 kilometers, one for paths far from the magnetic pole and one for paths relatively near the magnetic pole. This is because of the special effects in the vicinity of the magnetic pole mentioned above. The effect is very large. For instance, for transmissions between the United States and Europe, a distance of say 6,000 kilometers, the intensities are less than a tenth of what they are for transmissions over the same distance between the United States and South America.

There are variations from year to year. The evidence indicates that sky-wave intensities at broadcast frequencies are less in years of high sunspot numbers.

The data expressed by the curves represent only a beginning of effort to reduce this complex subject to quantitative form. The data for distances from 4,000 to 12,000 kilometers are based on measurements which although extensive, and averaged for several years, were all made at one time of year, early morning in the winter; it is not known to what extent they are representative for other times of the night and year. Incidentally, the practical importance of this is reduced, as far as east-west transmission is concerned, because night conditions remain only a short time over the whole of a very long path, particularly in the summer; furthermore there is little broadcasting after midnight at any one place, and thus time differences in the broadcasting tend to prevent interference.

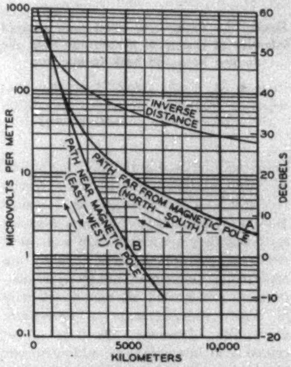


Figure 20. Received field intensity at night over great distances on frequencies from 150 to 1,500 kilocycles per second

Quasi-maximum for one kilowatt radiated

For the significance and limits of application of these curves, see text

THE CONTRIBUTIONS OF THE SEVERAL LAYERS TO PRACTICAL TRANSMISSION

It is extremely hazardous to generalize about the intensity or the satisfactoriness of radio reception at different seasons, different ranges of frequencies, etc. A simple summary is impossible because (*a*) the constant changes of conditions in each layer are much too complicated, (*b*) radio transmission changes from one layer to another, and (*c*) the facts of absorption are largely unknown. Further complications are the various irregularities described in section IV. Furthermore, the satisfactoriness of reception is determined by other factors besides the received field intensity, notably the intensity of received noise or atmospheric disturbances. Noise itself exhibits complicated variations with time of day, season, place, etc.; a discussion of noise and its effects on radio reception is outside the scope of this paper.

Some information on what layers are effective in radio propagation at various ranges of frequencies is given in the curves of maximum usable frequencies, such as figures 11 to 16, but not full information. When the maximum usable frequency is transmitted by the F_2 layer, for instance, the curves give no information as to how effectively a lower layer may be serving at the same time as the means of transmission for lower frequencies. Thus in fact most long- and medium-distance communication in the summer is via the *E* layer; this is in part occasioned by the frequent occurrence of sporadic *E*. The F_1 layer is normally useless for radio transmission except for narrow ranges of distance and frequency, and need seldom be considered; this is because it is seldom enough lower than the F_2 layer and enough more ionized than the *E* layer to serve as the transmitting medium, particularly for considerable distances.

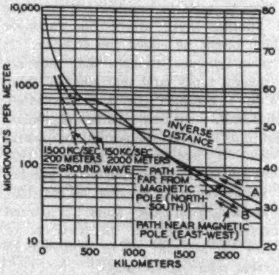


Figure 21. Night field intensity on frequencies from 150 to 1,500 kilocycles per second, for propagation over sea water

$\sigma = 4 \times 10^{-11}$ electromagnetic units

Quasi-maximum for one kilowatt radiated

For the significance and limits of application of these curves, see text

Daytime sky-wave transmission at broadcast frequencies is via the *D* layer. Night-time transmission at broadcast frequencies is principally via the *E* layer. At frequencies higher than about 1,000 kilocycles per second and particularly for the shorter distances, it is commonly via the *F* layer; this is particularly true in winter, sporadic *E* transmission replacing the *F* during much of the summer.

Specific information as to the layer which is operative in transmitting a particular frequency over a particular distance at a particular time is given by actual records of received intensities such as figures 17 and 19; there is, of course, as previously mentioned, no way of compressing all the information from such records into a simple summary. When the facts of radio transmission are examined, it is found that in general they follow very nicely from the facts of the ionosphere and the interpretations given here.

IV. Effects of Ionosphere Irregularities

The primary effects of the ionosphere on radio-wave propagation are those already described, which are due to the normal or regular characteristics of the ionosphere. The modes of variation of those characteristics have been shown to be of a regular and fairly predictable nature. There are some other ionosphere phenomena which are irregular in their nature and make radio phenomena in general much less predictable. Five types of such phenomena have been identified; sporadic *E*-layer transmission, scattered reflections, sudden ionosphere disturbances, prolonged periods of low-layer absorption, and ionosphere storms. The last three are probably due to irregular radiations of various types from the sun. The nature and origin of the first two are less well known; study of them must consider diffusion processes in the ionosphere as well as emanations from the sun and stars, meteors, and perhaps other agencies. The last three are primarily due to irregularities in time, while the first two are primarily due to irregularities in space; the space irregularities are patches or "clouds" in the ionosphere.

It is only recently that these irregularities have been well enough identified to be distinguishable from one another and from some of the regular ionosphere variations such as changes of critical frequency and consequent change of layer in radio transmission. This is another reason, besides the one cited in section II, why the present is a good time to summarize the facts of the ionosphere and their effects in radio transmission.

SPORADIC E

It sometimes happens that waves are reflected by the *E* layer on frequencies higher than that at which the *E*-layer waves normally disappear and the reflection of waves by higher layers begins; for instance, in the example shown in figure 3 waves may sometimes be reflected at the *E*-layer height of 110 kilometers by frequencies higher than 3,000 kilocycles per second. These reflections are due to a different process than the normal reflection in the ionized layer; the process is probably one of reflection from a sharp boundary of stratified ionization. The existence of these "sporadic *E*" reflections necessitates a redefinition of the term "critical frequency," previously defined as the highest frequency at which signals are received back from the layer. When sporadic-*E* reflections occur they may be received simultaneously with reflections from higher layers; thus, for example, in the case shown in figure 3, vertical-incidence reflections might be received at 8,000 kilocycles per second from both the *E* and the F_2 layers. The *E*-layer critical frequency, more precisely defined, is the value (3,000 kilocycles per second in

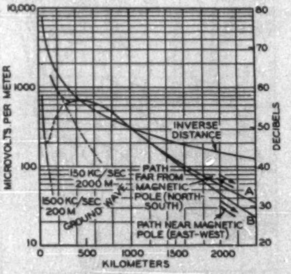


Figure 22. Night field intensity on frequencies from 150 to 1,500 kilocycles per second, for propagation over land of average conductivity

$\sigma = 4 \times 10^{-13}$ electromagnetic units

Quasi-maximum for one kilowatt radiated

For the significance and limits of application of these curves, see text

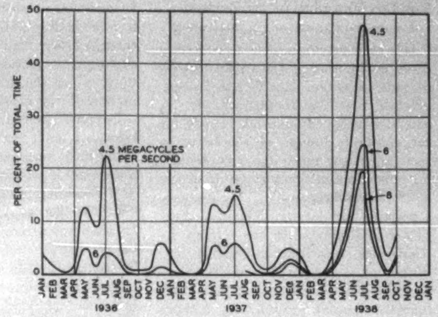


Figure 24. Variation of the prevalence of sporadic E, 1936 to 1938

Per cent of total time of observations on 4, 5, 6, and 8 megacycles per second; 6-megacycle observations begin April 1936, 8-megacycle observations begin August 1937

figure 3) at which the observed virtual height shows a sudden rise to very large values as the frequency is increased. Except for the occurrence of sporadic-*E* reflections, all waves of higher frequency pass through the *E* layer and are not reflected by it.

The action of sporadic *E* may be analogous to partial reflection in optics while the regular ionosphere layer action is analogous to total reflection in optics. The sporadic *E* is confined to limited regions, like clouds or patches, in the *E* layer which have a very sharp boundary. These patches may be from perhaps one kilometer to several hundred kilometers in extent. Waves reaching them are reflected, even when of frequencies much higher than the *E* critical frequency. Sporadic *E* is thus patchy in space as well as sporadic in time, so that its name is well justified.

Sporadic *E* leads to interesting results in radio transmission. It accounts for long-distance transmission up to higher frequencies than by any other means. The maximum vertical-incidence frequency for which strong reflections by sporadic *E* have been found is about 12 megacycles per second. By reason of the large angles of incidence possible with the *E* layer, this has made long-distance communication possible on frequencies as high as 60 megacycles per second. Such communication is generally for only a short time and for restricted localities. For example, on a particular day a patch of sporadic *E* intense enough to reflect 56-megacycle transmissions was found to be somewhat larger than the state of South Carolina, as shown in figure 23; transmissions from distant points by a single hop, at such distances from a receiving point that they were reflected from this patch over the general vicinity of South Carolina, were received on frequencies as high as 56 megacycles per second. Transmissions reflected from outside this patch were limited to lower frequencies determined by the normal ionosphere ionization.

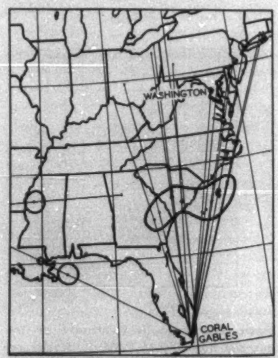


Figure 23. Location of large patch of sporadic E by reception of numerous transmitting stations over long distances

Points of reflection for 56-megacycle transmissions on May 15, 1938, reported by W4EDD, Coral Gables, Fla.

Sporadic *E* occurs most commonly in the summer, particularly in the morning and evening but may occur any time of day or night. It occurs occasionally at all seasons, particularly in the evening. Detailed information on its prevalence is published each month in *Proceedings* of the Institute of Radio Engineers by the National Bureau of Standards. A summary of known data is given in figure 24, expressed in terms of vertical-incidence transmission. The figure shows its year-round occurrence, its greater prevalence in May, June, July, and August, and its variation from year to year. The prevalence of sporadic *E* has not increased uniformly with the advancing solar cycle, like the regular characteristics of the ionosphere, but it is believed to be more prevalent at sunspot maximum than sunspot minimum. Its occurrence is not correlated with other types of ionosphere irregularities nor with thunderstorms or other known phenomena. There is evidence that it occurs more at high than at equatorial latitudes.

Scattered Reflections

An irregular type of reflection from the ionosphere occurs at all seasons and both day and night. These reflections are most noticeable within the skip zone, or at frequencies higher than those normally receivable from the regular layers. Like sporadic *E*, they occur at frequencies which may exceed the F_2 critical frequencies, but are unlike sporadic *E* in that they are complex thus causing signal distortion. They are almost useless for communication purposes. Some types of them are of very weak intensity. The scattered reflections are characterized by very great virtual heights, usually somewhere from 400 to 1,500 kilometers. Their occurrence was for a time thought to indicate the existence of another layer above the F_2 layer which might be called the *G* layer. It is now, however, thought that they are of several types, and that some of them are due to complex reflections from small, ephemeral, scattered patches of ionization in or between the normal ionosphere layers. It has been suggested that some types of these ephemeral patches of ionization may be due to irregular radiations from the stars. Scattered reflections are shown in portions of figures 27, 28, and 30, as explained in the text referring to each.

Sudden Ionosphere Disturbances

The most startling of all the irregularities of the ionosphere and of radio wave transmission is the sudden type of disturbance manifested by a radio fade-out. This phenomenon is the result of a burst of ionizing radiation from a bright chromospheric eruption on the sun, causing a sudden abnormal increase in the ionization of a portion of the ionosphere below the *E* layer, frequently with resultant disturbances in terrestrial magnetism and earth currents as well as radio transmission. The radio effect is the sudden disappearance of radio signals received on high frequencies.

The diminution of the radio signals to zero usually occurs within a minute. The effects occur simultaneously throughout the hemisphere illuminated by the sun, and do not occur at night. The effects last from about ten minutes to an hour or more, the occurrences of greater intensity in general producing effects of longer duration. The effects are more intense, and last longer, the lower the frequency in the high-frequency range (that is, the range from about 1,500 kilocycles per second up). The radio, magnetic, and other effects are markedly different from other types of changes in these quantities. The effects are most intense in that region of the earth where the sun's radiation is perpendicular, that is, greater at noon than at other times of day and greater in equatorial than in higher latitudes.

The effect is most striking. Frequently all "static" as well as the radio signals disappear. Many a radio operator has taken his radio receiver apart, thinking that some wire had become disconnected, and many a time it has been thought that a fuse had blown in the station, when one of these sudden fade-outs occurred.

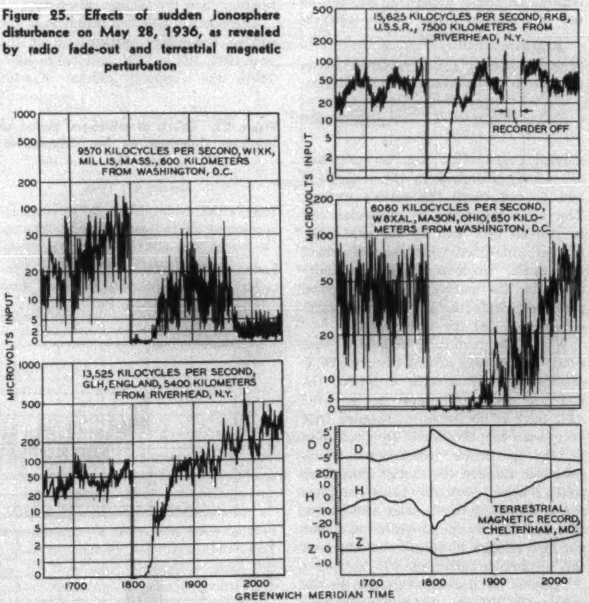
An example showing both radio and magnetic effects is shown in figure 25. The four records of received field intensity from distant stations show that the radio intensity suddenly dropped from normal intensity to zero at 1758 Greenwich meridian time, that is, 12:58 p.m., Eastern standard time. This completely wiped out radio transmission throughout the hemisphere; reports to that effect were received from many points in the United States, also Europe and Japan. As shown, the effect lasted much longer on 6,060 kilocycles per second than on 9,570 kilocycles per second at about the same distance. It did not last longer on 9,570 than on 13,525 or 15,625 kilocycles per second because the distance (and the angle of incidence) was greater in the latter cases. As has been noted previously in other regards, effects on a given frequency for a short-distance path correspond to those on a higher frequency for a long-distance path.

Taking due account of the variation of the effects with frequency and distance, varying effects in differing directions can be explained. Reception in the United States from stations in the southern hemisphere usually exhibits greater effects than reception from other directions (because of passing the equatorial regions). Similarly, when the disturbance occurs at a time when it is morning at the receiving point the effects are usually greater in reception from the east than from the west, and vice versa for the afternoon (because of passing the region where it is noon). A radio fade-out sometimes occurs when it is night at the receiving point, but only when the path of the wave is somewhere in daylight.

The cause of the sudden disappearance of radio signals is the sudden production, by a burst of ionizing radiation from the sun, of abnormally great ionization below the *E* layer. This causes abnormally great absorption of radio waves passing through this ionized region on their way up to and down from the regular reflecting layers. The ionization of the regular reflecting layers is not affected.

There is some evidence that received waves of broadcast and lower frequencies increase rather than decrease in intensity during one of these occurrences. As such waves are reflected by, instead of passing through, an ionized layer of the atmosphere below the *E* layer, this is consistent with the explanation of the disturbance as due to increase of ionization below the *E* layer. It is not known at just what height the ionization is produced, but it may be in the *D* layer, mentioned in section III as the layer responsible for daytime broadcast transmission in the winter. The height of the *D* layer is probably from 50 to 90 kilometers at different times.

Figure 25. Effects of sudden ionosphere disturbance on May 28, 1936, as revealed by radio fade-out and terrestrial magnetic perturbation



The reason this abnormal ionization is produced low in the atmosphere and not in the regular reflecting layers (*E*, *F*, etc.) is doubtless because the solar eruptions emit some radiation of different frequencies from those of the steady radiations which maintain the ionization of the *E* and higher layers. This abnormal radiation is probably of such a frequency as to pass readily through the *E* and higher layers and be absorbed by the ozone which exists at heights from about 15 to 60 kilometers. The frequency of this radiation is presumably in the ultraviolet, nearer to the optical frequencies than those which produce the regular ionization of the *E* and higher layers.

Study of these effects is arousing great interest and focusing new effort upon the study of the sun. The sudden ionosphere disturbance is the only known instance in which a specific happening on the earth follows directly from a specific random happening on the sun or other heavenly body. The ionosphere gives information about some of the radiations from the sun which can be

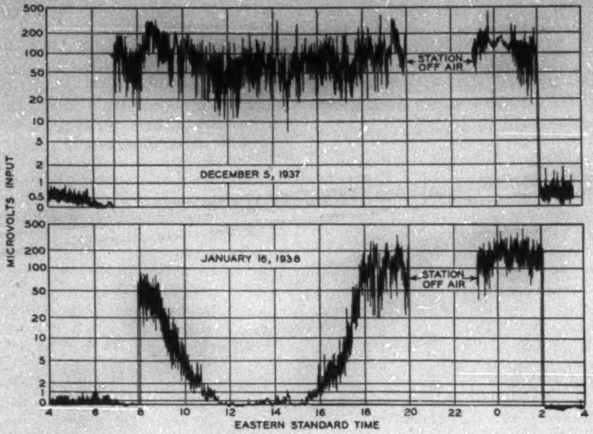


Figure 26. Comparison of recorded field intensity on normal undisturbed day and on day of prolonged period of low-layer absorption

W8XAL, Mason, Ohio, 6,060 kilocycles per second; 650 kilometers from Washington, D. C.

studied in no other way because they are wholly absorbed in the ionosphere and do not reach the earth's surface. The solar eruptions in particular can be studied very well by the aid of the sudden ionosphere disturbances which they cause. Such study may eventually elucidate the nature of the eruptive processes within the sun and the causes of sunspots and the 11-year cycle.

While the evidence indicates that every sudden ionosphere disturbance is accompanied by a solar eruption, the converse does not appear to be true. And there is no reason to suppose that every solar eruption would emit radiation of the particular frequencies which penetrate through the earth's ionosphere to the *D* layer. Probably many eruptions rise high enough in the solar atmosphere to permit the escape of visible light but not high enough to permit the escape of this ultraviolet radiation.

There is no seasonal variation in the occurrence of the sudden ionosphere disturbances or the solar eruptions which cause them. The solar eruptions produce the effect regardless of location on the sun's surface. An eruption usually, but not always, takes place near an active sunspot group. Most of the eruptions which produce sudden disturbances of the ionosphere are much brighter than the average eruption.

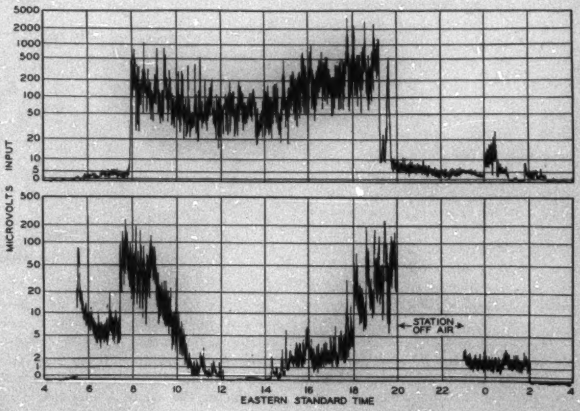
There were 17 known instances of the sudden ionosphere disturbance in the year 1935, 103 in 1936, and 220 in 1937. There was a similar increase of solar eruptions in these years, which were also years of increasing sunspot numbers. The variation of number of sudden ionosphere disturbances from week to week, and from month to month, corresponds fairly well with the number of solar eruptions, but not with the number of sunspots. Thus, just as we found in the case of the ionosphere critical frequencies (figure 10), the sunspot numbers show a year-to-year but not a short-time correlation.

Figure 27. Effects of prolonged period of low-layer absorption on two different frequencies

January 27, 1938

Top—W1XK, Millis, Mass., 9,570 kilocycles per second; 600 kilometers from Washington, D. C.

Bottom—W8XAL, Mason, Ohio, 6,060 kilocycles per second; 650 kilometers from Washington, D. C.



Prolonged Periods of Low-Layer Absorption

This phenomenon is similar to the sudden ionosphere disturbance in its effects and characteristics except that its beginning as well as recovery is gradual and it has a longer time duration, commonly several hours. The intensity diminution is in general not as severe as in the more intense fade-outs, but sometimes the intensities at the medium high frequencies fall to zero.

The phenomenon is illustrated in figure 26, which shows two field-intensity records of transmissions from a station on a frequency of 6,060 kilocycles per second, distant 650 kilometers from the point of reception. The upper graph, for December 5, 1937, is a record of a normal day. The average intensity goes down slightly during the middle of the day, indicating somewhat more absorption during the middle of the day than during the morning and evening hours. (The very low intensities at each end of each graph represent "static.") The lower graph, January 16, 1938, shows a prolonged period of low-layer absorption. Here the intensity falls gradually from the normal value at 0800 (a.m.) to zero, and is zero or very low for several hours, rising again to normal value at 1800 (6 p.m.). Except for the gradual beginning the effect is similar to the sudden ionosphere disturbances.

The different effects of this phenomenon at different frequencies are shown in figure 27. Both records are for the same day. The upper record, for a station on 9,570 kilocycles per second, shows some absorption, that is, reduction of intensity in the middle of the day. The lower record, for a station on a lower frequency (6,060 kilocycles per second) at about the same distance, shows very much greater absorption in the middle of the day. Thus, just as in the sudden ionosphere disturbances, the effects are less at higher frequencies if distance and other conditions are the same.

(Other phenomena shown in this figure are as follows. In the top graph, for W1XK: scattered reflections in the early morning until about 0800, then abrupt beginning of *F*-layer transmission as the ionization increases, abrupt failure of *F*-layer transmission as the ionization decreases at about 1910, then scattered reflections. The bottom graph, for W8XAL, shows: a burst of *F* layer at 0530, then scattered reflections until 0730, transmission until 1600, then F_2-layer transmission until some unknown time after 2000, then scattered reflections until 0200.)

Figure 28 shows that the simultaneous occurrence of low-layer absorption and sudden ionosphere disturbances is possible; in fact it shows two severe fade-outs happening during a prolonged period of low-layer absorption. The lower field-intensity record, for W8XAL, shows a period of low-layer absorption which reduced the received intensity to zero for several hours. Because this had already gone to zero it could not indicate the two intense fade-outs which occurred during this period. They appear, however, on the upper record, which is for a station of higher frequency (W1XK), because the low-layer absorption did not reduce the intensity at this higher frequency to zero. The simultaneous occurrence of these two phenomena was fortuitous.

The equivalent vertical-incidence frequency for station W8XAL was about 2,300 kilocycles per second and for W1XK was about 6,500 kilocycles per second. The reason that the vertical-incidence critical frequencies are not proportional to the transmission frequencies over nearly the same distance is that the two transmissions were reflected from layers of different heights, *F* and *E* layers respectively. This figure illustrates the value of the concept of equivalent vertical-incidence frequency in evaluating absorption over long paths. Records of vertical-incidence reflections at several frequencies are shown at the bottom of the figure. The reflections at 2,500 kilocycles per second were lost between 1100 and 1630 Eastern standard time because of low-layer absorption, so the fade-outs could not be shown at this frequency. The reflections at the higher frequencies were not eliminated by the low-layer absorption, and the loss of reflections between about 1300 and 1500 Eastern standard time indicates the fade-outs.

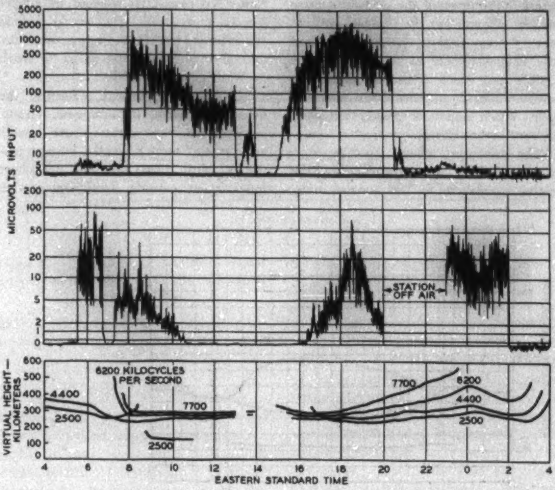


Figure 28. Occurrence of sudden ionosphere disturbances during a prolonged period of low-layer absorption

January 20, 1938

Top—W1XK, Millis, Mass., 9,570 kilocycles per second; 600 kilometers from Washington, D. C.

Center—W8XAL, Mason, Ohio, 6,060 kilocycles per second; 650 kilometers from Washington, D. C.

(Other phenomena shown in figure 28 are as follows. In the top graph, for W1XK: "static" from 0400 to 0530, then scattered reflections until about 0800, then F_2 transmission until 2030, then scattered reflections until 0100, then "static." In the bottom graph, for W8XAL, are shown: "static" from 0400 to 0530, then *F* transmission until 0615, then *E* transmission until 1820, then *F* transmission until 0200, then "static.")

The absorption causing the low-layer absorption effect appears to be due to ionization in a part of the ionosphere below the *E* layer, exactly as for the sudden ionosphere disturbances. The ionization is caused by an abnormally great outpouring of ultraviolet light from the sun, but in this case it is not so sudden as in the eruptions which cause the sudden ionosphere disturbances. The variation of the effects with frequency, and other characteristics, are the same as for the sudden ionosphere disturbances.

Both phenomena occur at all seasons, but the prolonged periods of low-layer absorption have been found to occur in a group of several weeks duration at periods of high sunspot activity, the groups being separated by more or less quiet periods of several months. They frequently but not always occur during periods when sudden disturbances of the ionosphere are numerous. They seem to occur more during years of large solar activity than at sunspot minimum.

IONOSPHERE STORMS

An ionosphere storm is a period of poor radio transmission (except for the low frequencies, below 500 kilocycles per second, which are sometimes improved) lasting a day or more, and usually accompanied by a magnetic storm, that is, a period of unusual fluctuation of terrestrial magnetic intensity. It has two phases, an initial turbulent phase and a following moderate phase. Usually only the second phase occurs in medium and low latitudes. The initial turbulent phase is the cause of the moderate phase which follows, but is confined to the auroral zone, that is, the region around the magnetic pole in which aurora is visible and which is usually limited to within about 20 degrees of the magnetic pole, which region is greatly extended in very severe storms.

The turbulent phase consists of a violent boiling or turbulence of the entire ionosphere in the auroral zone, resulting in irregularly moving small clouds of ionization and a disintegration of the normal stratification of the ionosphere from the *E* layer on up. Whatever causes the storm apparently plunges into the ionosphere at auroral-zone latitudes, and literally tears it up. On the rare occasions when the auroral zone has extended as far south as Washington, an increase in *F*-layer ionization has been observed to precede the turbulent phase. This is consistent with the idea that the carrier of the energy of the ionosphere storm, when it first entered the high ionosphere, caused an increase in ionization. No consistent increase in *F*-layer ionization has been observed to precede the ordinary less severe storms, when the auroral zone did not extend as far south as Washington.

During the turbulent period of the ionosphere storm, high-frequency transmissions are very erratic, both signals and "static" surge violently, being transmitted with good intensity for short intermittent periods, interspersed with periods of complete failure. This indicates severe turbulence in the ionosphere with small unstable patches or clouds of high ionization densities. Such clouds may not be directly over the midpoint of the great-circle path and the transmission may not even be by a great-circle path.

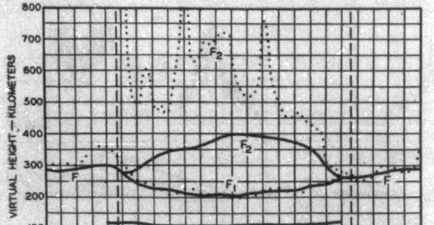


Figure 29. Comparison of ionosphere characteristics on a day of ionosphere storm and normal undisturbed days

June 1938

Solid curves—Average for undisturbed days

Dotted curves—June 8

The moderate phase, following the turbulent phase of an ionosphere storm, is characterized by an expansion and diffusion of the higher *F* region, extending into latitudes farther from the auroral zone, the greater the intensity of the storm. This expansion and diffusion of the ionosphere increases the virtual heights and lowers the ionization densities. This results in abnormally low critical frequencies and abnormally great virtual heights of the night *F* and daytime F_2 layers, and to a less extent of the daytime F_1 layer, and also increases the absorption, that is, reduces received intensities. (When the ionosphere storm occurs in a winter day, the normally unobservable F_1 layer appears.) The night *F* layer is more complex and turbulent than normal. Increased absorption of the *x* components of the daytime F_1 and F_2 layers is especially noticeable. The *E* layer is usually not appreciably affected. This moderate phase of the ionosphere storm extends to the latitude of Washington much more frequently than the turbulent phase. It lags behind the severe effects of the associated magnetic storm by several hours. During the moderate phase the ionosphere gradually returns to normal conditions, but this recovery also lags behind the associated magnetic storm's recovery.

The maximum usable frequencies for night *F*-layer and daytime F_2-layer transmissions are much reduced because of the lowered critical frequencies and increased virtual heights. Thus the higher frequencies are not usable. Frequencies low enough to be received are usually abnormally absorbed, especially during the daytime. There is usually increased fading and instability of transmissions over night paths. Sky-wave field intensities at broadcast frequencies rise much later at night and reach values much lower than normal.

Ionosphere storm effects diminish greatly with distance from the magnetic pole. Transmissions reflected from the ionosphere south of a radio receiving point may often be received satisfactorily while those reflected from the ionosphere farther north are not. Sometimes there appears to be a fairly sharp line of cleavage between the disturbed region (to the north) and the undisturbed (to the south). Transmissions which pass through the disturbed regions in the ionosphere are affected regardless of the direction of transmission.

In most ionosphere storms only the

second or moderate phase is experienced at any except very high latitudes. The principal effects of this phase may be summarized as: (*a*) increase of virtual heights of F, F_1, and F_2 layers, (*b*) decrease of critical frequencies of the same layers, (*c*) greater sharpness of F_1 critical frequency (*d*) decrease of maximum usable frequencies, (*e*) increase of skip distances, (*f*) increase of absorption (that is, decrease of received intensities). Effects (*a*) and (*b*) are illustrated in figure 29, which shows virtual heights and critical frequencies of a day of ionosphere storm (June 8, 1939) in comparison with the month's average of undisturbed days. The E layer was the same on June 8 as on the undisturbed days. Effects (*d*), (*e*), and (*f*) are illustrated in figure 30, which shows (at top) a graphical record of received intensity for a normal undisturbed day and (at bottom) a similar graph for a day of severe ionosphere storm; on the day of ionosphere storm the intensities were extremely low, even for the "static."

(Other phenomena shown in figure 30 are as follows. In the top graph: "static" from 0400 to 0600, then scattered reflections until 0700, then F_2 transmission until 2200, then scattered reflections until 0100, then "static" until 0400. In the bottom graph are shown: very weak "static" for a few minutes after 0400, then scattered reflections until 0700, then very weak "static" until 1130, then scattered reflections until 0100, then very weak "static." The top graph as recorded had values about three times higher from 1755 to 2205, because of a change of antenna at 1755; it is shown here as it would have been with constant antenna conditions.)

Ionosphere storms (and the magnetic storms that usually accompany them) have several characteristics the opposite of those of sudden ionosphere disturbances (and the magnetic perturbations that sometimes accompany them). The former are more intense the higher the latitude, while the latter are more intense the lower the latitude. The former occur both day and night, and the latter are confined to the day hemisphere. The former last one or more days, the latter usually last less than an hour.

These two types of ionosphere irregularity occur in general independently of one another but both are more likely to occur at times of great sunspot activity. They are more frequent and intense during years of sunspot maximum than sunspot minimum. A group of sudden ionosphere disturbances occurring on successive days is sometimes followed, after several days, by one or more ionosphere storms.

On account of their differences, different procedures are followed in practical radio communication to combat the effects of the two types of disturbance. When a sudden ionosphere disturbance occurs it may be possible to continue communication by shifting to a higher frequency; communication could also be accomplished by using a frequency lower than the broadcast range, but a change to such low frequency would in general be too cumbersome for the short time of the sudden ionosphere disturbance. During the turbulent phase of an ionosphere storm the only way to assure dependable communication is to use a frequency below the broadcast range. During the moderate phase of an ionosphere storm it may be possible to continue communication by shifting to a lower frequency within the high-frequency range.

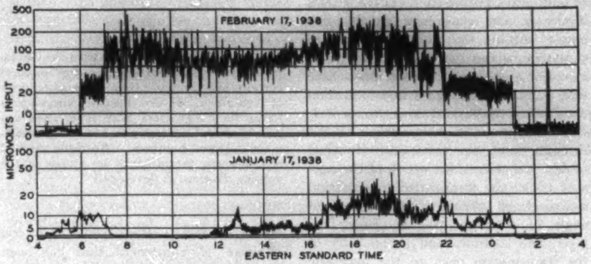


Figure 30. Comparison of received intensities on a day of ionosphere storm and on a normal undisturbed day

W1XK, Millis, Mass., 9,570 kilocycles per second; 600 kilometers from Washington, D. C.

V. Conclusion

The ionosphere is a new world to which radio research and radio operations have given us access in the past few years. A broad survey of this vast territory has been given in the present paper; many important features of the new territory have been neglected or barely mentioned.

I have summarized the work of many men and institutions. The nature of the paper forbade the individual crediting of most data and results to the originators. Splendid work has been done by the ionosphere pioneers (scientists and engineers) who have created the body of knowledge here reported. I am especially indebted to my colleagues in the National Bureau of Standards upon whose work I have drawn most heavily: S. S. Kirby, T. R. Gilliland, N. Smith, F. R. Gracely, and A. S. Taylor.

Short-distance radio transmission by means of the ground wave is not included in this paper, which is limited to the sky wave. Thus the important domains of very low frequencies and ultrahigh frequencies are not covered. The ground wave is calculable and is relatively unvarying with time; its phenomena are not so complicated or interesting as the sky waves. It is by use of the sky waves that nearly all long-distance radio communication is carried on.

This paper also does not include the subject of atmospheric disturbances (that is, natural electrical noise or "static"). They are themselves radio waves, originating principally in distant lightning flashes, and propagated by the same mechanisms as other radio waves. As their effects are largely produced by waves traveling in the ionosphere, much of the information presented in this paper is applicable to their study.

This paper has shown how sky-wave transmission is determined by, and calculable from, the heights and ionization densities and other properties of the ionosphere layers. The maximum usable frequencies at any distance, for instance, are directly determinable from the virtual heights and critical frequencies measured in vertical-incidence experiments. Optimum frequencies may be similarly estimated, though not as certainly as the maximum usable frequencies. The received intensities of the waves may be estimated to a certain extent from ionosphere data, but much more extensive data are needed for this purpose. Study of the behavior of the ionosphere during the five types of irregularities or anomalies discussed in section IV greatly clarifies our understanding of certain radio phenomena, and leads to knowledge of how to overcome transmission difficulties.

Examination of the facts and relations of radio wave propagation via the several layers of the ionosphere should give ample warning that it is hazardous to generalize about good and bad radio reception. Any conclusion must take into account the heights and ionization densities of the several layers concerned, the absorption at the various levels through which the waves pass, the time of day, the season, the epoch of the sunspot cycle, the distance of transmission and angle of take-off of the waves and angle of incidence at the ionosphere layer, the latitudes and propinquity of the transmission path to the magnetic pole, and the occurrence of ionosphere disturbances and irregularities.

The more one views the complexities of radio transmission via the ionosphere, the more he marvels that it provides any intelligible communication. However, as the facts of the ionosphere become better known, and the mechanism of reflection of radio waves from the ionosphere layers is more fully worked out, it becomes more nearly possible to assure long-distance radio transmission at all times. To this end it is fortunate that a beginning has been made on an ionosphere-data reporting service. As this is extended, it will be easier to predict radio-transmission conditions for a given time and path. The reliability of such prediction should surpass that of weather, for the controlling factors are better known and more uniform. Both weather and ionosphere phenomena are due primarily to the sun, but the sun's effects are more direct and more uniform over the earth for the ionosphere than for weather. The weather, incidentally, has no relation to ionosphere phenomena, being produced in much lower regions of the atmosphere.

Study of the ionosphere not only provides means of improving radio services, but is also advancing other branches of knowledge. It is furnishing an explanation for the variations of terrestrial magnetism, hitherto a great mystery. It supplies a way of studying various types of radiations from the sun, many of which do not reach the earth's surface because of being absorbed in the earth's atmosphere. These advances are believed to be only the beginning of gains which future exploration of the ionosphere will bring forth in abundance.

Resonant-Type Constant-Current Regulators

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MEMBER AIEE

Synopsis: This paper deals with the fundamentals of resonant regulators for constant current, and compares them with other kinds of regulators. It also discusses the various forms such regulators may take, their applications and control, and protective equipment required for them. A simple method of predicting performance by means of the voltage diagram is developed.

THE constant-current regulator is one piece of equipment that has been given little attention for some years. At a recent meeting of a Section of the Institute it was apparent, when the subject of constant-current regulators was mentioned, that many engineers present had only a very hazy idea of what they were. A description of the resonant type of regulator, now in its fourth decade, was received with considerable interest as an entirely fresh subject. Here, then, is a "Rip Van Winkle" who has come back to renew acquaintance with some of those other hoary oldsters that have reappeared in new clothes and increased stature. In that honorable company we find the step-type voltage regulator, the wound-core transformer, the overhead ground wire, and the spark-gap lightning arrester.

The term, constant-current regulator, as used here means a device that is used to supply a constant current to an electric system in which the receiving devices are in series, and in which the applied voltage must be varied in proportion to the impedance of the devices in use at any particular time. It is assumed that the regulation is automatic. The discussion is confined to a-c regulators.

Types of Constant-Current Regulators

A crude form of regulator can be made by inserting a large reactance in series with a number of resistance loads. The reactance consumes very little energy, but is such a large part of the total impedance that resistance loads can be connected into and out of the circuit without making large changes in the current. The fixed series reactance does not give current sufficiently constant for most purposes, and it is not widely used.

A variable reactance, either of inductance or capacity, inserted in series with the load and so designed that it automatically adjusts its reactance until the desired current flows in the circuit is the type of regulator in common use today. A new form of regulator of this type, sometimes known as a semiresonant or nonlinear network type, is one in which a saturable reactor in parallel with a capacitor of proper size behaves in the circuit like a variable capacitor, and automatically adjusts its effective reactance until a certain current flows.[1] The more common form is the moving-coil transformer that changes its leakage reactance. The moving coil is repelled from the other coil by the reaction of the currents in the windings. When properly counterbalanced, the coils will approach each other until the leakage reactance is just sufficient to pass the current for which the counterbalance weights are adjusted. In spite of the wide variance in physical form of these two regulators, they are essentially the same. Each is a variable reactance in series with the load.

The inherent characteristics of the regulators composed of a variable reactance in series with the load may be summarized for purposes of comparison as follows. The input current as well as the output current is constant. Consequently, the volt-ampere input to this type of regulator is constant and appreciably greater than the full load volt-ampere output. The power factor is comparatively low. The load circuit is reactive in character. In case of a break in it a very tenacious arc is drawn which frequently causes damage to equipment or contacts. The input circuit is also reactive in character, so switches and other control devices must be capable of breaking the reactive currents. The

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Paper number 39-82, recommended by the AIEE committee on electrical machinery, and presented at the AIEE South West District meeting, Houston, Tex., April 17-19, 1939. Manuscript submitted February 4, 1939; made available for preprinting March 13, 1939.

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1. For all numbered references, see list at end of paper.

losses are constant or practically so for the moving-coil regulator. The semiresonant or nonlinear network form of regulator has losses which vary about inversely as the square of the load. Consequently, the efficiency of either falls off rapidly as load is decreased below rated full load. The characteristic which is the outstanding virtue of the series impedance regulator is its independence of variations in the supply voltage. The moving-coil type of regulator has a certain amount of friction in the balancing linkages; hence it may not readjust itself for gradual voltage changes of a few per cent. Also, the inertia of the moving parts prevents rapid readjustment in case of violent fluctuations of the supply voltage. But under favorable conditions it is comparatively free from current changes caused by supply voltage change. The semiresonant regulator has no moving parts; hence it is still more free from the effects of supply voltage variation. Protective devices which de-energize the regulator in case of a break in the series circuit must be operated by the high open circuit secondary voltage, or by the lack of current in the load circuit.

A resonant network composed of linear impedances can be contrived to maintain a constant current in one element when energy is supplied at constant voltage. There are various forms that such a regulator may take, but the underlying principles are the same for all.

This resonant type of constant-current regulator differs from those previously mentioned in many important respects. It has one conspicuous fault. In fact, it is so conspicuous that it is often given weight far in excess of its importance when regulators are being chosen. This fault is that it is effected by variations in the supply voltage. The resonant regulator usually operates at the same power factor as the load. The input current is proportional to the load; hence both open circuit and overload protection are provided by an overcurrent device in the supply circuit. The load circuit is usually not reactive, and in case of a break there is practically no arcing, although film cutouts and similar devices operate perfectly. The losses vary approximately as the square of the load at the higher loads, and are approximately constant at lower loads. The efficiency is, therefore, similar to that of an ordinary transformer except that for small regulators the maximum efficiency will be near one-third load instead of three-fourths load as it is in the case of most transformers. The efficiency characteristic makes a resonant regulator especially adaptable to small regulators that do not exactly fit the load to be carried. Because there are no moving parts, adjustment to changes in load is immediate, and there are no over-shooting effects during voltage fluctuations. The high efficiency at light load makes the resonant regulator especially desirable for circuits which operate a part of the time at half load or less. The device is reversible; that is, it may be used to obtain constant voltage from a constant-current system.

The semiresonant regulator or nonlinear network type should not be confused with the resonant regulator. It employs capacitance and a *saturable-core* inductance. But the regulating function is performed in a manner analogous to the moving-coil regulator. It is essentially a variable reactance in series with the load and automatically controlled by the current. The same circle diagram that shows the performance of the moving-coil regulator will serve just as well for the semiresonant regulator. The only difference is that one uses the opposite half of the circle from the other.

Then just what is a resonant regulator? This definition may not be rigorous, but it would appear that a resonant type of regulator is a regulator that depends for the primary regulating function on one or more pairs of *linear* reactive impedances each equal to the other but of opposite sign, and which will transform from constant voltage to constant current, or from constant current to constant voltage.

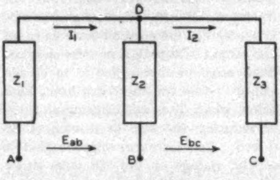

Figure 1. The general wye network

Fundamentals

The resonant constant-current regulator has been thoroughly described by Steinmetz, the inventor.[2] However, the existing literature is not of a type that permits of easy assimilation by the people who could make the most use of it. The resonant regulator is not a self-contained device, and will perform quite differently under different circumstances. For instance, it will carry more load when used on a three-phase four-wire system with neutral return than on a three-wire single-phase system. Therefore, it is essential that the men who apply this device understand how it behaves. It is also desirable that they have a convenient, simple method of predicting the performance of a given regulator under various conditions.

The resonant regulator can best be explained as a three-element wye-connected network with some special properties. A three-element network is shown in figure 1. The currents in this network are easily found in terms of the impedances involved and the applied voltages by the use of Kirchoff's laws. Solutions for the currents are as follows.

$$I_1 = [E_{ab}(Z_2 + Z_3) + E_{bc}Z_2]/[(Z_1 + Z_2) \times Z_3 + Z_1Z_2] \quad (1)$$

$$I_2 = [E_{bc}(Z_1 + Z_2) + E_{ab}Z_2]/[(Z_1 + Z_2) \times Z_3 + Z_1Z_2] \quad (2)$$

The solutions of equations 1 and 2 are general. In order that the network be a resonant regulator, it is necessary to make two of the three impedances equal and opposite, the third impedance being the load. Let us assume that Z_1 is a capacitance and that Z_2 is an inductance, and that their scalar values are equal. In that case Z_1 plus Z_2 will be zero. Therefore, the currents in the regulator and load are

$$I_1 = [E_{ab}(Z_3 + Z_2) + E_{bc}Z_2]/Z_1Z_2 \quad (3)$$

$$I_2 = E_{ab}/Z_1 \quad (4)$$

It is evident from inspection of equation 4 that I_2 is independent of the load impedance Z_3 and of voltage E_{bc}. Furthermore, the load current is always in quadrature with voltage E_{ab} and is constant as long as E_{ab} is constant.

For the purpose of understanding the operation of the regulator, the equations given are sufficiently accurate. It is, of course, not possible to have a pure inductance or a pure capacitance, but we can neglect losses for the present. The effect of losses will be discussed later.

If it is assumed that the supply voltage E_{ab} remains constant, the output current I_2 is not affected by the amount of impedance or the amount of electromotive force, of whatever origin, in the load circuit. E_{bc} can be considered as either a supply voltage or as a back electromotive force in the load circuit. While E_{bc} has no effect on the load current, it does have an effect on the input current I_1 as is clearly shown by equation 3. Note that E_{bc}, a potential introduced in the load circuit, causes a current in quadrature with it to flow as a component of I_1. Therefore, the power flowing in the supply unit *ab* may be dependent on the voltage E_{bc}, depending on the phase relation between E_{bc} and E_{ab}. These relations can best be illustrated by the voltage diagram.

In figure 2*A* there have been drawn the lines AB and CD. The conditions imposed on their relation is that AB is the constant supply voltage and CD is the voltage drop in the load caused by a current which lags behind voltage E_{ab} by 90 degrees. If we draw lines BD and AD and BC, the completed diagram as shown in figure 2*B* represents all of the voltages involved in the network. Since reactances Z_1 and Z_2 are known constants and the voltage across them is shown by the diagram, and since the current in Z_2 is known and the voltage across it is shown in the diagram, the diagram completely determines the network and may be used as the basis of a chart for studying its performance.

The usefulness of the diagram will be greater if it is based on unit voltages. Assume that E_{ab} is a unit voltage, that the load current is unity, and that impedances Z_1 and Z_2 are also unity. In that case the quantities marked on the diagram are represented by the lines alongside which they appear in figure 2*C*.

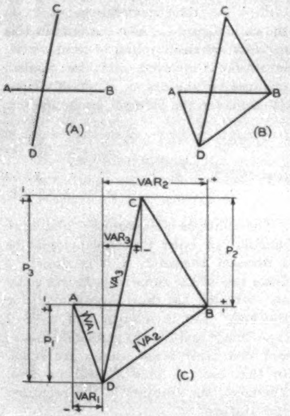

Figure 2. The voltage diagram

Subscripts 1 and 2 indicate quantities flowing in from machines *ab* and *bc*, respectively. Subscript 3 indicates quantities delivered to the load circuit

For convenience in discussion we will assume that voltages E_{ab} and E_{bc} are produced by machines *ab* and *bc*, respectively.

The losses can be calculated from the diagram also. The capacitor losses will be a fixed percentage of the volt-amperes flowing in the capacitor. The inductance losses will be very nearly proportional to the volt-amperes flowing in the inductance. Consequently, the diagram which shows the per cent of reactance required for any load also will show the per cent of full-load loss at any load.

The diagram clearly illustrates the effect of using a voltage BC such that point C lies well above line AB. In that case, machine *bc* supplies watts. At zero load these watts are all absorbed by machine *ab*. As load is added the amount absorbed by machine *ab* decreases until as point D reaches line AB it is absorbing none at all. As point D falls below line AB, the machine assumes load. Hence, when point D is as far below line AB as point C is above it, the machines are equally loaded.

Observe that when this condition exists and when AB equals CD that the volt-amperes of each reactance required is equal to only one-fourth of the load watts. That is the condition of load for maximum economy of material in the regulator. Note, also, that as CD is increased the volt-amperes of reactance required increases very nearly as the square of the load. Consequently, there is a very definite relation between input and output voltage that must be observed if an economical machine is to be obtained. However, there is a lower limit below which it is not economical to go in reducing the size of reactances required. For instance, suppose that a supply voltage of 115/230 volts single phase is available, and a load voltage of 400 volts is desired for a 6.6-ampere regulator. The load voltage is 1.74 times the supply voltage available. For a load voltage 1.74 below line AB, the capacitance and inductance required will be 3.53 times the load volt-amperes. However, the capacitor voltage will be 1.87 times 230 or 433 volts, and that is about as low as it pays to go for a capacitor voltage. While less volt-amperes would be required at 230 volts, the same capacity in microfarads would be required and it is more economical to provide the extra inductance than it would be to establish a 400-volt supply by a special transformer, or to build the smaller inductance and supply a current transformer of the proper ratio in the output circuit. Furthermore, it is more economical to use control equipment rated for 230-volt circuits.

The effect of an open circuit can be observed by manipulating the voltage diagram. In the case of an open circuit the load impedance becomes nearly infinite, and voltage cannot be controlled by the load impedance. Voltage tends to increase, thereby causing increases in voltages AD and BD. But BD is the voltage across an iron-core inductance, and this inductance will decrease in value when the voltage reaches the point at which the iron saturates. We have then a capacitor and an inductance of somewhat smaller impedance in series across voltage AB. Under that condition point D will swing far to the right of point B. In other words, the triangle collapses to almost a straight line. The values which voltages AD and BD assume depend on the design of the reactor, but BD will always be somewhat larger than normal full-load voltage, and AD will be approximately AB plus BD. Therefore, in the design of a regulator it is possible to limit the open-circuit voltage across the capacitor to a known value. The approximate value of AB plus BD for the open-circuit voltage means that the open-circuit voltage must be, for a single-phase regulator, something over the sum of the normal load voltage and the supply voltage. If a regulator is blindly designed for the most economical size of reactor and supply voltage for a given load, the capacitor will be subjected to rather high voltage in case of open circuit. This is another reason that we cannot always design for input and output voltages that give the most economical use of materials.

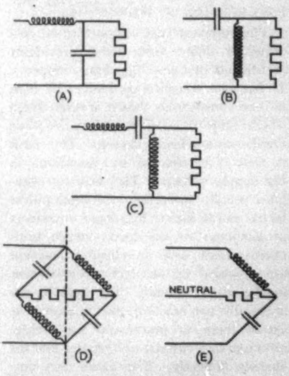

Figure 3. Some circuits used by Steinmetz

The input current under open load conditions is easily determined because both the voltage and impedance of the capacitor are known. Open-circuit protection for the regulator is obtained by setting the overcurrent device in the supply circuit to open on this current. Some such device would be required to protect the circuit in case of apparatus failure; hence no additional device is required for open-circuit protection. A very slight amount of time delay is sufficient to allow film cutouts in the series circuit to operate when a lamp burns out.

The writer has not analyzed the network sufficiently to determine the rate of rise of recovery voltage across a break in the load circuit, but numerous experiments have shown that this circuit does not tend to maintain an arc across a break in the series circuit. In fact, there is only a faint spark when the wire is broken on a regulator that has an open-circuit voltage of 1,000 volts.

The overcurrent device in the supply side is subjected to very rapid rise of recovery voltage across the switch or fusable element. The writer has used a simple "buffer" network consisting of a small capacitor (about three per cent of Z_1) and a high resistance. This network is connected across the load side of the supply fuse or circuit breaker, and is effective in reducing the recovery voltage to a point that ordinary fuses and small circuit breakers break the current without any sign of distress. The resistor also acts as a discharge resistor for Z_1 in case a break in the load circuit is followed by the opening of the supply circuit breaker.

When this buffer network is used, the duty on control equipment is light under all conditions. There is no high inrush current to lamp loads because the input current is only about one-tenth normal when the lamps are cold, and increases gradually as the filaments heat. There is no inductive arcing when the circuit is broken because the device is easily made to have a noninductive character on the supply side. There is no inrush under short circuits and grounds on the series circuit. Ordinary normal-duty contacts on time switches and relays have been found to give trouble-free service for long periods.

The regulator has no moving parts and no inertia; hence voltage surges cannot cause "pumping" or overshooting effects which will damage lamps or other equipment.

The regulator is reversible; that is, it may be used to obtain constant voltage from a constant-current circuit. This is clearly illustrated by the chart of figure 2*C*.

Losses in the inductance and capacitor have an effect on the regulation. This effect can be shown by substituting the per unit values in equation 2 to conform to the actual impedance of the reactances instead of the lossless reactances assumed previously. It would then read

$$I_2 = \frac{(m + jn)(r_1 + jx_1 + r_2 + jx_2) + (r_2 + jx_2)}{(r_1 + jx_1 + r_2 + jx_2)(r_3 + jx_3) + (r_1 + jx_1)(r_2 + jx_2)} \quad (5)$$

This reduces with certain approximations to

$$I_2 = j[1 + (m + x_3)(x_1 + x_2) - (r_3 - n)(r_1 + r_2)] \quad (6)$$

Remember that x_1 and x_2 are of opposite sign and very nearly equal, and that r_1 and r_2 are very small. r_1 is the equivalent per unit resistance of the capacitor, and r_2 is the equivalent per unit resistance of the inductance. They are fixed by the design of the regulator or rather of the elements of the regulator.

The quantity $(m + x_3)$ can be read from figure 2*C* on the line marked VAR_3. The quantity $(r_3 - n)$ can be read from the scale marked P_1.

It is possible to use equation 6 to work out the proper relation between r_3 and $(x_1 + x_2)$ so that I_2 remains more nearly constant for all values of r_3 than if $(x_1 + x_2)$ were always zero. The value of $(x_1 + x_2)$ can be controlled to a certain extent by proper design of the air gap in the inductance. That feature is the basis of a patent issued to S. F. Farkas in 1937. The condition necessary to perfect regulation is

$$x_1 + x_2 = (r_1 + r_2)(r_3 - n)/(m + x_3) \quad (7)$$

Note that n will usually either be zero or one-half times the largest value of r_3 (load resistance) for which the regulator is designed. In case n is zero, very good compensation can be secured. But if n is not zero, compensation may be worse than no compensation if the regulator will ever have to carry light loads. Perhaps it would be clearer to say that it is practically impossible to design x_2 so that equation 7 will be fulfilled under the latter condition.

In some cases, however, a magnetization curve can be determined for the inductance such that the regulator will give actually constant current. For a highly efficient regulator; that is, small values of r_1 and r_2, deviations in current are quite small. For all ordinary purposes special attention to the shape of the magnetization curve is not necessary. It is sufficient to fix two points on the curve, one at or near full load, the other at or near no load. This is easily accomplished by unbalancing the regulator by means of taps on the inductance winding until no-load and full-load currents are equal.

The current may be adjusted by changing the supply voltage or by using a variable-ratio current transformer between the regulator and the load. Also, some of the special forms of regulators to be described are designed so that output current can be adjusted.

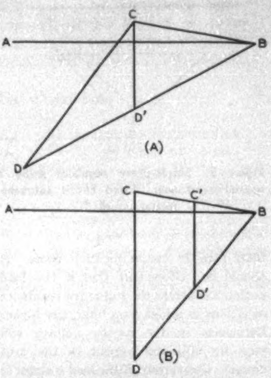

Figure 4. Voltage diagram of regulator using an autotransformer with an air gap

Practical Forms of Regulators

Steinmetz described two forms of regulators which he called the resonant T and the monocyclic square. They could be used with or without auxiliary transformers. The author has built a number of regulators which combine the functions of the regulator and the auxiliary transformers in a single device. These various forms of resonant regulators will be described. The resonant T is represented in figures 3*A* and 3*B* and by the voltage diagram of figure 2*C* provided voltage *bc* is zero. In other words, the load return wire is attached to one wire of the single-phase supply system. This is also described by saying that it consists of two equal and opposite reactances connected in series across the supply voltage with the load shunted across one of the reactances. Some may also recognize this as the familiar L

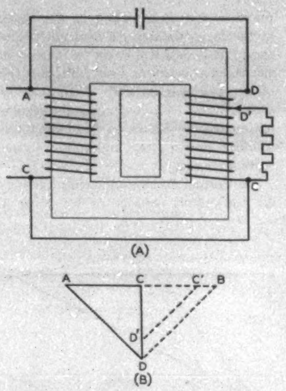

Figure 5. Single-phase regulator using a special transformer. Load circuit autotransformer coupled

filter used in communication work. It should be self-evident that if the load is shunted across the inductive reactance as is done in a high-pass filter, any higher harmonics in the supply voltage will pass through and appear in the load circuit. Conversely, if the load is shunted across the capacitor as is done in a low-pass filter, higher harmonics in the supply voltage will not pass and the current wave will be very nearly a true sine, even though the supply voltage is rich in harmonics. It is sometimes desirable for other reasons to shunt the load across the inductance. In that case harmonics may be suppressed by using an oversize capacity in series with a small inductance as the input arm of the T. This combination will be resonant at the fundamental frequency, but will not pass harmonics readily. The writer has not found any objectionable harmonics in the T-connected regulator except at less than ten per cent load.

The monocyclic square is perhaps the most familiar name for a resonant type of regulator. The connections are as shown in figure 3*D*. The symmetry of the diagram immediately suggests that it could be cut along the diagonal to which the supply voltage is applied and continue to function. Such a regulator is shown in figure 3*E*. This latter arrangement is represented by the voltage diagram, figure 2*C*, if we make voltage E_{bc} equal minus one-half of voltage E_{ab}. The advantage of the square over the half square is that it does not require a neutral wire on the supply circuit. The monocyclic square, like the resonant T with the load shunting the inductance, is affected by voltage harmonics in the supply, and can be cured by the same process as that used for the T.

Steinmetz used a back electromotive force in the load circuit equivalent to E_{bc} in our analysis. This usually consisted in the third phase of a three-phase circuit exactly as analyzed in figure 2*C*. But sometimes it was the second phase of a two-phase system introduced in the load circuit of a monocyclic square. At no load both halves of the square "folded over" so that they absorbed power. At full load the square furnished half of the power; the second phase of the system furnished the other half. This latter connection usually required that one or more auxiliary transformers be used.

The device as it has been described is about as it has existed up to the last few years. Various improvements have been made to make it more useful and flexible in its applications.

One improvement was to substitute for inductance Z_2 a transformer with an air gap in the core. In that case the transformer acts as a reactor and as a coupling transformer. This allows some leeway in the use of load voltages higher or lower than that of point *D* (figure 2). Voltage relations in such a device are shown in figure 4*A*. The current delivered from point *D'* is constant and equal to I_2 times the ratio *BD*/*BD'*. If the voltage diagram is not to be altered in shape (which will lower the power factor), the return current must be returned not to point *C*, but to a point *C'* chosen so that *BC*/*BC'* equals *BD*/*BD'*. This follows because the load current is always in quadrature with voltage E_{ab}, and to avoid pulling the voltage diagram out of shape a point for the return current must be chosen on the voltage diagram so that *C'* lies directly under point *D'* (for pure resistance loads) as it does in 4*B*. For regulators in which the ratio *BD*/*BD'* is small, there is little advantage in changing the point of current return. This is the case when a small street lighting regulator is equipped with taps near point *D* to enable the regulator current to be adjusted to the supply voltage available. But if large currents at low voltage are desired from a similar regulator, it would be essential to reduce voltage *BC'* in proportion to the turn ratio used to get voltage *BD*.

Realization of this essential need led to still another improvement. That was to build a special transformer such that voltage *BD* and *BC* would *both be induced in the same winding*. By that method a change of taps to get any desired turn ratio *BD*/*BD'* automatically selects a turn ratio *BC*/*BC'* that equals it. If a regulator is to be used to control current for a wide range of special tests, such as melting-time tests for fuses, this is a very valuable improvement. One method of accomplishing it is shown in figure 5, which is a single-phase regulator. Figure 5*B* is a diagram illustrating how it operates. In figure 5*B* the dotted line *CB* represents the voltage induced in coil *CD* by flux from winding *AC*. The dotted line *BD* represents the voltage induced in the same coil by the leakage flux flowing through the center leg across the air gap. Line *CD* is shown drawn parallel to the resultant voltage which will appear across coil *CD*. The load is connected across a portion of the turns of *CD*, and the coil acts as a coupling autotransformer; that is, the coil *CD* has three functions. It has induced in it part of the supply voltage. It is the reactor winding. It is a coupling autotransformer winding.

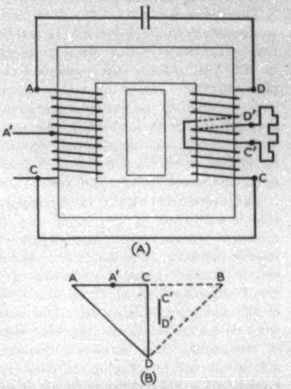

Figure 6. Single-phase regulator using a special transformer. Load circuit electrically isolated

Figure 6*A* is similar except that the windings are arranged to use a higher voltage in the resonant circuit than can be obtained from the supply available. Also, the load is coupled by using a separate winding on the same leg of the core as winding *CD*. This arrangement allows the resonant-circuit reactances to be chosen for maximum economy without regard to the supply voltage available or to the current desired. It also allows the load circuit to be insulated from the supply circuit.

Figure 7*A* is a three-phase regulator in which the transformer winding also serves a triple purpose of supply transformer, reactor, and output transformer. The diagram 7*B* shows the voltage relations. Dotted lines *BC* and *BD* are the supply and reactor components of the total voltage induced in winding *CD*.

Applications

Recent practice in series street lighting shows a trend toward smaller regulators supplying 50 to 100 lamps. During the same period there has been a trend toward higher distribution voltages. If the higher primary voltage is used to supply these regulators, high-voltage control equipment is necessary. This will mean still higher costs for constant-current regulators and control. If the number of regulators is kept low, the series circuit voltage must be high. That is not at all desirable. Many operators have turned to the multiple system for street lighting in order to keep the operating voltage low. That, too, has its disadvantages.

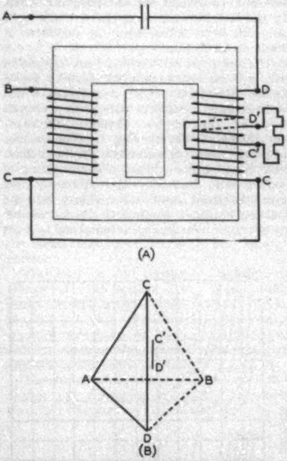

Figure 7. Three-phase regulator using a special transformer. Load circuit electrically isolated

There is a field for a low-voltage constant-current lighting regulator that is economical and can be economically controlled. If this regulator can be supplied from the lighting secondary lines instead of the primary lines, there is opened a still larger field for it, such as for airport lighting and lighting extensive private grounds. Of course, the resonant regulator can be applied at the higher voltages, but it appears to be particularly applicable at the lower voltages.

One of the most promising systems is one in which power is taken from 115/230-volt single-phase secondaries or from wye networks. The grounded-neutral grid is used as the return wire. In case of open circuit the portion of the circuit between the break and the regulator can be energized through one of the reactances at 115 volts to ground. By making a few tests with a voltmeter or test lamp, the fault is easily located. Hand control on each unit is provided so that in case of failure of one circuit the cascade can be made operative by closing the manual switch on the unit following it in the cascade. Time-switch control can be used as simply as can hand or cascade control. Any form of control that will handle sufficient power to close a relay is easily applied. Ordinary relay contacts are capable of short-circuiting a portion of the circuit for midnight lighting circuits. Figure 8 shows the current regulation and efficiency of such a regulator. If overhead wires are used, adequate lightning protection is necessary. Also, a fuse in the load circuit to protect the regulator in case of back feed from high-voltage circuits is desirable.

The resonant regulator has been applied in testing fuses for blowing time. When so used it compensates for the change of resistance of the fuse as its temperature changes. It is fast enough to hold current constant during rapid changes in the resistance. The power requirements are held down to the actual power required to fuse the link. The resonant regulator has been applied to power systems in apparatus for obtaining high-voltage direct current.[3] This application has been described in some detail in previous publications.

Production line testing, testing of motor protective devices, relays, and similar equipment are fields in which the regulator can be used. Constant-torque control for motors can be obtained from it.

The serving of single-phase loads such as electric-arc furnaces from three-phase power circuits has always been a knotty problem. The three-phase resonant regulator will supply single-phase constant current while taking its energy from a three-phase supply. The load balance is not perfect, but it will be better than is usually obtained in two-electrode furnaces. Saving in furnace construction and improved control facilities make the possibility worth investigating. Control of electrode current would be by means of the regulator. Control of furnace voltage would be by means of electrode spacing. It is believed that independent control of these two quantities would be quite valuable in some processes.

Resonant regulators can no doubt be applied in many fields. They are not an expensive device to build. However, the profession seems to have forgotten them; or perhaps it has never been really acquainted. If this effort serves to bring them to mind when a possible application is discovered, its purpose will be fulfilled.

List of Symbols

E_{ab} = the principal applied voltage
E_{bc} = the second applied voltage
I_1 = current flowing in the capacitance
I_2 = the output current of the regulator
Z_1 = the impedance of the capacitor
Z_2 = the impedance of the inductance
Z_3 = the load impedance
$m + jn$ = the components of the unit voltage E_{bc}/E_{ab}
$r_1 + jx_1$ = components of the unit impedance Z_1/E_{ab}
$r_2 + jx_2$ = components of the unit impedance Z_2/E_{ab}
$r_3 + jx_3$ = components of the unit impedance Z_3/E_{ab}
P_1 = power supplied by source of voltage E_{ab}

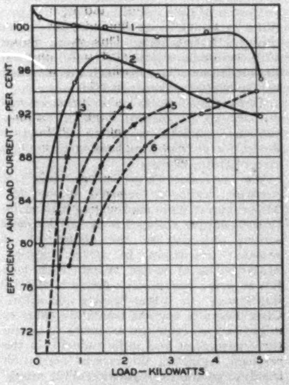

Figure 8. Regulation and efficiency curves

1—Current from a four-kva resonant regulator
2—Efficiency of the same regulator
3, 4, 5, 6—Efficiency curves for one-, two-, three-, and five-kva moving-coil regulators

P_2 = power supplied by source of voltage E_{bc}

P_3 = power absorbed by load

VAR_1 = reactive volt-amperes supplied by source of voltage E_{ab}

VAR_2 = reactive volt-amperes supplied by source of voltage E_{bc}

VAR_3 = reactive volt-amperes absorbed by load

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2. Theory and Calculation of Electrical Circuits (a book), Charles Proteus Steinmetz. McGraw-Hill Book Company, first edition pages 245–96.

3. Constant-Current D-C Transmission, C. H. Willis, B. D. Bedford, and F. R. Elder. AIEE Transactions 1935, pages 102–08.

Discussion

J. Peterson (nonmember; General Electric Company, West Lynn, Mass.): Mr. Miner has presented an interesting review of the fundamentals of resonant regulators for constant current. We feel sure that his concise and yet comprehensive presentation of the characteristics of these resonant circuits will revive interest in their applications by the profession in general.

An obvious reaction to Mr. Miner's paper is the question, why have moving coil regulators been used almost exclusively for series street lighting during the last four decades while the virtues of resonant regulators have apparently gone unheeded all these years?

The truth of the matter is, that resonant regulators have undergone considerable development by various manufacturers of street-lighting equipment during these decades, in an effort to utilize the desirable properties of these circuits, and at the same time, to minimize or eliminate the undesirable characteristics. This work began with Steinmetz who applied and improved the fundamental circuit which was invented by Boucherot in France in 1890.

Development work is still being carried on, but in its present form the simple resonant regulator is not a satisfactory device for universal application to modern street-lighting circuits.

The so-called "conspicuous fault" of resonant regulators referred to by Mr. Miner (variations in load current with variations in supply voltage) is by far the most serious objection to the application of resonant regulators in street lighting. Maintenance of lumen output with long life in series incandescent lamps is of vital importance to street-lighting operators. Close limits of current regulation (plus or minus one per cent) are necessary to obtain these properties, and unfortunately, in the resonant regulator, the current regulation is only as good as the regulation of the supply voltage. Usually it is necessary to allow for considerable variation of primary supply voltage in the distribution circuits from which series street-lighting equipment is energized.

We grant that starting surges are present in the automatic types of moving-coil regulators, but on the other hand, the regulator design minimizes these surges and furthermore the design of series incandescent lamps takes into account these surges and life is, therefore, not adversely affected.

The presence of harmonics in resonant regulator circuits has always been a source of difficulty in applications to series lighting. Mr. Miner has pointed out that certain resonant circuits pass harmonic distortion in the primary supply on through to the load circuit. It is obvious, of course, that these same circuits encourage harmonic distortion in the load-circuit current, when nonlinear load elements exist. Autotransformers and insulating transformers with burned-out lamps on their secondaries generate harmonic distortion on open circuit, which, in the case of the monocyclic square, for instance, causes the root-mean-square series current to *increase dangerously* above the normal value.

The increasing use of vapor lighting in series circuits further precludes the use of simple resonant regulators. Both sodium- and mercury-vapor lamps have arc characteristics which promote distortion in both current and voltage wave forms in resonant circuits. When stabilized by inductive reactance as in moving-coil regulators, the current through the lamps remains practically sinusoidal and distortion appears in the voltage alone. Harmonics in the current must be avoided because both life and lumen output are dependent to a great extent on the use of sine-wave current through the lamp.

Another difficulty introduced by harmonics in the series-circuit current wave shape is telephone interference, particularly from long single-wire loops. Fortunately, the almost universal use of moving-coil regulators has minimized this type of difficulty on both incandescent- and vapor-lamp series lighting circuits.

I would like to stress the distinction between the functions of open-circuit protective equipment for resonant regulators as opposed to moving-coil regulators. Protective equipment is absolutely essential to protect the resonant regulator itself from destruction due to resonance currents within the machine when the load is open circuited. On the other hand, this protection equipment when used with a moving-coil regulator is not required to protect the regulator itself, but rather to protect the load circuit external to the regulator. As a matter of fact, the protective equipment is optional in the latter case and many moving-coil-regulated circuits are operating today without protective equipment.

An interesting characteristic of the polyphase resonant networks not stressed by Mr. Miner, is the fact that the terminals of three-phase equipment must be connected to the three-phase lines in a definite phase sequence depending on the phase rotation of the polyphase lines.

A study of a resonant regulator manufactured in a practical form reveals some interesting points. There will be required a series of taps in the reactors and transformer elements. One group of these taps is necessary to permit the manufacturer to compensate for manufacturing variations in capacitance values in the capacitor section. The other group of taps is necessary to permit the customer to adjust the machine for the actual line voltage that exists on the customer's property. When an insulating load transformer has been added to the simple resonant regulator and when suitable filter equipment for the elimination of harmonics in the load circuit has been added, the kilovolt-amperes of equipment required is such that the economics are favorable to the moving-coil regulator even when the moving-coil regulator is provided with power-factor-correcting capacitors.

W. A. Wolfe (Kansas Gas and Electric Company, Wichita): Mr. Miner is to be congratulated for renewing the acquaintanceship of the profession with the resonant-type of constant-current regulator, and for developing a regulator of this type which is practical for commercial use.

Mr. Miner mentions that the resonant regulator has been used for testing fuses for blowing time. I have used one of the regulators built by Mr. Miner with very successful results for fuse testing. The regulator used was 6.6-ampere four kilovolt-ampere for series-street-lighting service and designed for a 230/115-volt supply source. In the use I made of it the regulator demonstrated its ability to carry considerable overload on intermittent duty. In the fuse-blowing tests currents up to 30 amperes were taken from the regulator at loads up to 15 kva.

The company with which I am associated uses a considerable number of 66-kv transformer-bank fuses of their own design, which is essentially a Micarta tube with a copper wire as the fusible element. Accurate knowledge of the blowing time of various size fuse wires is necessary for co-ordination with relays. Since the resistance of copper wire increases approximately five times between room temperature and its melting point, it was practically impossible to get good results by attempting to hold

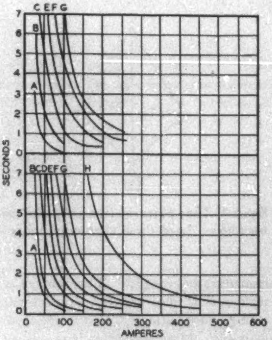

Figure 1

A—Number 26 copper
B—Number 24 copper
C—Number 22 copper
D—Number 21 copper
E—Number 20 copper
F—Number 19 copper
G—Number 18 copper
H—Number 16 copper

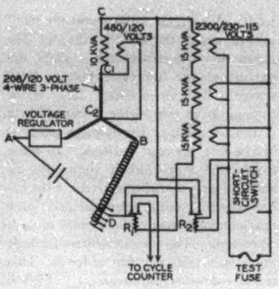

Figure 2

the current through the fuse constant by means of a hand-controlled rheostat of any kind.

Using the resonant constant-current regulator in conjunction with a current transformer and an induction regulator to get the desired values of current, it was possible to get very consistent results.

In figure 1 of this discussion the top group of curves were made using a hand-controlled water rheostat while the bottom group was obtained by the use of the resonant regulator. The improvement is pronounced, as the top group of curves is obviously inconsistent.

Figure 2 of this discussion shows the diagram of the connections used to obtain a current through the fuse in the 400- to 600-ampere range. The diagram is drawn so that the supply voltage and the elements of the regulator form a voltage diagram as well. The letters *A*, *B*, *C*, and *D* designate the same relative points as in Mr. Miner's paper.

The load on the regulator for different values of fuse current was kept within limits by changing connections or cutting in and out the three 15-kva distribution transformers used as current transformers and by moving the point *C* connection to any one of the three positions *C*, C_1, or C_2.

The function of relay, R_2, was to short-circuit the regulator when the fuse blew thus preventing excessive voltage rise on the regulator and excessive current from the supply circuit.

Roland R. Miner: Mr. Peterson's observation that the simple resonant regulator is not satisfactory for *universal* application to modern street-lighting circuits is of course correct. Use of a resonant regulator requires that consideration be given to application problems as well as to the regulator proper.

However the almost exclusive use of moving-coil regulators for the past 40 years does not prove that resonant regulators do not have enormous possibilities. Our series-street-lighting practices have been built around the moving regulator. Had they been built around a resonant regulator the practices and applications would not have been the same.

The lack of development of this type of regulator is in the writers opinion largely the result of commercial considerations and engineering development at the time Steinmetz's work was done. Consider that there was no efficient core material for reactors. Capacitors were very expensive, bulky, and not very dependable. The resonant regulator lacked the inherent inductance necessary to stabilize the arc lamps in common use. Furthermore few users would have been able to understand and properly apply the regulator. Later when the conditions had changed, the profession was busy at other things and the old patents which might once have helped justify further development had expired.

As to the maintenance of proper supply voltage to provide good current regulation, that also is an application problem. The writer has not found it difficult. It should be pointed out that most voltage variations occur when series circuits are not operating. Consequently the regulation obtained during the hours of darkness is all that needs to be considered.

The effect of nonlinear elements in the series circuit is outside of the writers experience. However, analysis indicates that if the harmonic content of the voltage wave in the load circuit is not over about 50 per cent and if the wave forms of all of the devices are similar it does not appear to be impossible to use a resonant regulator, *provided it is adjusted on the wave form on which it is to be used.* A mixture of linear and nonlinear loads would lead to difficulties.

The question of current surges reducing lamp life is not one of starting surges. Starting surges occur when the lamps are cold and are probably harmless. The surges which take a toll of lamps are the ones which occur when a considerable portion of a loaded circuit is short-circuited while the lamps are burning and the ones which are caused by system disturbances. This is especially important if the regulator is lightly loaded.

Comparison of costs of resonant regulators versus moving-coil regulators is largely one of design and of course depends on the purpose for which the regulator is designed. Whether filter equipment to correct for defects in wave form of certain types of lamps is justly chargeable to the regulator is questionable. The series transformer can be eliminated as shown in the paper. Taps, at least on small regulators, have not offered any difficulties or any great expense. The simplicity of control and the lower first cost of small units are two factors that have contributed savings of well over 20 per cent on two installations now in use. These installations have performed more satisfactorily than the ones they replaced.

In order to use the resonant regulator effectively for series street lighting it is necessary to consider the whole system including the lamps, the type of distribution system, the optimum size of units, and a number of purely operating features. While many circuits now supplied with moving-coil regulators could be supplied with resonant regulators of the same rating it would not be economical in part of the cases. In general small units appear to be the most favorable from the standpoint of cost. Some circuits now served by large regulators and operated at high voltage could be cut into smaller segments and operated at lower voltage. Some circuits now using 20-ampere lamps with insulating transformers could be operated without insulating transformers by using a few small 20-ampere regulators fed from low-voltage networks. It certainly would not be safe to assume that the two types of regulators are interchangeable in an existing system. Each should be considered only for the work it is particularly fitted to perform.

An Automatic Voltage Regulator Without Moving Parts Employing Ferroresonance

PALMER H. CRAIG
FELLOW AIEE

Synopsis: An automatic line voltage regulator is described which employs ferroresonance and operates without tubes or moving parts of any kind. The device requires no maintenance attention nor expense, is highly efficient, very accurate, and much faster in its response than previous large-size line regulators.

DESIRABLE features in a line-voltage regulator either for substation or central-station service or for industrial service covering applications such as those requiring a constant alternating voltage for test conditions should, obviously, include as many as possible of the following:

1. No moving parts
2. Complete freedom from maintenance attention or expense
3. A high over-all efficiency
4. No appreciable distortion of the wave form
5. Rapid response
6. Economy in initial cost and maintenance

A regulator incorporating all of the above features has been on the commercial market for several years for loads of the order of a few kilovolt-amperes.[1] Recently, the sizes in which this regulator is being built have been extended into the higher power field up to a load of 625 kva.

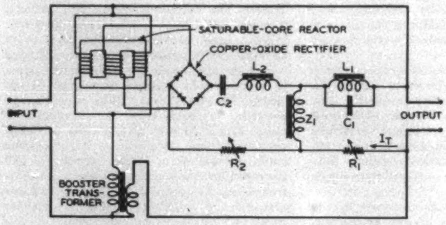

Figure 1. Basic wiring diagram of the voltage regulator

All of these regulators with which the author has worked employ a circuit of which figure 1 is a simplified diagram. The heart of the operation of this circuit depends upon the ferroresonant networks utilized. The reactors L_1 and L_2 in figure 1 are iron-core devices operated on the knee of their saturation curve in such a manner that for a slight change of applied voltage across these reactors there will be a slight change of flux in their cores. Due to the portion of the characteristic curve on which they are worked, this change of flux will change the apparent inductance of the coil throwing it either into or out of resonance with the associated capacitor at the frequency of the impressed voltage. We see from figure 2 that as the impressed voltage decreases slightly from normal, the current through L_1 decreases very rapidly. L_1 and C_1 are associated as a parallel resonant circuit and their output can be thought of as feeding the autotransformer Z_1. Consequently, a slight decrease in the line voltage from normal will increase the output from Z_1 and will impress a higher voltage on the series resonant circuit comprised of the reactor L_2 and capacity C_2. Figure 2 illustrates the manner in which the current through the series circuit increases for a slight departure of the impressed voltage from normal. The series circuit L_2C_2 can be thought of as comprising a second stage of amplification of the changes inherent in the first resonant network. In other words, the series circuit comprising L_2 and C_2 increases the sensitivity of the entire control circuit tremendously over that obtained from a single stage of such a network. The current I_2 flows through the copper-oxide-rectifier bridge R_2. This rectifier supplies direct current to the d-c saturating leg of a saturable-core reactor. Saturable reactors are too well known to require further description but it will suffice to say that an increased direct current in the middle leg of this device will decrease the impedance of the a-c coils wound on the outer two legs. These a-c coils are placed in series with the primary of a booster transformer, the secondary of which is in series with the line. The a-c coils are, of course, so poled as to produce no induced electromotive force in the middle leg, a shorted turn or coil usually being used on the

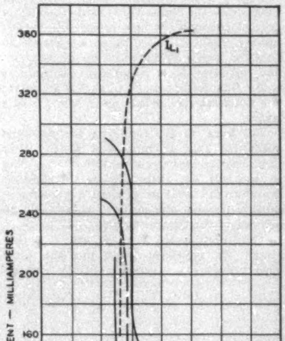

Figure 2. Variation of current in the control circuit with line voltage

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Paper number 39-12, recommended by the AIEE committee on electrical machinery, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted April 20, 1938; made available for preprinting December 5, 1938.

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The author wishes to acknowledge the valuable work of Lester C. Herman, of the Invex Corporation, in developing this regulator, and of Doctor Benjamin L. Snavely, of Lehigh University, for his assistance in the preparation of the mathematical analysis.

[1] For all numbered references, see list at end of paper.

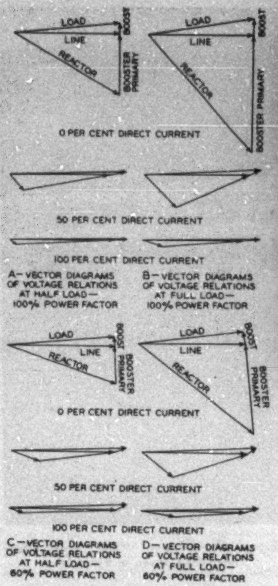

LOAD
LINE
REACTOR
BOOST
BOOSTER PRIMARY
0 PER CENT DIRECT CURRENT
50 PER CENT DIRECT CURRENT
100 PER CENT DIRECT CURRENT
A—VECTOR DIAGRAMS OF VOLTAGE RELATIONS AT HALF LOAD—100% POWER FACTOR
B—VECTOR DIAGRAMS OF VOLTAGE RELATIONS AT FULL LOAD—100% POWER FACTOR
C—VECTOR DIAGRAMS OF VOLTAGE RELATIONS AT HALF LOAD—60% POWER FACTOR
D—VECTOR DIAGRAMS OF VOLTAGE RELATIONS AT FULL LOAD—60% POWER FACTOR

Figure 3. Vector relationships between line, load, reactor, and booster voltages

middle leg to insure that no a-c flux will exist in that leg. The a-c coils may be connected in series or parallel with each other as required.

It should be noted that no part of the load current flows through any of the resonant circuits, which is important because the saturating currents introduce wave-form distortion in the current flowing through them. Since this current is rectified and used only for saturation in the regulators being described, the resonant networks introduce no wave-form distortion in the load circuit. Note, for example, the output voltage wave form of figures 4 and 5.

Since the lowest output available from the control circuit is never zero, a constant "bucking" winding is also used on the center leg of the saturable-core reactor. This coil is supplied with just enough direct current to oppose completely the variable saturation at the condition of highest normal line voltage, thus producing no net saturation at that condition.

Summarizing, as the output voltage of the regulator tends to decrease slightly from normal there will be a very rapid increase in the d-c saturation of the saturable reactor and a relatively large boost introduced by the booster transformer of an amount just sufficient to offset the tendency of the line voltage to decrease.

It should be noted that the added boost is obtained not merely due to a higher voltage on the primary of the booster transformer but because of a shift in position of the vectors of the voltages across the a-c coils of the reactor, primary and secondary of the booster transformer, and the line voltage.

Figure 4. Oscillograms of voltage and current relationships. Input voltage varying

Figure 3 illustrates the manner in which these vectors change for two load conditions at unity power factor and similar conditions at 60 per cent power factor. It can be easily seen from these vector diagrams that the magnitude of the booster primary voltage is not nearly as important as its vector relationship with respect to the line-voltage vector. It is also apparent from figure 3 how buck may be obtained in the line comprising the booster transformer without changing any of the connections of that trans-

Table I. Data Taken on Regulator for 25-Kva Load

Number of Test	E In	E Out	I_L In	I_L Out	Kilovolt-Amperes In	Kilovolt-Amperes Out	I_s	Kilowatts Out	I^2R Loss in Shunt Circuit	Control Circuit Watts	Power Factor of Load
1	2,380	2,376	more than 10 amperes	10			0.9	24.3	30.5	70	1
	2,200	2,376		10		23.76	0.95	24.0	34	210	1
	2,180	2,376		9.8		23.2	0.96	23.7	34.8	220	1
2	2,440	2,380	7.8	7.35	19	17.5	0.72	15.0	19.6	70	0.86
	2,200	2,376	9	7.35	19.8	17.45	0.95	15.25	33.9	210	0.874
3	2,410	2,380	more than 10 amperes	9.25		22	1.27	12	75.5	70	0.55
	2,200	2,360		9.13		21.55	2.35	12	20.8	210	0.55
4	2,480	2,380	6.3	5.98	15.6	14.24	0.6	10.2	13.6	70	0.72
	2,300	2,330	6.85	5.78	15.7	13.5	1.18	9.8	52.7	100	0.73
	2,160	2,340	7.65	5.78	16.55	13.55	1.9	10.2	135.5	210	0.75
5	2,440	2,360	6.1	5.58	14.88	13.19	0.66	9.2	16.5	70	0.7
	2,260	2,340	6.8	5.52	15.35	12.8	1.34	9.05	68	120	0.71
	2,170	2,340	7.4	5.52	16.05	12.8	1.9	9.1	136	210	0.71
6	2,180	2,360	7.5		16.35		1.9	8.35	136	210	

former. For example, referring to figures 3*C* and 3*D*, under the condition of zero direct current in the reactor we see that the position of the "boost" vector is such with respect to the line-voltage vector as to produce an output voltage lower than the line voltage.

The oscillograms of figures 4 and 5 will give some idea of the currents and voltages which exist in the various parts of the circuit of figure 1.

In figure 4, trace number 1 represents the output voltage, number 2 the total current of the control circuit, number 3 the current through L_1, number 4 the current through C_1, number 5 the current through the series resonant circuit, and number 6 the d-c output to the reactor. It can be seen that the wave form of the load voltage is not appreciably distorted under any condition. The variation in the oscillogram was obtained by producing arbitrarily a system voltage drop which produced the condition shown therein.

Figure 5 is a similar oscillogram wherein the variation was caused by an input voltage drop due to impressing the load on the output of the regulator.

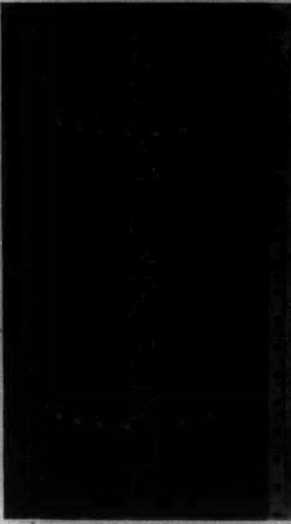


Figure 6A. Typical input voltage variation used for test

Figure 6B. Output voltage under input conditions shown in figure 6A

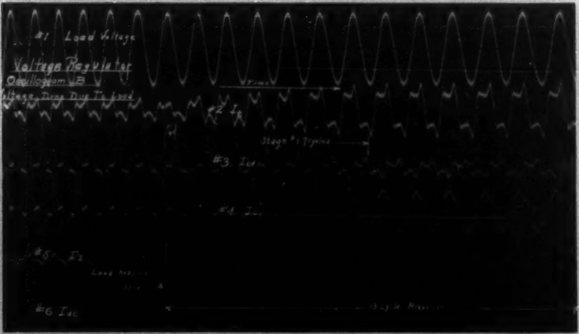
Efficiency

Table I gives test data on a typical regulator for 25-kva 2,400-volt load in which the input and output voltages are given as well as the input and output currents, kilovolt-amperes, shunt current (that is, the current through the a-c coils of the reactor and the primary of the booster transformer), kilowatts out, I^2R losses, control-circuit losses, and power factor of the load. This table shows an over-all accuracy under all conditions of a total of about 2.1 per cent or an accuracy of plus or minus 1.05 per cent. As seen from the table, the efficiency is very high and it should be noted that the power factor of the load is varied from 0.55 to unity, which is a larger variation than would normally be encountered.

Accuracy of Regulation

While the accuracy shown in table I is only approximately plus or minus one per cent, it is quite easy to obtain an accuracy very much better than this figure. Numerous commercial installations of the smaller-sized regulators have been made in which the accuracy is held to within plus or minus one-quarter of one per cent. The author does not wish to imply that an accuracy of this magnitude can be maintained under all conditions, but for special test applications where extremely high accuracies are required and where the conditions of application of the regulator are well known, the device can be quite easily adjusted to accuracies of this order of magnitude.

Figure 5. Oscillograms of voltage and current relationships. Load varying



Figures 6*A* and 6*B* represent respectively the input and output voltages on a system regulated by this regulator, where the input was varied manually.

Variation of the Magnitude and Power Factor of the Load

Figure 6*C* illustrates the effect of varying both the magnitude and power factor of the load through very wide limits. For a certain design of a regulator, full load at 60 per cent power factor produced a range of regulation from approximately $2^3/_4$ per cent buck to 10 per cent boost. The same regulator for half load at 60 per cent power factor covered a range of from about $^3/_4$ per cent boost to about $11^1/_4$ per cent boost. At unity power

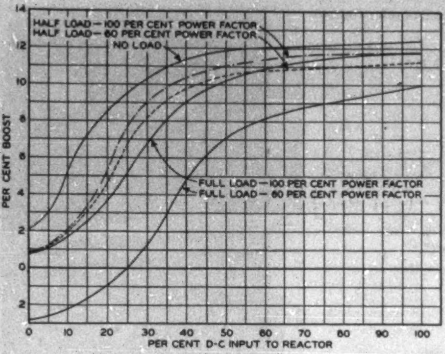


Figure 6C. Range of regulation with various loads

factor the half-load range was from about $^3/_4$ per cent boost to about $11^3/_4$ per cent boost. For the no-load condition, the range was from about $2^1/_8$ per cent boost to about $12^1/_2$ per cent boost. In other words, under the worst possible change of power factor and load for this design, a range from about 2 per cent boost to 10 per cent boost would have been realized. Since the time of that design, improvements have been made in the regulator so that in some models a net over-all range under the worst possible conditions of some 15 per cent is obtained. Obviously, by special design a greater range could also be covered.

Speed of Response

Figures 7*A* and 7*B* illustrate respectively the condition where the input voltage drop is due to impressing the load, and the condition wherein the load is kept constant and the input voltage is decreased. In figure 7*A* it is apparent that the output voltage is completely restored to normal in four cycles. In figure 7*B* it can be seen that the output voltage is restored in approximately eight cycles. In the worst possible condition wherein the input voltage is varied by as much as ten per cent or more all in one step and instantaneously, the output voltage is restored to normal in approximately 14 cycles. The difference between the above time constants can be explained as follows.

Referring to figure 1, when the drop in input voltage is due to an increased load, we see that there will be an increased current produced in the primary of the booster transformer when the increased load is impressed. This increased current is in the right direction to produce increased boost in that transformer. On the other hand, when the load is kept constant and the input voltage is decreased, this decrease in input voltage tends to decrease the current in the primary of the booster transformer which is in the wrong direction for regulation, thus the control circuit in this case must restore the booster primary current not only to normal but increase it in the opposite direction, that is, it must reverse the direction of change of this current and then carry it to whatever increased value is necessary for regulation. This obviously requires a longer time than in the first case.

Physical Arrangement of Various Sizes

Figures 8*A*, 8*B*, and 8*C* illustrate an exterior and two interior views of the regulator in sizes to handle 1- and 2.5-kva loads. Figure 9 illustrates the arrangement for loads of 5, $7^1/_2$, 10 and 25 kva. Figure 10 shows a 30-kva three-phase unit and figure 11 shows the interior of the transformer portion of this size regulator. This three-phase unit is essentially three single-phase regulators each supplied with its own control circuit so that any type of unbalanced load may be regulated perfectly on every phase. The three units shown at the bottom of figure 11 are the saturable-core reactors, those on the top are the booster transformers, and those in the middle are tapped autotransformers for shifting the range of regulation up and down the scale as desired. In the small-size units, the later effect is accomplished by simply rotating the knob on the front of the panel which controls a small variable autotransformer to provide any desired

Figures 7A and 7B. Oscillograms of input and output voltages

Load varied in figure 7*A*. Input voltage varied in figure 7*B*

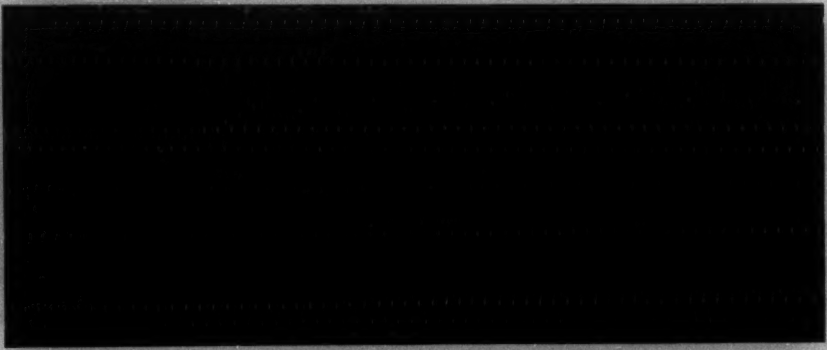
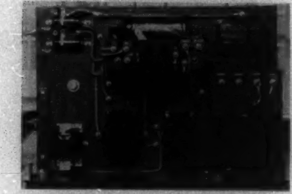


Figure 7*A*

Figure 7*B*

(A)



(B)

Figures 8A and 8B. Exterior and interior views respectively of regulator for 2.5-kva load

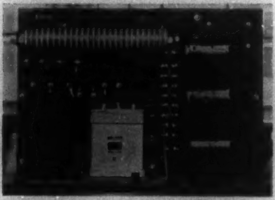


Figure 8C. Rear of interior of 2.5-kva regulator

output voltage and to maintain that voltage constant after it has been set at the required place.

Analysis of Series Resonant Circuit

Figure 12 illustrates the notations utilized in the following mathematical analysis.

Assume that the reactance of the iron-cored reactor can be expressed in the form:

$$X_{ls} = \lambda_s - b_s E_l \qquad (1)$$

λ_s and b_s are constants

E_l = applied voltage
$E_l = |e_l|$

Bold face letters will refer to the vector values of current and voltage. Corresponding upper-case letters refer to absolute values.

$$E_l = X_{ls} I = \lambda_s I - b_s E_l I$$

$$\therefore E_l = \frac{\lambda_s I}{1 + b_s I}$$

$$E_s = E_l - E_c = \frac{\lambda_s I}{1 + b_s I} - X_{cs} I$$

$$= \frac{\lambda_s}{b_s} - X_{cs} I - \frac{\lambda_s}{b_s}\left[\frac{1}{b_s I} - \left(\frac{1}{b_s I}\right)^2 + \ldots\right]$$

Let I_c be such that $E_s = 0$
Then

$$I_c = \frac{\lambda_s}{X_{cs} b_s}$$

if we neglect terms $\frac{1}{b_s I}$ etc.

$$\therefore E_s \approx (I_c - I) X_{cs} \qquad (2)$$

Analysis of Parallel Resonant Circuit

Assume that:

$$X_{lp} = \lambda_p - b_p E_p \qquad (3)$$

where λ_p and b_p are constants.

$$I = I_c - I_l = \frac{E_p}{X_{cp}} - \frac{E_p}{X_{lp}}$$

$$= \frac{\lambda_p - b_p E_p - X_{cp}}{X_{cp}(\lambda_p - b_p E_p)} E_p$$

or

$$b_p E_p^2 + (X_{cp} - \lambda_p - X_{cp} b_p I) E_p + X_{cp} \lambda_p I = 0$$

Let E_c be such that $I = 0$
then

$$\lambda_p = X_{cp} + b_p E_c$$

$$\therefore b_p E_p^2 + (-b_p E_c - X_{cp} b_p I) E_p + X_{cp} \times (X_{cp} + b_p E_c) I = 0$$

$$E_p = \frac{1}{2}\left\{E_c + X_{cp} I \pm (E_c - X_{cp} I) \times \left[1 - \frac{4X_{cp}^2 I}{2b_p E_c^2}\left(1 - \frac{X_{cp} I}{E_c} + \ldots\right)^2\right]^{1/2}\right\}$$

If

$$\frac{X_{cp} I}{E_c} << 1 \text{ and } \frac{X_{cp}}{b_p E_c} << 1$$

$$E_p \approx \frac{1}{2}\left\{E_c + X_{cp} I \pm (E_c - X_{cp} I)\left(1 - \frac{2X_{cp}^2 I}{b_p E_c^2}\right)\right\}$$

Upon discarding the negative sign:

$$E_p \approx E_c - \frac{X_{cp}^2 I}{b_p E_c} \qquad (4)$$

Figure 9. Exterior view of regulator for loads of 5, 2.5, and 10 kva

Combined Series and Parallel Resonant Circuits

Assume that $E_p > E_s$; that is that the regulating circuit draws a leading current.

$$E_0 = [(E_p - E_s)^2 + (RI)^2]^{1/2}$$

In order that the regulating circuit may be effective, that is that a decrease in current in the circuit shall correspond to an increase in E_0, R must certainly

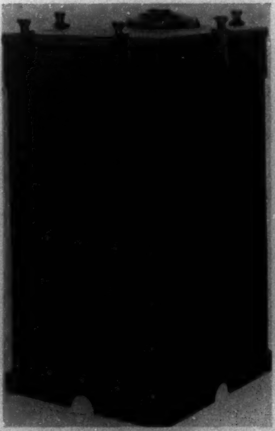


Figure 10A. Regulator for 30-kva three-phase load

Figure 10B. Interior of control portion of 30-kva three-phase regulator

be smaller than that given by the equality $RI = E_p - E_s$. Hence

$$E_0 \approx E_p - E_s$$

$$= E_c - \frac{X_{cp}^2 I}{b_p E_c} - (I_c - I)X_{cs}$$

$$\therefore I \approx \frac{E_c - I_c X_{cs} - E_0}{\dfrac{X_{cp}^2}{b_p E_c} - X_{cs}} \tag{5}$$

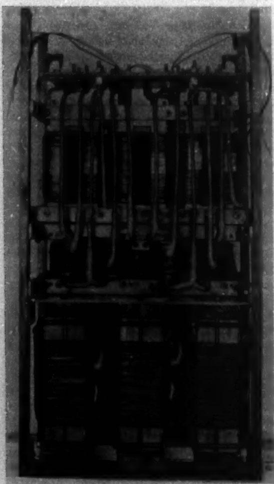


Figure 11. Transformer portion of 30-kva regulator

Let

$$E_c - I_c X_{cs} = V \text{ and } \frac{X_{cp}^2}{b_p E_c} - X_{cs} = r$$

then

$$I = \frac{V - E_0}{r} \tag{5a}$$

Power Circuit

Let k = ratio of primary turns to secondary turns on the transformer.

Let X_1 = reactance of transformer primary when secondary is open. Then we must have:

$$\left.\begin{aligned} \boldsymbol{E}_1 &= \boldsymbol{E}_r + \boldsymbol{E}_p \\ \boldsymbol{E}_0 &= \boldsymbol{E}_i + \boldsymbol{E}_2 \\ \boldsymbol{E}_1 &= k\boldsymbol{E}_2 = jX_1\left(\boldsymbol{I}_1 - \frac{\boldsymbol{I}_0}{k}\right) \\ j &= \sqrt{-1} \end{aligned}\right\} \tag{6}$$

Also we may write:

$\boldsymbol{E}_0 = E_0$ $\quad E_0$ is real

$\boldsymbol{I}_0 = I_0(\cos\theta + j\sin\theta)$ $\quad I_0$ is real

$\boldsymbol{I}_1 = I_1(\cos\phi + j\sin\phi)$ $\quad I_1$ is real

θ = angle of lead for the load

ϕ = phase angle to be determined

We shall assume that the a-c resistance of the d-c controlled reactor may be neglected. Thus in addition to the relations given above:

$$\text{Arg } \boldsymbol{E}_r = \text{Arg } \boldsymbol{I}_1 + \frac{\pi}{2} \tag{7}$$

Combining the above relations:

$$\begin{aligned} \boldsymbol{E}_r = \boldsymbol{E}_0 - \left(1 + \frac{1}{k}\right)\boldsymbol{E}_1 = \boldsymbol{E}_0 - \\ \left(1 + \frac{1}{k}\right) jX_1\left(\boldsymbol{I}_1 - \frac{\boldsymbol{I}_0}{k}\right) \\ = E_0 + \left(1 + \frac{1}{k}\right)X_1\left(I_1 \sin\phi - \frac{I_0}{k}\sin\theta\right) - \\ j\left(1 + \frac{1}{k}\right)X_1\left(I_1 \cos\phi - \frac{I_0}{k}\cos\theta\right) \end{aligned} \tag{8}$$

$$\begin{aligned} \boldsymbol{E}_i = \boldsymbol{E}_0 - \frac{1}{k}\boldsymbol{E}_1 \\ = E_0 + \frac{1}{k}X_1\left(I_1 \sin\phi - \frac{I_0}{k}\sin\theta\right) - \\ j\,\frac{1}{k}X_1\left(I_1 \cos\phi - \frac{I_0}{k}\cos\theta\right) \end{aligned} \tag{9}$$

Determination of ϕ

By virtue of (7) (using the notation of ordinary vector analysis):

$$\boldsymbol{E}_r \cdot \boldsymbol{I}_1 = 0$$

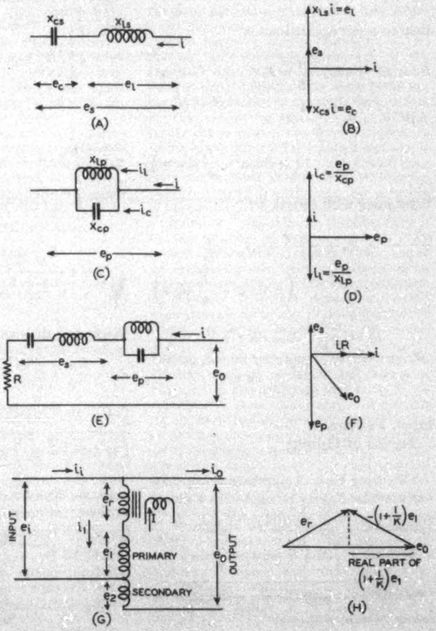


Figure 12. Illustrating the notations employed in the mathematical analysis

$$\left[E_0 - \left(1+\frac{1}{k}\right)jX_1\left(I_1 - \frac{I_0}{k}\right)\right]\cdot I_1 = E_0\cdot I_1 + \left(1+\frac{1}{k}\right)\frac{1}{k}X_1(jI_0)\cdot I_1 = 0$$

$$E_0I_1\cos\phi + \frac{1+k}{k^2}X_1I_0I_1\,(\cos\theta\sin\phi - \cos\phi\sin\theta) = 0$$

$$\therefore \tan\phi = \frac{\sin\theta - \dfrac{k^2E_0}{(1+k)X_1I_0}}{\cos\theta} \qquad (10)$$

Determination of I_1

Let us assume that the drop across the d-c controlled reactor can be expressed accurately enough for present purposes in the form

$$E_r = \frac{\rho I_1 - \beta}{I} \qquad (11)^*$$

where ρ and β are positive constants.

(The question of the value of E_r in the neighborhood of points where $I = 0$ need not concern us since the current supplied by the resonant network will never become zero.)

If

$$E_1 < \frac{1}{2}E_0$$

then to a fair approximation:

$$E_r = E_0 - (R\rho)\frac{1+k}{k}E_1$$

$$\therefore E_r \approx E_0 + \frac{1+k}{k}X_1 \times \left(I_1\sin\phi - \frac{I_0}{k}\sin\theta\right)$$

Combining with equation 11

$$\frac{\rho I_1 - \beta}{I} = E_0 + \frac{1+k}{k}X_1 \times \left(I_1\sin\phi - \frac{I_0}{k}\sin\theta\right)$$

$$\therefore I_1 = \frac{E_0 - \dfrac{1+k}{k^2}X_1I_0\sin\theta + \dfrac{\beta}{I}}{\dfrac{\rho}{I} - \dfrac{1+k}{k}X_1\sin\phi} \qquad (12)$$

Input Voltage in Terms of Output

The same type of approximation that was used for E_r may be used with a much greater degree of accuracy for E_t.

$$\therefore E_t \approx E_0 + \frac{1}{k}X_1\left(I_1\sin\phi - \frac{I_0}{k}\sin\theta\right)$$

in which I_1 is obtained from equation 12 and ϕ is obtained from equation 10, and the I occurring in equation 12 is to be obtained from equation 5*a*.

$$E_t = E_0 + \frac{1}{k}X_1\left[\frac{\left(E_0 - \dfrac{1+k}{k^2}X_1I_0\sin\theta\right)(V-E_0)+\beta r}{\rho r - \dfrac{1+k}{k}X_1\sin\phi(V-E_0)} \times \sin\phi - \frac{I_0}{k}\sin\theta\right] \qquad (13)$$

Expression for Output Voltage

Neglecting for the present the involvement of E_0 in $\sin\phi$, equation 13 may be regarded as an equation of the second degree which may be solved for E_0. Equation 13 may be written:

$$E_0^2 - \left[\frac{1+k}{k}E_t + V - \frac{\rho r}{X_1\sin\phi}\right]E_0 - \frac{\rho r}{X_1\sin\phi}\left(\frac{I_0X_1}{k^2}\sin\theta + E_1\right) + \frac{1+k}{k}VE_t + \frac{\beta r}{k} = 0$$

$$\therefore 2E_0 = \frac{1+k}{k}E_t + V - \frac{\rho r}{X_1\sin\phi} \pm \sqrt{\left(\frac{1+k}{k}E_t + V - \frac{\rho r}{X_1\sin\phi}\right)^2 + 4\left[\left(\frac{I_0X_1}{k^2}\sin\theta + E_1\right)\frac{\rho r}{X_1\sin\phi} - \frac{1+k}{k}VE_t - \frac{\beta r}{k}\right]}$$

$$= \frac{1+k}{k}E_t + V - \frac{\rho r}{X_1\sin\phi} \pm \left(\frac{1+k}{k}E_t - V + \frac{\rho r}{X_1\sin\phi}\right) \times \sqrt{1 + 4\,\frac{\rho I_1\dfrac{X_1}{k^2}\sin\theta - \dfrac{\rho E_t}{k} - \dfrac{\beta X_1}{k}\sin\phi}{\dfrac{1+k}{k}E_t - V + \dfrac{\rho r}{X_1\sin\phi}}\,r}$$

Discarding the root:

$$E_0 \approx \frac{1+k}{k}E_t - \frac{\dfrac{1}{k}\rho I_0X_1\sin\theta - \rho E_t - \beta X_1\sin\phi}{\left(V - \dfrac{1+k}{k}E_t\right)X\sin\phi - \rho r} \times \frac{r}{k} \qquad (14)$$

Since E_0 does not differ greatly from E_t we may, for purpose of substituting in the above equation, replace E_0 in equation 10 by E_t.

$$\therefore \tan\phi \approx \frac{\sin\theta - \dfrac{k^2E_t}{(1+k)X_1I_0}}{\cos\theta} \qquad (15)$$

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United States Patent to P. H. Craig: Number 2,138,732.

Discussion

W. Richter (A. O. Smith Corporation, Milwaukee, Wis.): The usual voltage regulator employing a saturable reactor and capacitor has a rather poor wave shape on the output side. This is no serious drawback, if the regulator is to be used for the regulation of devices where the root-mean-square value only is of importance, such as for instance the filaments of thermionic tubes, but this distortion of wave shape will materially affect the regulation, if for instance the peak value is of importance; such a case would arise for instance if the regulated voltage were rectified and then used for the operation of *X*-ray or similar tubes. While a root-mean-square meter connected across the output voltage may show a constant value in spite of variations of the input voltage, a d-c meter connected across the rectified voltage would show considerable variation if serious distortion occurs.

It would seem that this undesirable distortion should be quite less with the device described by Mr. Craig, due to the fact that he does not regulate the whole voltage, so to speak, but simply adds a small booster voltage in series with the line voltage. This has the additional advantage that the load current does not flow through the whole regulated voltage. Since the actual regulated voltage is only a small part of the total voltage any distortion occurring in this small part will distort the total voltage to a considerably smaller degree.

It would have been of interest if the paper would have given some information as to the effect of small frequency variations on the regulated voltage.

C. Lynn (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Several years ago the Westinghouse Electric and Manufacturing Company experimented with a regulator using saturable cores for controlling the voltage regulation. Thermionic tubes were used for rectifiers and by the use of two saturable cores, both boost and buck to the same extent could be ob-

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[*] For applications of this formula see the paper by C. V. Aggers and W. E. Pakala, *The Electric Journal*, February 1937.

tained. Instead of using a ferroresonant network for a voltage sensitive element in the control, the equivalent was obtained by means of a bridge employing resistors having different temperature-resistance coefficients. The voltage variation of this voltage-sensitive bridge element was applied to the grid of a thermionic tube to control the saturating current, thus giving quite accurate and close voltage regulation both for buck and boost.

This development work was abandoned, however, because of the poor operating performance, especially efficiency, and because of its large size in comparison to a conventional induction voltage regulator for the amount of power regulated.

Referring to the vector diagrams in Mr. Craig's paper, it should be noted that the reactor voltage and the booster primary voltage are quite large, even greater than that of the line voltage. Thus for the 25-kva load, the booster transformer is larger than 2.5 kva for ten per cent range of voltage regulation and the saturable-core reactor is even still larger.

In the data in table I on this type of regulator, the control circuit loss and the I^2R loss in the shunt circuits alone are as much as 345 watts. When to this is added the I^2R loss in the secondary of the booster transformer, the core loss of the booster transformer, and the much larger core loss of the saturable-core reactor, steps must, of course, be taken to maintain the total losses to a sufficiently low value to make this apparatus competitive.

For a comparable 25-kva load using a conventional induction regulator, for the same total, ten per cent voltage range, only a $1^1/_4$-kva unit would be used giving five per cent boost and five per cent buck. This $1^1/_4$-kva regulator has a total loss, including the average loss of the control device and its motor (note that the control device only consumes power during the short interval when it is actually changing the voltage with no power required to hold a different voltage) of about one-half of the watts loss of the control circuit alone, of the regulator described by Mr. Craig.

For small amounts of power there is still another type of voltage regulator available in capacities up to two kilowatts. This is the saturated-core capacitor type of regulator, also using no moving parts, nor even a rectifier or any separate control devices. This device will deliver output voltage constant within plus or minus one per cent or even better with a primary voltage varying 30 per cent total, that is, ±15 per cent. This device is particularly suited for supplying power for use with electronic devices and similar application. It has poor efficiency and large reactive kilovolt-ampere loss due to its having to handle the entire amount of power being controlled instead of only the portion being regulated, and is relatively large for the amount of power controlled. Thus this type is not comparable in this respect to any of the saturable-core reactor-type voltage regulators.

Paul H. Odessey (Heyer Products Company, Belleville, N. J.): Doctor Craig's analysis of the ferroresonant circuit used in this regulator employs concepts that are commonly associated with linear resonance conditions, but which appear to be of limited value regarding the type of phenomena under examination. While this method of approach may have certain practical advantages, the resulting interpretation of combined ferroresonant elements is restricted to rather idealized approximations. In this respect, the treatment is somewhat obscure concerning the physical aspects of such circuits.

The behavior of simple ferroresonant circuits, as is well known, is characterized by double-valued properties which in transition are unstable and subject to abrupt changes in value. Under limiting conditions, however, such circuits exhibit characteristics that are both single valued and sensitive to slight variations (critical stable conditions).

In this application, the use of a complex ferroresonant circuit that would have multivalued characteristics appears to be objectionable because of the existence of several unstable conditions. Likewise, it is possible that the circuit may operate over a portion of its characteristic ineffective to the regulator. In circuits of this kind, this situation may well arise during switching operations.[1] It is likely, therefore, that the ferroresonant elements combined in Doctor Craig's control circuit have single-valued characteristics and operate possibly under so-called critical stable conditions.

In any case, the conditions under which the control circuit performs as a single-valued sensitive element do not appear to be correlated with analysis. The foundation of Doctor Craig's treatment is based upon the assumption that unity power factor (or more explicitly, the condition of equal and opposite reactive components) sufficiently determines the sensitive operating condition. In previous work,[2] however, critical conditions defining the sensitive character of ferroresonance and based upon circuit stability, were found to be independent of unity power factor.

Doctor Craig's application of ferroresonance in a regulator system is commendable for as the performance indicates, an unusual degree of regulation is attained. The control circuit employed in this system is unique and exhibits properties that should bear further investigation.

References

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Palmer H. Craig: The voltage regulator described in this paper is, of course, quite different from that described by C. Lynn. Mr. Lynn states that development work was abandoned on his regulator because of poor operating performance, especially efficiency, and because of large size. When it is borne in mind that regarding operating performance, the voltage regulator described in the present paper has an accuracy far better in its commercial form than that obtainable from the best induction voltage regulator, at a speed of response very much faster than could be possible with any induction voltage regulator, it would seem that operating performance is definitely better from those standpoints. Efficiency was not considered to be a major point in connection with the relatively small size regulators described in the present paper, which are used commercially, chiefly in testing and laboratory applications. In such applications efficiency is a very minor point. In the large-size regulators we have improved the efficiency to a point where it compares very favorably with the induction voltage regulator. Data in this regard will be presented in a future paper dealing primarily with the larger size regulator.

It is true, as Mr. Lynn points out, that the kilovolt-ampere parts size of the booster and reactor utilized in the present voltage regulator is larger than the actual percentage regulation obtained. The physical size of the saturable-core reactor utilized in this regulator is about $1^1/_2$ times the kilovolt-ampere parts size of an equivalent transformer showing a ratio to load represented by the percentage regulation. Compared with the advantages of the present regulator, however, it is not felt that this added physical size is a serious handicap.

Referring to the last paragraph of Mr. Lynn's discussion, it is hardly felt that the regulator Mr. Lynn mentions is comparable with the regulator described in this paper, since the two are designed to cover quite different fields of load, and since no part of the load circuit flows through the resonant network in the case of the present regulator. The regulator described in this paper does not produce an appreciable distortion of wave form nor introduce a large reactive kilovolt-ampere loss in the line, nor exhibit the poor efficiency of the type which Mr. Lynn discusses.

With reference to the discussion of W. Richter, an important advantage of the regulator described in this paper has been pointed out, namely, that very little appreciable distortion in the wave form occurs in the present regulator. Mr. Richter points out several of the many instances in which an undistorted, regulated voltage is necessary. Similarly, in testing any iron-core device such as fractional-horsepower motors or small transformers, the presence of harmonics in the regulated voltage is very undesirable. Variations in the frequency of the line to which the present regulator is attached of a magnitude such as is experienced on ordinary public utility power systems, will not appreciably effect the regulation. If, however, frequency departs materially from normal, variations in the point around which regulation occurs will be noticeable.

Replying to the discussion by Paul H. Odessey, the author realizes that the results obtained in the mathematical analysis are restricted in their application. In devising an algebraic analysis of even a fairly simple circuit it is necessary to choose between solutions involving certain simplifying assumptions, and therefore of restricted validity, and more elaborate solutions in which the primary effects of variation in the various parameters are difficult to follow.

No attempt has been made in the present paper to prove the stability or single-valuedness of the solution obtained. While it is true that a simple series ferroresonant circuit possesses multivalued characteristics, it does not follow necessarily that the more complex circuit treated here is multivalued

Recent Developments in Generator Voltage Regulation

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Synopsis: This paper discusses the fundamental requirements for stability in a generator voltage regulating system having several time delays. A new rheostatic type of voltage regulator element is described, together with a method of introducing antihunting means into the regulating system. In the appendices, a mathematical treatment gives the requirements for stability and adequate damping for several typical voltage regulating systems. It is shown that with certain systems, theoretically perfect regulation and also high damping are possible.

ALL voltage regulators are in effect amplifiers because small deviations in the regulated voltage are made to produce relatively large changes in the generated voltage of the final machine to offset the internal voltage drop caused by variations in load. Recently, conventional electronic tubes have been used as amplifiers in such regulating systems.[1] A little analysis will show that all previous voltage regulators have also been amplifiers, although this concept has not been used to denote their performance characteristic. Such devices as vibrating contacts, saturable reactors,[2] and motor-operated rheostats[3] have been used to produce the large changes in excitation for small errors in the regulated voltage and although in some cases their action has been intermittent in character, the net result produced has been amplification.

In this paper, it will be assumed that the output voltage of the amplifier is continuously variable and is proportional to the deviation voltage at the input. This assumption represents very closely the net characteristic of all such amplifiers, even those using vibrating contacts, because the vibration effect rarely appears in the output voltage of the machine being regulated. Such a concept greatly simplifies the solution of complicated systems because all of the relations over a limited range may be assumed to be linear.

Application of the above assumptions to practical regulating systems has shown that the calculated stability and damping characteristics check very closely the actual performances of such systems. This is a distinct advantage as compared with "cut and try" methods used in the past.

The theoretically perfect regulating system has infinite amplification. Certain systems which will be described in the body of the paper would permit the use of infinite amplification with stability and good damping provided an amplifier with infinite amplification were available. In practice, it is possible to devise systems using any of the above mentioned types of amplifier for very high amplification and accuracy, the only practical limitations being mechanical friction, magnetic hysteresis, etc. This is at variance with the thought that accuracy is incompatible with stability.

New Regulator Element

In order to make the discussion of these problems more concrete, a description of a new form of regulator element will first be given. Following this, the methods of obtaining stability of regulators in general and the new regulator in particular, will be shown.

The regulator, known as the "Silverstat," consists of a novel rheostatic control member, requiring very small force and movement, arranged to vary a resistor having a large number of steps. Figures 1 and 2 show the arrangement.

A number of silver buttons are so mounted that they may be actuated in sequence by a driving member. Each button is carried on the free end of a flat leaf spring which normally rests against a block of insulating material. The leaves are clamped together as shown with insulation between them. The buttons are separated from each other when the driving member is at the left-hand end of its travel. A small movement to the right, only a small fraction of an inch, is required to close the gaps between all of the buttons in sequence. Since each leaf spring is connected to a tap on the regulating resistance, the sequential closing or opening of the buttons respectively short-circuits or cuts in portions of the resistance. The insulating block serves to give the springs a

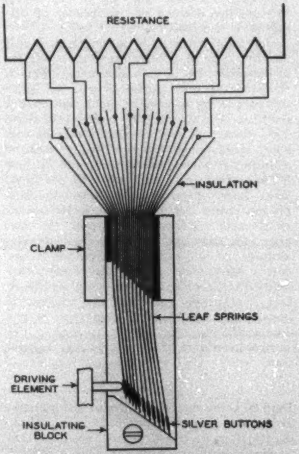

Figure 1. Schematic of Silverstat

Paper number 39-91, recommended by the AIEE committee on electrical machinery, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted October 13, 1938; made available for preprinting March 17, 1939.

C. R. HANNA is manager of the electromechanical division of the research department, K. A. OPLINGER is research engineer, and C. E. VALENTINE is section engineer in the switchboard engineering department, Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.

The authors gratefully acknowledge the contributions and suggestions of S. J. Mikina, S. Sentipal, and V. E. Thornberg during the progress of this development.

1. For all numbered references, see list at end of paper.

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in its response to the application of an arbitrary constant electromotive force. As a matter of fact, it may be shown by a three dimensional graphical analysis (a method which is sufficiently general that it permits account to be taken of the nonlinear nature of the reactor and rectifier resistances) that throughout a wide range of choice of circuit constants, the sensitive circuit will have a single-valued response for all values of applied voltage, quite apart from any reference to the so-called critical stable conditions.

While it may be desirable to operate the circuit with approximately equal and opposite reactive components, such a condition is by no means necessary for the functioning of the circuit. The discussion of unity power factor is therefore entirely irrelevant and especially so since the reference which Mr. Odessey gives concerns a very different circuit from the present one.

definite initial loading which insures a fixed minimum pressure between each pair of buttons before the next pair is shorted, and to space the buttons uniformly regardless of the initial straightness of the leaves. Turning the block through a small angle provides a simple adjustment by means of which the button spacing may be changed.

Figure 3 shows the complete voltage regulator with cover removed. The tapped resistor is placed in the ventilated compartment at the rear, and the Silverstat control member in an enclosed projection near the top. The driving element, mounted just below, is of the moving iron type. It consists of a C-shaped iron magnetic circuit and a potential coil, with an iron armature arranged to move in the constant air gap of the magnetic circuit. The arm which carries the armature comprises the single main moving portion of the regulating device. This arm is mounted on leaf-type springs arranged at right angles to each other, thus providing a frictionless type of axis without wearing parts about which the moving arm is free to turn within the required limits of travel.

When the potential coil is energized, the armature is attracted into the air gap of the magnetic circuit. The use of a constant air gap tends to minimize any change in force due to position. The magnetic pull on the armature is balanced by a coiled spring attached to the arm and located just above the magnetic circuit. Thus at normal voltage on the potential coil, the movable system is in equilibrium.

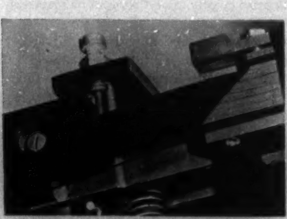
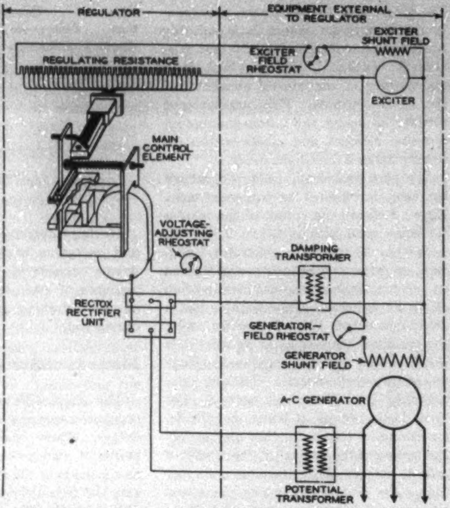


Figure 2. Rheostatic control member

Figure 3. Silverstat voltage regulator

Figure 4. Circuit for regulator system



The voltage of an a-c generator may be controlled as shown by the schematic diagram of figure 4. The generator field is energized by an exciter. Potential for the d-c coil of the voltage regulator is obtained by means of a suitable potential transformer and a dry-type full-wave rectifier.

The action of the Silverstat regulator in controlling the a-c generator voltage will be readily understood by reference to the schematic diagram and the following description. Assume that the a-c machine and exciter are brought up to normal speed and voltage under regulator control. When the voltage on the regulator coil is below normal, the pull of the main spring on the moving element is greater than that of the electromagnet, and therefore, the main spring holds the driving member against the silver buttons, maintaining them closed, thus short-circuiting the regulating resistance. The buttons remain closed up to the moment when the alternating voltage approaches the normal value. When this takes place, the pull of the electromagnet on the moving-iron armature overcomes the opposing pull of the main spring and the driving member backs off, thus opening the silver buttons in sequence to cut resistance into the circuit of the exciter shunt field. The regulator moving element takes a position where just sufficient steps of the regulating resistance are short-circuited to give the excitation required for normal a-c generator voltage.

The damping transformer shown in figure 4 is for the purpose of eliminating hunting. Its action will be described in greater detail under the heading "Methods of Securing Stability."

The regulating resistance has sufficient steps so that the voltage change per step in the normal working range is small, and therefore, smooth voltage control is achieved. The regulating action is that of a semistatic device which operates only when a correction in voltage is necessary.

Due to the fact that the voltage per step is kept below the arcing value, and the volt-amperes per step within reasonable limits, there is no serious pitting of the silver buttons by the short-circuiting or cutting in steps of resistance. By reason of simple construction, and the selection of suitable materials and ratings, the life of the device is practically unlimited.

The use of a separate regulating resistance permits flexibility in the choice of the total resistance and the resistance per step, which may be varied to suit the requirements of the machine and type of service for which the machine is used.

The location of the resistance in a separate compartment where it is suitably protected from external injury and adequately ventilated, removes heat from the vicinity of the control element and the silver buttons. This arrangement permits enclosing the entire voltage-responsive element and silver button assembly under a dust-tight cover.

The performance of the new regulator has been determined by numerous tests. Figure 5 shows the action of the regulator when used with a 25-kva 2,300-volt 1,200-rpm 60-cycle a-c generator. Full load at rated power factor was thrown on the generator by manual closing of an oil circuit breaker. This machine had a direct-connected 125-volt exciter, with the regulator circuits as in figure 4. It will be noted that the regulated voltage was returned to normal value in considerably less than one second. For usual load changes of lesser magnitude, for example, only a fraction of the full-load rating of the generator, the regulator acts to correct the voltage in a shorter time. For different machines, the actual time required for correction of voltage after a load change will vary, depending on the machine time constants.

The satisfactory damping action of the regulator should be noted. There was no oscillation of the regulated voltage during its recovery to the normal value.

In order to meet the requirements of portable and marine applications, the regulator moving arm is statically balanced. For this reason the regulator will operate satisfactorily at various angles of tilt from the vertical. It may be even laid on its back, face, or side without causing wide changes in the regulated voltage.

Figure 5. Oscillogram showing application of full load to a 25-kva 2,300-volt 1,200-rpm 60-cycle a-c generator

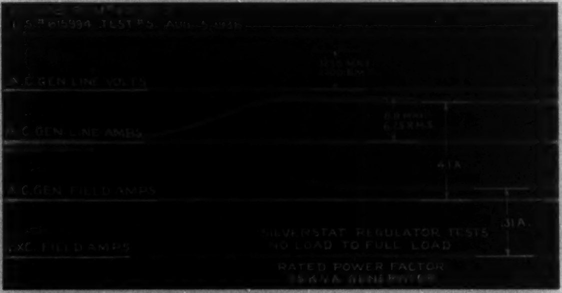
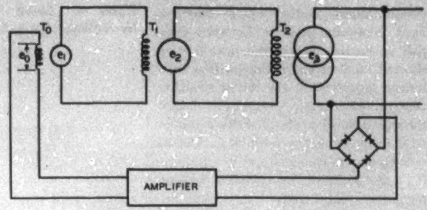


Figure 6. Three time delays—no damping transformers



Having described the construction and operation of a suitable amplifying device capable of handling fairly large amounts of excitation power, the methods of obtaining stability will next be considered.

Methods of Securing Stability

The chief difficulty in stabilizing all regulating systems is the presence of time delays. These may be the time constants of various inductive circuits such as the fields of the rotating machines and also the time delays involved in the amplifier itself. The latter may be electrical, mechanical, or both. In power-generation systems, the usual circuit consists of a voltage regulator and two rotating machines, the exciter and the generator whose voltage is to be regulated. Any delay in the amplifier in addition to the delays in the field circuits of the two rotating machines makes a total of three delays. The circuit shown in figure 6 idealizes such a system by showing the amplifier without delay, but with an extra exciter to represent its delay so that all the constants can be represented by simple inductive and resistive electrical circuits.

With this circuit, the only possibility of obtaining stability is to use low amplification as shown in appendix II where a computation is made for: $T_0 = 0.2$ second, $T_1 = 1$ second, $T_2 = 5$ seconds. It is shown that the amplification may not be greater than 37 if the system is to be free from hunting and that it must be considerably lower than this value in order to result in adequate damping for reasonably rapid recovery from an oscillating condition following a disturbance caused by a change in load. A particular case is computed assuming amplification of 13, for which the rate of decay is 74 per cent per cycle.

A much more satisfactory method of stabilizing voltage-regulator systems is to introduce into the input of the amplifier one or more voltages which are proportional to the rates of change of the various machine voltages. Such voltages may be obtained by various combinations of resistance with capacitors, reactors, or mutual inductances. The choice will depend largely upon the input characteristic of the amplifier. In the present case with the magnetically driven Silverstat regulator, the mutual inductance or damping transformer is a suitable method. The circuit of figure 7 will be used to give a physical understanding of the action of such transformers. Assume a suddenly applied voltage e_0 to the field of the rotating machine. It will be shown that a voltage immediately appears at the secondary terminals of the transformer and that for a certain relation between transformer constants and field constants this voltage will equal e_0. At the start, the inductance of the field circuit will be the principal factor determining the current and

$$e_0 = Lpi_0$$

$$i_0 = \frac{e_0}{Lp}$$

where p is the time derivative operator.

In most d-c machines, the generator voltage and field voltage are approximately equal and small changes in field voltage produce approximately equal changes in generator voltage after sufficient time has elapsed for the field current to build up. The change in generator voltage will at that time equal the change in i_0R of the field and for inter-

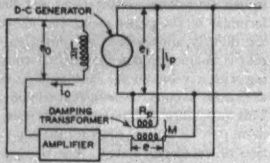


Figure 7. Antihunting stimulus introduced by damping transformer

vening values of i_0 during its growth, i_0R also represents the intervening values of the change in generator voltage. Therefore,

$$e_1 = i_0R = \frac{e_0R}{Lp} = \frac{e_0}{Tp}$$

where

$$T = \frac{L}{R}$$

The transformer is designed to have sufficient resistance in its primary circuit as compared with its inductive reactance to cause the primary current to rise and fall approximately in phase with the applied voltage. With this in mind, the induced voltage in the secondary of the transformer is,

$$e = Mpi_p = Mp\frac{e_1}{R_p} = tpe_1 = \frac{t}{T}e_0$$

Thus, if the time constant $t(= M/R_p)$ of the transformer is equal to the time constant T of the field of the rotating machine, there will be induced instantaneously in the secondary of the transformer a voltage equal to the impressed voltage at the field terminals. Such a feed-back voltage when properly connected to the input of amplifier in a direction to oppose the change in the input which caused e_0 to appear in the output will, of course, be a powerful antihunting influence.

The foregoing may be stated in words as follows: A suddenly applied voltage e_0 produces a rate of change of current in the field winding, which in turn causes the generated voltage to rise with a definite rate. This rising voltage applied to the mutual inductance produces a rising current in the primary which (neglecting inductance) has a proportional rate. Induced in the secondary is a definite voltage equal to the mutual inductance times the rate of change of primary current. Thus an antihunting voltage e proportional to and in phase with e_0 appears at the input of the amplifier.

This does not give any quantitative determination of the antihunting properties of such transformers, but merely illustrates the physical nature of their action. In what follows, several systems employing damping transformers will be described and their performance shown.

The circuit of figure 8, for example, shows a system with three time delays and with three damping transformers. Here again the delay in the first exciter may be thought of as representing the delay in the regulator element or amplifier. It is shown in appendix I that if

$t_1 = T_0$
$t_2 = T_1$
$t_3 = T_2$

the regulator system is more than critically damped; the solution for the recovery of e_3 following any disturbance being of the form

$$e_3 = A\epsilon^{-\frac{t}{T_0}} + B\epsilon^{-\frac{t}{T_1}} + C\epsilon^{-\frac{t}{T_2}}$$

It is seen that no damped oscillatory term is present and that the rate of recovery is determined principally by the largest of the time delays which is usually T_2.

This solution is true for the above choice of damping transformers no matter how large the amplification. Thus a perfect regulating system with more than adequate damping is theoretically possible.

In practice, a transformer t_3 having a time constant comparable with that of the final circuit T_2 would be quite large. Fortunately, the transformer at that position can be omitted because it is still possible to obtain adequate damping of the system by virtue of the other transformers. The circuit shown in figure 9 is the same as the one described, but with t_3 omitted. In appendix III, the damping is computed for the same time delays as indicated in the description of the circuit shown in figure 6; namely,

$T_0 = 0.2$ second
$T_1 = 1$ second
$T_2 = 5$ seconds

In this appendix, t_1 is chosen equal to 0.2 second and t_2 equal to one second. The solution shows the system to be oscillatory, but so highly damped that the decay for one cycle of its oscillation amounts to 74 per cent. This damping is obtained with infinite amplification and, therefore, the system may be theoretically perfect in its regulating ability.

In the corresponding circuit without damping transformers as computed in appendix II, it was necessary to reduce the amplification to 13 in order to obtain an equivalent degree of damping of free oscillation.

The calculations in appendix III show the order of time constants required for damping transformers in a regulating system which is highly damped and yet which has infinite amplification. If a lower degree of damping is desired the time constants of the damping transformers may be reduced. A simple, practical method of reducing and controlling the time constant of a given damping transformer is to use a small adjustable resistance in series with the primary winding. For a given choice of transformers, the damping may be increased by lowering the amplification.

Experimental results with regulating systems as described above show that the calculated performance of the systems checks very closely the actual performance. As many as four machines, a generator, and three exciters, have been set up experimentally in a practical regulating system. With damping transformers to offset the various time delays, satisfactory performance was secured. In fact, such a system might be extended

Figure 8. Three time delays with three damping transformers

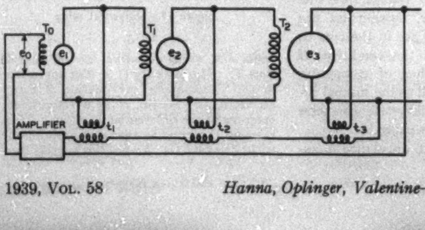
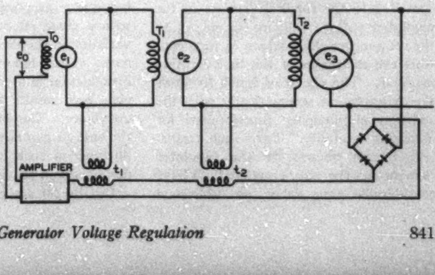


Figure 9. Three time delays with two damping transformers



indefinitely as indicated by the relations in appendix I.

In another application, two exciters with a single pair of regulating contacts operating in the field of the first exciter were used to regulate a 4,300-horsepower synchronous motor operating as a generator. The damping transformers for this installation were calculated in advance from the time constants of the machines and when installed, the regulator performed as predicted by the calculations. The sudden application of approximately full load to the system showed the regulator to be stable, and very highly damped. The regulated voltage changed about one-half of one per cent with the above load change showing that the contacts were giving high amplification. By using several exciters in cascade as above, the current in the field of the exciter may be made very small so that the power handling requirements of the regulator element are reduced. The use of several cascaded machines does not appreciably reduce the rate of recovery, because the exciters are rapid as compared to the generator. Equation 16 of appendix I shows that only the longer time delays have much influence on the rate of voltage recovery of the generator.

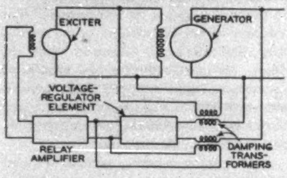


Figure 10. Voltage regulator with relay amplifier

Voltage Regulator Systems With Relays

Many types of relays have been used in the past to increase the power handling capacity of voltage regulators. In all such systems, a sensitive element responding to the small deviations in the regulated voltage delivers its output to the relay or amplifier which in turn controls the excitation of the first rotating machine. The circuit of figure 10 shows the arrangement schematically with the addition of damping transformers for obtaining stability. Two such transformers are shown. If the regulator element and the relay amplifier both have considerable time delay, an additional transformer with primary connected across the output of the amplifier might be necessary. In many cases, however, both the piloting element and the amplifier are sufficiently rapid in their performance that the circuit may readily be stabilized using only the transformer with primary connected across the exciter.

Figure 11 shows a relay employing the Silverstat principle, capable of controlling larger amounts of field power. It is made up of four components similar to those previously described in the direct-driven element. The several elements may be connected in various ways, depending upon the current and voltage requirements. For parallel connection, a small resistance which cannot be short-circuited by the Silverstat buttons is placed in series with each branch. The relay is adjusted mechanically to cause all sections to be short-circuited approximately together, the series resistance causing approximate equality in the division of current for possible errors in the mechanical adjustment.

One pilot element is readily capable of controlling two or more relay amplifier Silverstats to increase the power to any value that may be needed. In such cases, the relay elements have their output terminals connected in series so that any lack of synchronism between the movements of their mechanical parts will not cause improper division as would be the case for the parallel connection. The power rating, of course, increases directly with the number of such relays provided the proper choice of resistance per step or button is made for the voltage and current involved.

Design of Damping Transformers

With the usual connection of the damping transformers in the regulator circuits as described, direct current flows in both primary and secondary windings with directions such as to produce ampere-turns in opposition. Advantage is taken of this fact by balancing the ampere-turns for the average value of the exciter voltages so that as large a value of mutual inductance as possible may be obtained with a given size. The relatively large variation in the exciter voltage from this mean value, however, necessitates the introduction of an air gap in the core to give best results for the condition of unbalance. The method of computing the best air gap as well as the mutual inductance of such transformers has been described in a previous paper.[4]

Usually 30 per cent unbalance is assumed in making computations and the formulas of the paper mentioned above are directly applicable if the direct current is assumed to flow in only one winding with a value 30 per cent as large as the actual average value in that winding. This procedure will result in the maximum possible mutual effect for the condition of unbalance stated. For the balanced condition, the mutual inductance will not be as great as could be obtained with a smaller air gap, but it will be higher than obtained at either extreme of unbalance.

Appendix I. Stability of System With Three Time Delays and Three Damping Transformers

Consider the circuit of figure 8, wherein the time delays are caused by the field circuits of the successive machines. In any one field circuit, the current and voltage are related according to the equation

$$Ri + Lpi = e \tag{1}$$

where p is the time derivative operator. This may be written

$$i = \frac{e}{R + Lp} = \frac{e}{R} \times \frac{1}{1 + Tp} \tag{2}$$

where $T = L/R$ the time constant of the circuit.

It is well known that for the case of a suddenly applied fixed voltage to such a circuit, the current will ultimately reach a value e/R, rising to within $1/\epsilon$ of this in time T. The quantity $(1 + Tp)$ appearing in the denominator of equation 2 may, therefore, be thought of as a time delay operator in the differential equation. This is much the same as the representation of lag or delay by $a + jb$ when this appears in a

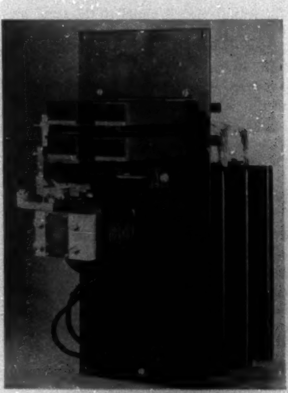


Figure 11. Silverstat relay

denominator of an expression for an a-c vector.

In the present case, it will be assumed that changes in field voltage ultimately appear as increased generated voltage in a one-to-one ratio. All currents and voltages shown will be considered as changes or deviations. During a transient, the change in generated voltage will, therefore, equal the iR drop in the field circuit. That is, for example:

$$e_3 = i_2 R_2 = e_2 \times \frac{1}{1 + T_2 p} \qquad (3)$$

Rewriting this as

$$e_2 = e_3\,(1 + T_2 p) \qquad (4)$$

indicates that changes in e_2 occur ahead of changes in e_3 as represented by $(1 + T_2p)$ because the factor now appears in the numerator.

Similarly

$$\begin{aligned} e_1 &= e_2\,(1 + T_1 p) \\ &= e_3\,(1 + T_2 p)(1 + T_1 p) \qquad (5) \end{aligned}$$

and

$$\begin{aligned} e_0 &= e_1\,(1 + T_0 p) \\ &= e_3\,(1 + T_2 p)(1 + T_1 p)(1 + T_0 p) \qquad (6) \end{aligned}$$

Another relation between e_0 and e_3 will now be derived by considering e_0 as the amplified input voltage which is made up of e_3 and the induced voltages in the damping transformer secondaries. In any of the transformers, the secondary voltage is related to the primary current by

$$e_s = M p i_p \qquad (7)$$

where M is the mutual inductance.

In actual designs of transformers, the primary resistance predominates over its self-inductive effect so that

$$i_p = \frac{e_p}{R_p} \text{ approximately} \qquad (8)$$

Therefore

$$e_s = \frac{M}{R_p} p e_p = t p e_p \qquad (9)$$

where $t = M/Rp$ represents the time constant of the damping transformers. Letting $-\alpha$* represent the voltage amplification

$$e_0 = -\alpha(e_3 + t_3 p e_3 + t_2 p e_2 + t_1 p e_1) \qquad (10)$$

Substituting for e_2 and e_1, their equivalents as given by equations 4 and 5

$$e_0 = -\alpha e_3\,[1 + t_3 p + t_2 p(1 + T_2 p) + t_1 p\,(1 + T_1 p)(1 + T_2 p)] \qquad (11)$$

The complete relation from which stability and damping characteristics may be computed is obtained by equating the two values of e_0 as given by equations 6 and 11.

$$e_3(1 + T_2 p)(1 + T_1 p)(1 + T_0 p) = -\alpha e_3\,[1 + t_3 p + t_2 p\,(1 + T_2 p) + t_1 p\,(1 + T_1 p)(1 + T_2 p)] \qquad (12)$$

Equation 12 may be used in computing circuits having fewer time delays or damping transformers by setting the proper T or t equal to zero. The complete relation will be used in this appendix to give a better physical understanding of the action of the transformers. If we let

$$\frac{t_3}{T_2} = \frac{t_2}{T_1} = \frac{t_1}{T_0} = 1 \qquad (13)$$

that is, if the transformer time constants are each equal to the time delay which precedes them,

$$\begin{aligned} e_3(1 + T_2 p)(1 + T_1 p)(1 + T_0 p) &= \\ -\alpha e_3[1 + T_2 p + T_1 p\,(1 + T_2 p) &+ T_0 p\,(1 + T_1 p)(1 + T_2 p)] = \\ -\alpha e_3\,[(1 + T_2 p)(1 + T_1 p)(1 + T_0 p)] & \qquad (14) \end{aligned}$$

or

$$e_3(\alpha + 1)(1 + T_2 p)(1 + T_1 p)(1 + T_0 p) = 0 \qquad (15)$$

The solution of this differential equation for any value of amplification, no matter how large is

$$e_3 = A\epsilon^{-\frac{t}{T_0}} + B\epsilon^{-\frac{t}{T_1}} + C\epsilon^{-\frac{t}{T_2}} \qquad (16)$$

which indicates very high damping of e_3 after any disturbance. It is the equating of the various transformer time constants t_1, t_2, and t_3 with the values of the preceding delays T_0, T_1, and T_2 as indicated by (13) which brought about the convenient factoring and the highly damped solution of equation 14. The transformers, therefore, may be thought of as anticipatory in their action, each taking care of one delay if made equal to that delay. If it were possible to obtain infinite amplification over an infinite range of corrective ability, the regulator would permit no departure whatever. Practically, it is possible to have very high amplification, but only for a range as limited by the size of the exciters. A suddenly changing load will, therefore, cause departure in the regulated voltage e_3, which will recover as indicated by equation 16. Within the range for which the amplification is very high, the ultimate correction will be practically perfect, however.

Appendix II. Stability of System With Three Time Delays and No Damping Transformers

Using equation 12 of appendix I, and setting $t_1 = t_2 = t_3 = 0$ as is the case in the circuit of figure 6.

$$e_3[(1 + T_2 p)(1 + T_1 p)(1 + T_0 p) + \alpha] = 0 \qquad (17)$$

Such a system can be stabilized if the amplification is kept below a certain value. To illustrate, let

$T_0 = 0.2$ second
$T_1 = 1$ second
$T_2 = 5$ seconds

Equation 17 becomes

$$e_3(p^3 + 6.2\,p^2 + 6.2\,p + \alpha + 1) = 0 \qquad (18)$$

The requirements for stability of a system represented by a third-order linear differential equation with constant coefficients are:

1. All coefficients must be positive.
2. The product of the second and third coefficients must exceed the product of the first and fourth.

If the amplification is chosen with the correct sign, the first condition will be met. The other condition leads to

$$6.2 \times 6.2 > \alpha + 1$$

or

$$\alpha < 37.4$$

for stability.

The amplification must be considerably below this value to obtain high damping. As an example of a damping computation, let the amplification equal 13 in equation 18. Then, the roots of this equation are:

$$p = -5.53,\ -0.3325 \pm j\,1.555$$

The solution is of the form

$$e_3 = \epsilon^{-0.3325t}\,(A \cos 1.555t + B \sin 1.555t) + C\epsilon^{-5.53t}$$

The damped oscillatory term has a frequency

$$f = \frac{1.555}{2\pi} = 0.247 \text{ cycles per second}$$

The damping factor for one cycle is:

$$\epsilon^{-\frac{0.3325}{0.247}} = \epsilon^{-1.345} = 0.26$$

representing a decay per cycle of 74 per cent.

This method of damping a system is not satisfactory because an amplification of 13 yields only mediocre regulation. The use of damping transformers permits higher amplification, in fact, theoretically infinite amplification and is, therefore, to be preferred.

Appendix III. System With Three Time Delays and Two Damping Transformers

Appendix I dealt with an ideal system having every time delay anticipated by a feed-back rate of change effect. With very large generators, the last transformer would be of an impractical size. For this reason, it is seldom included in an actual regulator system. Figure 9 shows such a circuit. The omission of the last transformer does not cause instability or too low damping if the transformers in the other locations are properly designed. Theoretically infinite amplification is still permissible with good damping.

In equation 12, let t_3 equal zero and the amplification equal infinity. The left-hand side of the equation can then be neglected, giving:

$$e_3[1 + t_2 p(1 + T_2 p) + t_1 p(1 + T_1 p)\,(1 + T_2 p)] = 0 \qquad (19)$$

Using the same delays as in appendix II; namely,

$T_0 = 0.2 \quad T_1 = 1 \quad T_2 = 5$

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* The negative sign is used because corrections must be opposite to deviations.

and correcting for the first two with transformers having

$t_1 = 0.2$ and $t_2 = 1$

equation 19 becomes:

$$e_3(p^3 + 6.2\,p^2 + 1.2\,p + 1) = 0$$

The roots are

$$p = -6.029,\ -0.0857 \pm j0.398$$

The solution is of the form

$$e_3 = \epsilon^{-0.0857t}(A\cos 0.398\,t + B\sin 0.398t) + C\epsilon^{-6.029t}$$

The damped oscillatory term has a frequency

$$f = \frac{0.398}{2\pi} = 0.0635 \text{ cycles per second}$$

The damping factor for one cycle is

$$\epsilon^{-\frac{0.0857}{0.0635}} = \epsilon^{-1.35} = 0.259$$

representing slightly over 74 per cent decay per cycle.

This is the same damping as obtained in appendix II without transformers, but with the amplification reduced to 13. In the present instance the amplification was assumed infinite and the regulation is, therefore, theoretically perfect.

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2. A Generator-Voltage Regulator Without Moving Parts, C. P. West and I. D. Applegate. *The Electric Journal*, April 1936, pages 181–3.

3. The Exciter-Rheostatic Regulator, A. G. Gower, Jr. *The Electric Journal*, February 1935, page 73.

4. Design of Reactances and Transformers Which Carry Direct Current, C. R. Hanna. AIEE Transactions, volume 46, February 1927, pages 155–60.

Discussion

E. E. George (Tennessee Electric Power Company, Chattanooga): This paper on generator regulators is one of the most comprehensive and interesting that has been presented to the Institute. The principle of electrical feedback is especially ingenious. The use of a feed-back transformer for stability has been tested out on a d-c telemetering receiver operating off the output of a thermocouple supplied from periodic capacitor discharge. The action of the feed-back transformer is remarkable. It is possible to damp rapid variations so that they are insignificant, although the resulting time delay in load response might be objectionable for some purposes.

The use of high gain and heavy feedback in a generator regulator at once arouses a question as to the size of equipment required. Using a large turboalternator with ordinary time constants, would not a very large main exciter be required if high-speed excitation is to be as effective as the paper would indicate? The practical application of this scheme should be followed with great interest and will do much to answer the old question as to how much real value is obtained from high-speed excitation on large alternators with a time constant of several seconds.

G. S. Lunge (General Electric Company, Schenectady, N. Y.): It is stated clearly in this paper that:

1. Direct-acting regulating devices having appropriately designed antihunting or stabilizing transformers are capable of restoring the regulated quantity to its correct value in practically aperiodic manner; that is, without subsequent oscillation.

2. The voltage introduced by such antihunting or stabilizing transformers can be of smaller magnitude and still more effective if these transformers are connected to the armature of the exciter rather than to the voltage that is being regulated.

It is interesting to note from the oscillogram in figure 5 that in the case of the particular machine tested, most of the time lag was in the exciter. It will be noted that the a-c generator field current reached its maximum value nearly as rapidly as did the exciter field current. It will also be noted that the exciter field current started to increase almost immediately that the alternating voltage dropped due to the application of load. Although this shows that the regulator response is very rapid compared with the time that it takes for the a-c machine field current to build up, this oscillogram would have been of even greater interest if it had shown just how long it took the regulator to respond, as shown, for instance, by the voltage drop across the resistance controlled by the regulator contacts. It would also have been of interest if the oscillogram had shown the complete rating, including the power factor and speed, of the a-c generator, as well as the exciter rating and nominal response.

Because the authors of this paper have used several terms interchangeably in this paper, the reader should be careful to distinguish between the two basic types of stabilizing means:

1. *Antihunting or restoring means.*

2. *Damping means*, such as provided by a dashpot or by eddy-current damping.

The first class is of preventive nature, whereas the second simply provides braking means proportional to the speed of motion of the mechanical parts, as distinct from friction braking which produces an effect practically independent of the speed of motion.

Regulators utilizing true antihunting or restoring means of proper design can eliminate, or at least mitigate, overshooting and hence removed the initial cause of temporary or sustained mechanical oscillation of the regulator, without appreciably slowing up the corrective action of the regulator. In the interest of correcting the regulated voltage promptly, a moderate amount of overshooting is desirable, hence completely aperiodic regulator action is not usually necessary in practice, although prolonged oscillations, even if damped, are objectionable.

W. K. Boice (General Electric Company, Schenectady, N. Y.): This paper recognizes the need for analytical treatment of the behavior of voltage regulating systems. It points out a means of determining characteristics of equipment for stabilizing certain idealized systems such as those shown in figures 6, 7, and 8. The actual system described in the paper and shown in figure 4 differs from these idealized regulating systems in certain fundamental respects. These include:

1. The exciter shunt field in the actual system is connected across the exciter armature and is thus affected by exciter armature voltage changes as well as impulses from the preceding step in the amplification.

2. The time delay in the regulator driving element involves an inertia effect which cannot be described by the simple first-order differential equation which expresses delays in the idealized systems.

3. The stabilizer secondary is in a current-carrying circuit, current changes in which will induce appreciable voltages in the primary.

These features are among those which were taken into consideration by a group of engineers in the General Electric Company in making a study of a practical system somewhat similar to that shown in figure 4. Results of their work have been submitted to the Institute for presentation at a future convention. ["The Direct-Acting Generator Voltage Regulator," W. K. Boice, S. B. Crary, G. Kron, and L. W. Thompson, presented at the 1939 AIEE combined summer and Pacific Coast convention, San Francisco, Calif., and scheduled for publication in the 1940 AIEE Transactions.]

In appendix III of the paper of Messrs. Hanna, Oplinger, and Valentine, the decay of oscillations of a stabilized regulating system is shown to be 74 per cent in one cycle. It should be noted that this is one cycle at oscillation frequency, so that the decay of 74 per cent occurs in about 16 seconds which represents extremely slow decay for a practical system.

It is of interest to note that the response of the actual system is better than that indicated by the calculations in the paper.

C. R. Hanna, K. A. Oplinger, and **C. E. Valentine:** The authors wish to thank Mr. George for his generous appraisal of their paper, and agree with him that "the action of the feed-back transformer is remarkable." The regulating systems described are applicable to high-speed excitation with its attendant advantages. Large exciters will, of course, require a large damping transformer, but the size will not be excessive nor will slow regulator response be the result. For very large swings in exciter voltage, the ampere turns of the transformer are so far out of balance that the resulting saturation greatly reduces the mutual inductance. The lower feed-back effect, therefore, causes less damping for large changes in voltage than for small changes, and the recovery following the disturbance is more rapid.

Mr. Lunge states that the response of the regulator would have been indicated better if the oscillogram had included the voltage across the regulator resistance. This is true, but as he goes on to say, our oscillogram shows the exciter field current to start increasing almost immediately indicating very rapid regulator response. The authors cannot subscribe to Mr. Lunge's so-called "basic types of stabilizing means." The term "damping means" should not be restricted to dashpots or eddy-current devices which are useful in bringing to rest oscillating mechanical systems. A damped system (and Mr. Lunge himself uses the word in this sense) is one whose oscillations—electrical, mechanical, temperature, etc.—do not build up to a sustained value as in hunting, but die down to zero following a disturbance. It seems logical, therefore, to use "damping means" to connote any device which will prevent hunting of a system or cause its oscillations to decay. Contrary to Mr. Lunge's statement, there is no damping means which will not appreciably retard the corrective action of a regulator.

In order to study practical regulating systems, the authors made certain assumptions as described in the paper which greatly simplify the calculation and study of such systems. Mr. Boice has pointed this out and mentions that he and his associates are making a study of a similar system with some additional assumptions taken into consideration. This work has recently been made available in the AIEE paper referred to in his discussion and although the calculations are greatly complicated by these additional assumptions, it is gratifying to the authors to note that there is a fundamental agreement in conclusions.

With reference to Mr. Boice's comments on appendix III in which the regulator has a rapid decay per cycle, but a long period of oscillation, these calculations were made assuming infinite amplification and the omission of the last damping transformer to show that a theoretically perfect regulator with good damping is possible under such assumptions. In practice, the damping transformers will saturate for large voltage swings as described above so that the regulating system will recover much faster than indicated by the calculations. The calculations hold for small amplitudes and are of significance because they show the degree of stability for the normal rather than the exceptional condition.

The value of the stability studies reported in the paper is attested by the fact that several hundred Silverstat regulators have been placed in service and only in a few instances have even minor adjustments or changes in circuit values been necessary to give stable operation. Parallel operation of generators has been accomplished likewise with no difficulty.

Predetermination of Temperatures in Resistance Welds

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ONE of the most important variables in the resistance welding process is the temperature obtained in the region of the weld. If this temperature is too low, the parts will not unite; and if it is too high, a burned weld will result. Besides the necessity for the proper temperature at the surfaces to be joined, it is often essential to bring a considerable portion of the bodies to a plastic temperature in order that the pressure exerted by the electrodes can be transmitted to the surfaces to be joined. Furthermore, the temperature gradients and the rates of heating and cooling have an important bearing on the final quality of the weld. Consequently, it is desirable to have some method by which these quantities can be calculated.

The physical complexity of this problem has made a satisfactory mathematical solution exceedingly difficult and has limited previous theoretical analyses. A simple graphical method for solving transient heat-conduction problems has been described in the literature.[1,2,3] This analysis is here extended to apply to the present problem. The extended graphical method permits many mathematically complex conditions to be handled with ease, and routine calculations are simple enough to be performed by a draftsman. The analysis applies particularly to the resistance welding of comparatively thin sheets held between electrodes of equal contact areas, and covers a large percentage of resistance welds. It evaluates the temperatures, temperature gradients, and rates of heating and cooling of a cylindrical portion of the work enclosed between the electrodes, as functions of both time and position along the axis of this cylinder. When these quantities have been determined, a knowledge of the welding characteristics of the material will permit one to predict the quality of a weld made under given conditions, or conversely, one can predetermine important factors such as the value of current and the length of the period of current conduction which are required to produce a good weld.

The necessary knowledge of the welding characteristics of a material might be obtained by comparing the quality of welds made in this material with the calculated temperatures for those welds. Subsequent calculations would, in effect, extend the results of these few preliminary tests to apply to welds made in similar material under considerably different conditions.

The use of such analytic methods of temperature calculation should promote the more exact and rational design of welding apparatus, and should facilitate the development of new welding techniques. It should be pointed out that a calculation of temperatures does not completely solve the problem, as other variables such as pressure and surface condition of the work must be considered. However, the method presented herein permits the predetermination of the most important and most difficult to calculate of these variables.

It is therefore the purpose of this paper to present a simple and fundamental graphical method for the calculation of the temperatures obtained in comparatively thin resistance-welded material, and to compare the results of some calculations with test data.

Nature of the Problem

In spot and seam resistance-welding processes, the pieces to be welded (called the "work") are gripped between electrodes under high pressure, and an electric current is passed through the material. Heat is generated at the contacts between the two pieces of work and between the work and the electrodes, as well as in the various bodies themselves. The electrodes are made of a highly conductive material, usually a copper alloy, so that relatively little heat is generated in them. Heat is carried away from the work by the electrodes, which are usually water cooled, and is conducted laterally through the work itself. As the parts to be welded become hot and plastic, the pressure exerted by the electrodes forges the pieces together, forming a strong joint under the proper temperature and pressure conditions.

Specifically, the problem is: Given

Paper number 39-13, recommended by the AIEE committee on electric welding, and presented at the AIEE winter convention, New York, N. Y., January 23-27, 1939. Manuscript submitted February 3, 1938; made available for preprinting December 5, 1938.

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[1] For all numbered references, see list at end of paper.

the initial temperatures throughout the work and the electrodes, it is required to determine the temperature distribution at any time after the beginning of the period of current conduction.

Assumptions

In order to simplify the analysis, it has been assumed that:

1. Current flow and heat flow are essentially one-dimensional. This specifies that the current density is approximately the same throughout the portion of the work between the electrodes, and that the heat flow is mainly in a direction perpendicular to the plane of the work. This condition will be approximated where the diameter of electrode contact is somewhat greater than the thickness of the sheets to be welded, provided that the electrodes have equal contact areas. This assumption covers a large number of ordinary resistance welds.
2. The density, specific heat, and thermal conductivity of the material are independent of temperature. For the temperatures and pressures encountered in resistance welding, the densities and specific heats vary a comparatively small amount. The thermal conductivity may vary over a range of two to one, so that a mean value should be chosen. Variations in electrical conductivity, which are most important particularly in the case of ordinary steels, are taken into account.

Nature of the Analysis

Calculations will be made of the temperatures obtained in that part of the work compressed between the electrodes. Imagine a cylinder enclosed in this region, coaxial with the electrodes and with a radius somewhat smaller than that of the electrode contacts, as shown in figure 1. If the diameter of the electrode contacts is somewhat greater than the thickness of the sheets to be welded, both heat and current flow in the cylinder will be entirely in the direction of its axis, and any plane normal to this axis will be at a nearly uniform temperature within the cylinder. In general, the planes nearest the contact between the sheets will be at the highest temperatures, while those nearer the electrodes will be somewhat cooler because of the heat conducted away by these bodies. The material of the work surrounding the imaginary cylinder will also be cooler due to the lateral heat conduction in the sheets and because the current flow is not entirely in the direction of the axis of the cylinder in this region, but suffers "end-effects." This distribution of temperature, as shown by crystal structure, is illustrated clearly in figure 5*c*, which is a photomicrograph of the etched cross section of a weld.

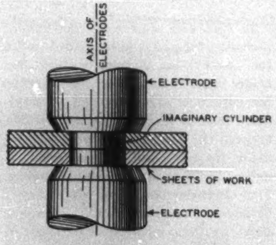

Figure 1. Cross section of weld region showing imaginary cylinder

The diameter of the imaginary cylinder is not a very definite quantity, but is somewhat less than the diameter of the actual weld. Photomicrographs of welds made in mild steel with conduction periods of 0.13 seconds and less, indicate that the diameter of the imaginary cylinder can be roughly approximated as $d_c = d_e - 1.5T$ where

d_c = diameter of imaginary cylinder
d_e = diameter of electrode contacts
T = total thickness of work

The above equation is purely empirical and since it is based on comparatively few experiments, it should be used only as a rough guide. As the diameter of the imaginary cylinder approaches zero, the calculated temperatures will be somewhat greater than those actually present, due to the lateral conduction of heat in the work.

In this analysis, the temperatures existing along the axis of the imaginary cylinder are plotted against distance measured along this axis, using a sequence of curves to represent successive instants of time. Each temperature distribution curve is derived from the one immediately preceding it by graphical constructions combined with simple calculations. Thus, building from known initial temperatures, one can determine the temperature distribution in the work for any later time, including both the heating and cooling periods. Temperatures, temperature gradients, and rates of heating and cooling can then be obtained from these curves.

This method of analysis is very flexible, and lends itself to a variety of assumed conditions, so long as both heat and current flow are essentially one-dimensional in that portion of the work lying near the axis of the electrodes. A convenient assumption is that the electrodes remain at a constant temperature. Since the electrodes are water-cooled in a majority of cases and are of a highly conductive material, this assumption should approximate the true condition. The thermal and electrical resistances of the several contacts should be taken into account. A method of calculating temperatures under these assumed conditions will be given first. An extension will then be described in which the heating of the electrodes is considered.

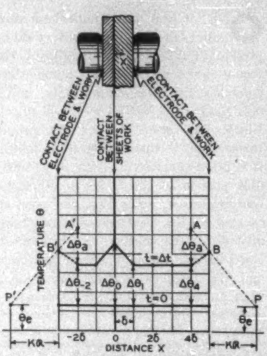
X
CONTACT BETWEEN ELECTRODE & WORK
CONTACT BETWEEN SHEETS OF WORK
CONTACT BETWEEN ELECTRODE & WORK
TEMPERATURE Θ
A'
A
B'
B
Δθa
Δθa
t=Δt
Δθ-2
Δθ0
Δθ1
Δθ4
t=0
P'
P
θe
θe
δ
-2δ
0
2δ
4δ
KR
KR
DISTANCE X

Figure 2a. Constructions for obtaining first two temperature-distribution curves

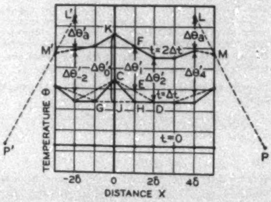

Figure 2b. Constructions for obtaining temperature-distribution curve for time t = 2Δt

Procedure in Calculating Temperatures

Simplified Method

The simplified analysis is based on the assumption that the electrodes remain at a constant temperature. Consider the electrodes and work to be sectioned through the center line of the electrodes, and the distance *x* to be measured from the plane of separation of the work out toward an electrode as shown in figure 2*a*.

The steps necessary to calculate the

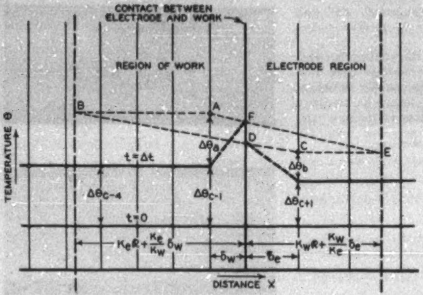

Figure 3a. Construction between work and electrode for time t = Δt; heating of electrode considered

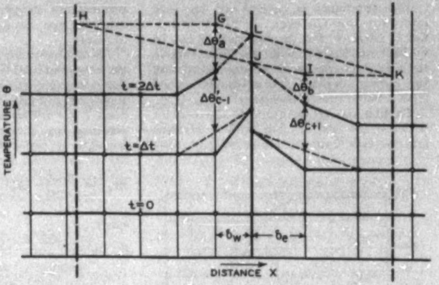

Figure 3b. Construction between work and electrode for time t = 2Δt; heating of electrode considered

temperature distribution curves for successive intervals of time are as follows:*

1. Select a space increment δ which will divide the thickness of the work into at least eight parts and which will permit all contacts (between sheets of work and between electrodes and work) to fall on lines of division, as in figure 2*a*. If this cannot easily be done, the position of one contact can be arbitrarily shifted a small amount so as to fall on the nearest line of division. The time interval between successive temperature distribution curves is then given by

$$\Delta t = \frac{c\rho\delta^2}{2K} \tag{1}$$

where

Δt = increment of time
c = specific heat of the work
ρ = density of the work
δ = length of space increment
K = thermal conductivity of the work

2. Construct temperature (θ) and distance (x) co-ordinate axes, and using a suitable scale, divide the portion of the x-axis corresponding to the work into equal intervals of length δ and indicate the position of the contacts between the sheets and between the work and the electrodes, as in figure 2*a*. If the sheets are of the same thickness the contact between them will form a plane of symmetry and constructions need be made only for the material to one side of this plane.

3. As the preliminary construction for the thermal resistance between the work and the electrodes, locate the points P and P' at a distance of $K\mathcal{R}$ length units away from the electrode contacts and at an ordinate θ_e, where

$\mathcal{R}$ = thermal resistance per unit area of the electrode contact
θ_e = temperature of the electrodes

4. A horizontal line in figure 2*a* represents the uniform temperature of the work at time $t = 0$, the beginning of the period of current conduction. The temperature distribution curve for time $t = \Delta t$ is next found as in figure 2*a*. At each of the previously marked space increments within the work but not at a contact, add to the ordinate of the horizontal line an amount

$$\Delta\theta_k = \frac{m\delta^2 J^2}{2K} r_k \tag{2}$$

where

k = an integer
$\Delta\theta_k$ = temperature increment at abscissa under consideration, $x = k\delta$
δ = length of the space increments
J = current density in the work
r_k = electrical resistivity of the material at the abscissa under consideration, $x = k\delta$
K = thermal conductivity of the work
m = a conversion factor between electrical and thermal units. If θ is in degrees Fahrenheit, J is in amperes per square inch, K is in Btu per second-inch-degree Fahrenheit, and r is in ohm-inch, then $m = 9.48 \times 10^{-4}$ Btu per watt-second

To find the temperature for time $t = \Delta t$ at the contact between the two pieces of work add to the previous temperature of the contact an amount

$$\Delta\theta_0 = \frac{m\delta^2 J^2}{2K}\left(r_0 + \frac{R_w}{\delta}\right) \tag{3}$$

where

$\Delta\theta_0$ = temperature increment at the contact between the sheets of work
R_w = electrical resistance of the contact between the sheets of work, per unit area
r_0 = electrical resistivity of the work at the contact

To find the new temperature at the contact between the work and an electrode, first locate the point A (or A') by adding to the new temperature one interval removed from the boundary an amount

$$\Delta\theta_a = \frac{m\delta J^2}{2K} R_e \tag{4}$$

where

R_e = electrical resistance between the electrode and the work, per unit area

Join with a straight line the points A and P (or A' and P'). The intersection B (or B') of this line with the contact indicates the new temperature at this point. Note that the point A does not indicate a temperature in the work but is only an auxiliary point used to find the contact temperature.

After the temperatures for time $t = \Delta t$ have been found for all intervals, these ordinates are joined to form a new temperature distribution curve.

The current density J may be taken to be either the instantaneous or the root-mean-square value. When the period of current passage is short, say one or two cycles in length, the alternations of the current and the resulting variations in heating may be considered. However, for longer periods it seems reasonable that the alternations should produce little effect, and the simpler procedure of using only the root-mean-square value of current could be followed.

5. The constructions for obtaining the temperatures at time $t = 2\Delta t$ are illustrated in figure 2*b*. To obtain a new temperature at an interior line of division, say at $x = k\delta$ where k is an integer, join with a straight line the two previous temperatures existing at the intervals $x = (k - 1)\delta$ and $x = (k + 1)\delta$ on opposite sides of the abscissa in question. To the ordinate of the intersection of this line with the abscissa $x = k\delta$ add the temperature increment

$$\Delta\theta_k' = \frac{m\delta^2 (J')^2}{2K} r_k' \tag{5}$$

where

$\Delta\theta_k'$ = new temperature increment at $x = k\delta$
J' = new value of current density in the work
r_k' = new value of electrical resistivity at $x = k\delta$

For example, to find the temperature for time $t = 2\Delta t$ at $x = \delta$, join points C and D (figure 2*b*) with a straight line, and to the intersection E with the line $x = \delta$, add an amount $\Delta\theta_1' = m\delta^2 (J')^2 r_1'/2K$ thus obtaining point F, which is the desired temperature.

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* For derivations of all constructions, see appendix II.

This procedure is carried out for each interval within the work but not on a contact. Observe that the resistivity r can be varied from point to point and from time to time as the temperature changes, and that the current density J can also be varied from time to time if necessary.

To find the temperature of the contact between the sheets, join with a straight line the two temperatures previously existing at the lines of division on opposite sides of that boundary (points G and H in figure 2*b*), and add to the intersection J of that line with the contact, the temperature

$$\Delta\theta_0' = \frac{m\delta^2(J')^2}{2K}\left(r_0' + \frac{R_w'}{\delta}\right) \qquad (6)$$

where

r_0' = new value of electrical resistivity of the work at the contact

R_w' = new value of electrical contact resistance between sheets of work, per unit area

thus obtaining the new contact temperature, point K.

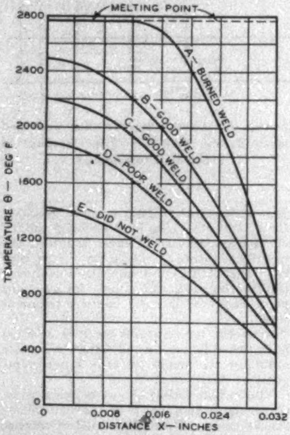

Figure 4. Calculated temperature distribution curves for ends of conduction periods; spot-welded mild-steel sheets

Curve *A*— $J = 4.38 \times 10^5$ amperes per square inch, two cycles

Curve *B*— $J = 2.42 \times 10^5$ amperes per square inch, eight cycles

Curve *C*— $J = 2.95 \times 10^5$ amperes per square inch, four cycles

Curve *D*— $J = 5.38 \times 10^5$ amperes per square inch, one cycle

Curve *E*— $J = 2.70 \times 10^5$ amperes per square inch four cycles

If the two sheets of work are identical, the construction line corresponding to GJH in figure 2*b* will be horizontal for each temperature distribution curve and calculations need be made for positive values of x only.

The temperature at the contact between the work and an electrode for time $t = 2\Delta t$ is found in the same manner as before. Referring to figure 2*b*, locate an auxiliary point L (or L') by adding to the new temperature one interval removed from the boundary an amount

$$\Delta\theta_a' = \frac{m\delta(J')^2}{2K} R_e' \qquad (7)$$

where

R_e' = new value of electrical contact resistance between electrode and work, per unit area

Draw the straight line LP (or $L'P'$). The intersection M (or M') of this line with the contact indicates the new temperature at this point.

It will be seen that the constructions given under (4) are special cases of the later constructions and differ from them only because the initial temperature distribution was a horizontal line.

When the temperatures for time $t = 2\Delta t$ have been found for all points these ordinates are joined to form the temperature distribution curve for this time.

6. This process is continued building curve upon curve for successive intervals of time, changing the current density and resistivity as necessary. When the end of the period of current conduction is reached the current density J is set equal to zero and the temperature curves are derived only by the graphical constructions. In this way the temperatures during the cooling of the work can be calculated.

The temperature distribution curve for any given time indicates the temperatures and temperature gradients in the material, and the rates of heating and cooling for any point can be derived from successive curves.

Extension of Method: Heating of Electrodes

A method for taking into consideration the heating of the electrodes will now be described. The extended method differs from that previously described mainly in that the portions of the x axis corresponding to the electrodes are now also divided into increments of equal length, and calculations are continued into the electrode regions.

The length of the intervals in the electrode regions must differ from the length of the increments in the work. Letting the subscript e refer to the electrode and the subscript w refer to the work, the relation between the increments in the two regions is given by

$$\delta_e = \delta_w \sqrt{\frac{C_w\rho_w K_e}{C_e\rho_e K_w}} \qquad (8)$$

Temperature (θ) and distance (x) co-ordinate axes are first constructed, and the space increments for the work and the electrodes are drawn, taking care to make the lengths of the increments in the two regions satisfy equation 8.

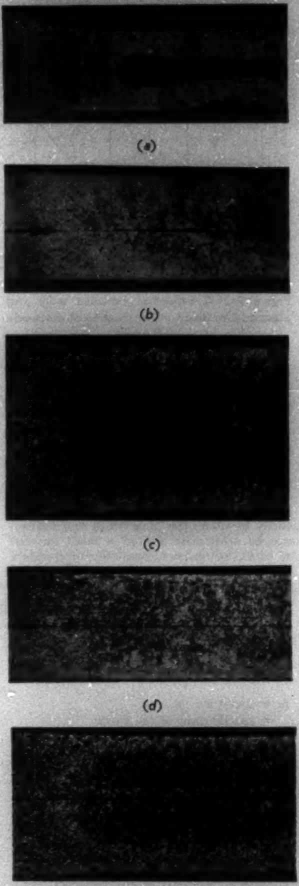
(*a*)
(*b*)
(*c*)
(*d*)
(*e*)

Figure 5. Photomicrographs of cross sections of welds. Center lines of welds at right of pictures

As a preliminary construction for the thermal resistance at the contact between the work and each electrode, draw a vertical line in the region of the work at a distance $K_e\mathcal{R} + \delta_w K_e/K_w$ from the contact line, as shown in figure 3*a*, and another vertical line in the region of the electrode at a distance $K_w\mathcal{R} + \delta_e K_w/K_e$ from the contact.

Starting with an initial uniform temperature distribution for time $t = 0$, the temperatures in the interior of the work and the electrodes are found for time $t = \Delta t$ by the method first described for interior points, and the construction for the contact between the sheets of work remains the same as before. At the contact between an electrode and the work the construction shown in figure 3a is necessary. To the new temperature one interval removed from the electrode contact and in the region of the work, add an amount

$$\Delta\theta_a = \frac{m\delta_w J^2}{2K_w} R_e \qquad (9)$$

thus obtaining the new ordinate A in figure 3a. To the temperature in the electrode region one interval removed from the contact, add the temperature

$$\Delta\theta_b = \frac{m\delta_e J^2}{2K_e} R_e \qquad (10)$$

obtaining a new ordinate C in figure 3a. Draw the horizontal lines AB and CE, and from the intersections of these lines with the vertical construction lines, draw the straight lines AE and BC. The intersection F of the line AE with the contact indicates the new temperature on the work side of the contact, and the intersection D of the line BC with the contact indicates the new temperature on the electrode side of the contact. The temperature distribution curve is then completed as shown by the heavy dotted lines.

The temperature distribution curve for time $t = \Delta t$ is then used to construct the temperature curve for time $t = 2\Delta t$, as shown in figure 3b. Interior temperatures are found by the method described in a previous section. To obtain the electrode contact temperature, locate point G by adding to the temperature at that abscissa the amount

$$\Delta\theta_a' = \frac{m\delta_w (J')^2}{2K_w} R_e' \qquad (11)$$

where

J' = new value of current density
R_e' = new value of electrical resistance between the electrode and the work, per unit area

Locate point I by adding to the ordinate at that abscissa the temperature

$$\Delta\theta_b' = \frac{m\delta_e (J')^2}{2K_e} R_e' \qquad (12)$$

Draw the horizontal lines GH and IK, and the straight lines GK and HI. The intersection L of GK with the contact indicates the temperature on the work side of the contact, and the intersection J of HI with the contact indicates the temperature on the electrode side of the contact. The temperature curve is completed as shown by the heavy dotted lines.

By building each temperature distribution curve upon the one preceding it, the process can be continued until the desired period of time has been covered.

Comparison With Test Results

In order to test the method of analysis and to check the assumptions, calculations were made for the spot welding of low-carbon-steel sheets for various values of current density and for different lengths of current passage, and test welds were made under these conditions. Low carbon steel was chosen for these welds because its properties were quite well known and because its steeply rising electrical resistivity versus temperature characteristic would impose a severe test on the analysis. Since the direct measurement of temperatures in the welds would be quite difficult, and since the temperatures evidently bear a close relation to weld quality, the calculated maximum temperatures in the region of the weld were compared directly with the quality of the test welds. Photomicrographs were made of the cross sections of the various welds, and the structures which these revealed were also compared with the calculated temperatures.

Each test weld was made between two sheets of 0.032-inch low carbon steel, using electrodes with a contact diameter of about 0.25 inches. The electrode pressure in each case was approximately 700 pounds. Calculations of current density were based on the carefully measured actual area of contact between the electrodes and the sheets. A lack of data on the thermal resistance of contacts under high pressures made necessary the assumption that the electrical and thermal resistances of a contact bear the same approximate relation as do the electrical resistivity and the thermal resistivity (reciprocal of thermal conductivity) of the work. To simplify the calculations, the electrode temperatures were assumed to remain constant.

The temperatures calculated for the end of the period of current conduction for several cases are shown in figure 4, and photomicrographs of the cross sections of these welds are shown in figure 5. The interior temperatures necessary to produce a good weld check quite well for widely varying conditions of current and time, as do the temperatures calculated for the dark regions of carbide precipitation shown in the photomicrographs of figure 5. It appears that a maximum temperature of over 2,000 degrees Fahrenheit is necessary to produce a good weld in this material. It is interesting to observe that curve A of figure 4 indicates that a considerable portion of the work had reached melting temperature, and that the corresponding photomicrograph (figure 5a) shows a proportional reduction in the thickness of the work at the weld. Curve B of figure 4 indicates that the proper welding temperatures have been obtained, and this weld was indeed quite strong, although the photomicrograph (figure 5b) does not show the grain structure ordinarily considered typical of a good weld. Higher magnification showed that the dark line of apparent separation was really composed of fine interlaced grains, forming a strong weld. Curves C, D, and E of figure 4 illustrate strikingly the differences between the calculated temperatures in good and poor welds, and the corresponding photomicrographs (figure 5c, d, e) show the expected differences in structure.

The maximum temperatures calculated for the interior of these welds are seen to compare quite well with the quality of the welds. Furthermore, the calculated temperatures are reasonable in value and agree with the observations of other investigators. Larsen[4] states that recrystallization across the plane of contact is the fundamental phenomenon of resistance welding. Recrystallization is favored by a state of strain and occurs in steel at temperatures somewhat below the melting point of approximately 2,800 degrees Fahrenheit. Larsen mentions

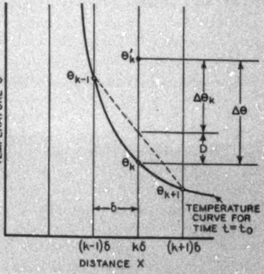

Figure 6. Graphical construction for obtaining temperature increment during time Δt

welding temperatures of from 1,750 degrees Fahrenheit to 2,400 degrees Fahrenheit for mild steel, and Sparagen and Claussen[6] give data deduced from microstructural observation which indicate maximum temperatures of about 2,300 degrees Fahrenheit, which check quite well with the present results.

Effect of Pressure and Contact Area

The exact nature of a resistance weld in ferrous materials is difficult to determine, since the physical metallurgy of metals under high pressures has been investigated but little, and any discussion of this subject must therefore be somewhat speculative. It is reasonable to believe, however, that the welding operation in ferrous materials is a forging process, in which the adjacent portions of the bodies are brought to a sufficiently plastic state while under pressure, thus causing the metals to unite.

It should be pointed out that both pressure and temperature are requisite to the production of a good weld. It is well known that a weld in heavy materials requires comparatively low currents and long times of application, so that the metal will be heated throughout its thickness. Apparently this is necessary in order to render the material so plastic that sufficient pressure will be transmitted to the surface between the bodies to cause the forging action to take place. It has been observed that spot welders with light heads require less spring pressure to produce a good weld than do welders with heavy heads, under otherwise similar conditions. The inertia of the heavier head prevents it from "following in" when the work has become plastic, and deforms readily. As a result, the pressure actually applied to the high temperature region may be insufficient to forge the materials together unless the spring pressure is quite large. This again illustrates the importance of pressure as well as temperature in the production of a good weld.

Because the heat generated per unit volume of the work varies as the square of the current density, it is necessary to know the electrode contact area, as well as the total current, quite accurately, in order to make satisfactory calculations. This is shown by curves *C* and *E* of figure 4, between which the current density varied less than ten per cent. The effect is here accentuated, of course, by the sharply rising electrical resistivity versus temperature characteristic of the low carbon steel. These curves illustrate the fact that accurate control of electrode contact area is necessary in order to secure consistently good welds with a given transformer tap and conduction period. This necessity has already been emphasized by welding engineers.

In the case of electrodes of small contact area and with work of considerable thickness, the assumptions used in this analysis will not be valid, since current density will not be uniform and heat flow in the plane of the work may be considerable. To cover these cases, some sort of three-dimensional treatment may be necessary.

Conclusions

The method of analysis described herein is believed to be based upon fundamentally correct principles, and has been shown by comparison with test results to permit a prediction of the temperatures which will be obtained in comparatively thin resistance-welded material. The relation between temperature and weld quality for a given material can be established by comparing preliminary calculations with the results of comparatively few tests. Predictions of weld quality can then be extended to other thicknesses of this material and to various currents and periods of current conduction without further tests.

The constructions are simple, and routine calculations can be made by a draftsman. If desired, the computations can be carried out numerically in tabular form, instead of using the graphical constructions described in this analysis.

Although this method of analysis has been used only in spot-welding calculations, it should apply also to seam welding, and, under some conditions, to butt welding. The use of such methods of analysis should facilitate the development of welding processes, and should be an aid in the design of new welding apparatus.

Appendix I. Nomenclature

C = specific heat
C_e = specific heat of the electrode
C_w = specific heat of the work
D = temperature increment obtained by graphical construction
J = current density
K = thermal conductivity
K_e = thermal conductivity of the electrode
K_w = thermal conductivity of the work
k = an integer
m = conversion factor between electrical and thermal units
Q = rate of heat conduction across a plane, per unit area
q = rate of heat generation, per unit volume
R_e = electrical resistance per unit area of the electrode contact
R_w = electrical resistance per unit area of the contact between sheets of work
$\mathcal{R}$ = thermal resistance per unit area of the electrode contact
r_k = electrical resistivity of the material at the abscissa $x = k\delta$
r_0 = electrical resistivity of the work at the contact between sheets
t = time measured from the beginning of current conduction
Δt = time increment between successive temperature distribution curves
x = distance measured from the contact between sheets of work, parallel to heat flow
δ = length of space increment
δ_e = length of space increment in the electrode region
δ_w = length of space increment in the region of the work
θ = temperature
θ_{ce} = temperature of the electrode at the plane of contact
θ_{cw} = temperature of the work at the electrode contact
θ_e = electrode temperature
θ_{e+1} = electrode temperature one interval removed from the plane of the contact
θ_{e-1} = temperature of the work one interval removed from the electrode contact
θ_k = temperature existing at the abscissa $x = k\delta$
$\Delta\theta$ = total temperature increment during time Δt
$\Delta\theta_a$, $\Delta\theta_b$ = temperature increments to obtain construction points
$\Delta\theta_k$ = temperature increment at abscissa $x = k\delta$ due to generation of heat
$\Delta\theta_0$ = temperature increment at contact between sheets due to generation of heat
ρ = density of the material
ρ_e = density of the electrode
ρ_w = density of the work

Primed quantities refer to the values of increments and parameters occurring at a later time than the unprimed quantities.

Appendix II. Derivations

Constructions for Interior of Body

This analysis depends upon the replacement of first and second derivatives in the heat-flow equations with quotients of finite differences. By this replacement a step-by-step approximate solution may be obtained.

The partial differential equation of heat flow for the case of constant thermal conductivity can be shown to be

$$C\rho\frac{\partial\theta}{\partial t} = K\left(\frac{\partial^2\theta}{\partial x^2}+\frac{\partial^2\theta}{\partial y^2}+\frac{\partial^2\theta}{\partial z^2}\right)+q \qquad (13)$$

where

C = specific heat
ρ = density
K = thermal conductivity
θ = temperature
t = time
q = rate of heat generation per unit volume

Where the heat flow is unidimensional equation 13 reduces to

$$C\rho\frac{\partial\theta}{\partial t} = K\frac{\partial^2\theta}{\partial x^2}+q \qquad (14)$$

By definition of a derivative

$$\frac{\partial\theta}{\partial t} = \lim_{\Delta t\to 0}\frac{\Delta\theta}{\Delta t} \qquad (15)$$

where

$\Delta\theta$ = finite increment of temperature
Δt = finite interval of time

or approximately

$$\frac{\partial\theta}{\partial t} = \frac{\Delta\theta}{\Delta t} \qquad (16)$$

provided that the time increment Δt is taken to be reasonably small. If we now divide the desired span of x into equal small intervals of width δ we can approximate the temperature gradients at points $x = k\delta$ and $x = (k+1)\delta$ in figure 6 by

$$\left.\frac{\partial\theta}{\partial x}\right|_{k+1} = \frac{\theta_{k+1}-\theta_k}{\delta}$$

and

$$\left.\frac{\partial\theta}{\partial x}\right|_{k} = \frac{\theta_k-\theta_{k-1}}{\delta} \qquad (17)$$

where θ_{k+1} θ_k and θ_{k-1} are the temperatures existing at time $t = t_0$ at the abscissas $(k+1)\,\delta$, $k\delta$, and $(k-1)\delta$, respectively.

By definition,

$$\left.\frac{\partial^2\theta}{\partial x^2}\right|_k = \lim_{\delta\to 0}\left(\frac{\left.\frac{\partial\theta}{\partial x}\right|_{k+1}-\left.\frac{\partial\theta}{\partial x}\right|_{k}}{\delta}\right)$$

or approximately, provided that δ is quite small, we obtain by substituting equations 17,

$$\left.\frac{\partial^2\theta}{\partial x^2}\right|_k = \frac{\theta_{k+1}+\theta_{k-1}-2\theta_k}{\delta^2} \qquad (18)$$

Substituting the approximations expressed by equations 16 and 18 into the differential equation 14, we obtain for the increase in temperature at $x = k\delta$ during time Δt,

$$\Delta\theta = \frac{K\,\Delta t}{C\rho\delta^2}\,(\theta_{k+1}+\theta_{k-1}-2\theta_k)+\frac{q\,\Delta t}{C\rho} \qquad (19)$$

For convenience of graphical construction, we shall let

$$\frac{K\,\Delta t}{C\rho\delta^2} = \frac{1}{2} \qquad (20)$$

For any given material, this will fix the time increment once the space increment δ is chosen. Substituting condition 20 into equation 19, there is

$$\Delta\theta = \frac{1}{2}\,(\theta_{k+1}+\theta_{k-1}-2\theta_k)+\frac{\delta^2 q}{2K} \qquad (21)$$

The rate of heat generation is

$$q = mJ^2r_k \qquad (22)$$

where

J = current density
r_k = resistivity of the material at $x = k$
m = conversion factor between electrical and thermal units

If q is in Btu per second-inch^3, J is in amperes per inch^2, and r_k is in ohm-inch, then $m = 9.48 \times 10^{-4}$ Btu per watt-second.

Substituting equation 22 into equation 21, we obtain for the total temperature increase at $x = k\delta$ during the time Δt,

$$\Delta\theta = \frac{1}{2}\,(\theta_{k+1}+\theta_{k-1}-2\theta_k)+\frac{m\delta^2J^2r_k}{2K} \qquad (23)$$

Consideration of figure 6 will show that the quantity

$$\frac{1}{2}\,(\theta_{k+1}+\theta_{k-1}-2\theta_k)$$

is the difference between the ordinate θ_k at x_0 and the chord joining θ_{k-1} and θ_{k+1}, and may be obtained graphically.

Equation 23 can be rewritten as

$$\Delta\theta = D+\Delta\theta_k$$

where

$$D = \frac{1}{2}\,(\theta_{k+1}+\theta_{k-1}-2\theta_k) \qquad (24)$$

$$\Delta\theta_k = \frac{m\delta^2J^2}{2K}\,r_k$$

This affords a convenient graphical construction. Given the temperature distribution at time t_0, the temperature of a plane, say at $x = k\delta$, for time $t_0 + \Delta t$, can be found by first joining the ordinates θ_{k-1} and θ_{k+1} with a straight line (figure 6). The intersection of this line with the trace $x = k\delta$ indicates the temperature which this point would have attained at time $t_0 + \Delta t$ if no heat were generated in the element, and represents the increment D. If to this ordinate is added the quantity $m\delta^2J^2r_k/2K$ as indicated by equation 24, the result will be the temperature θ_k' at time $t_0 + \Delta t$. In this way a new temperature distribution for the time $t_0 + \Delta t$ can be constructed from the temperature curve for time t_0.

It will be observed that the resistivity r_k can be regarded as varying from increment to increment and from time to time. This variation can be determined most conveniently from a resistivity versus temperature curve.

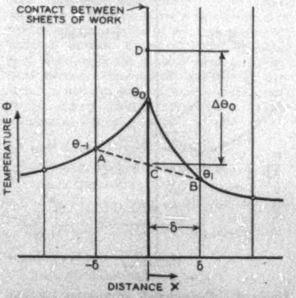

Figure 7. Construction at contact between sheets of work

Boundary Conditions

The above-described conditions will suffice for the interior of a body, but further considerations are necessary for the boundaries.

CONTACT BETWEEN SHEETS OF WORK

The construction at the contact between sheets of work is similar to that used for the interior of the sheets, except that the heat generated by the electrical resistance of the contact must be added to that generated in this increment by the electrical resistivity of the work. Since the constructions are based on the space increment as a unit, the electrical contact resistance may be reduced to an equivalent electrical resistivity distributed throughout the space increment at the contact. The rate of heat generation per unit volume of the work in this increment is given by the expression

$$q = mJ^2\left(r_0+\frac{R_w}{\delta}\right) \qquad (25)$$

where

R_w = electrical resistance of the contact between the sheets of work, per unit area

Substituting equation 25 into 21, the equation which applies to the temperature increment at the contact is found to be

$$\Delta\theta = D+\Delta\theta_0 \qquad (26)$$

where

$$D = \frac{1}{2}\,(\theta_1+\theta_{-1}-2\theta_0)$$

$$\Delta\theta_0 = \frac{m\delta^2J^2}{2K}\left(r_0+\frac{R_w}{\delta}\right)$$

This indicates that to obtain the new temperature at the contact between the sheets of work, connect with a straight line the previous temperatures existing on opposite sides of the junction (points A and B in figure 7), and to the ordinate (C) of the intersection with the contact, add the temperature increment $\Delta\theta_0$ as given by equation 26. The resulting ordinate (D) will indicate the new temperature at the contact. Observe that if the construction is symmetrical about the contact, as would be the case with two identical sheets of work, points A and B will be at the same ordinate. It is then necessary to make calculations for the material to one side of the junction only, and since the line BC is then horizontal, the temperature increment at the contact is measured above this ordinate.

CONTACT BETWEEN WORK AND ELECTRODE: ELECTRODE AT CONSTANT TEMPERATURE

The contact between the electrode and the work is considered to offer a resistance to the flow of both heat and current, and the temperature of the electrode is here assumed to be constant. The thermal resistance of the contact is defined by the equation

$$\mathcal{R} = \frac{\theta_{cw} - \theta_e}{Q} \tag{27}$$

where

$\mathcal{R}$ = thermal resistance of the contact, per unit area
Q = rate of heat conduction across the contact, per unit area
θ_{cw} = temperature of the work at the contact
θ_e = temperature of the electrode

The heat passing across the thermal resistance of the contact is taken to be equal to the heat conducted to the contact from the work plus one-half of the total heat generated at the contact. This is equivalent to assuming that heat is generated uniformly throughout the thickness of the contact resistance. The rate of heat conduction is, from elementary theory, equal to $-K\partial\theta/\partial x$, so the heat crossing the contact is given by the expression

$$Q = \frac{K(\theta_{c-1} - \theta_{cw})}{\delta} + \frac{mJ^2R_e}{2} \tag{28}$$

where

R_e = electrical resistance of the contact between the electrode and work, per unit area
θ_{c-1} = temperature of the work one interval removed from the contact

Eliminating Q between (27) and (28), we obtain the expression

$$\frac{\theta_{cw} - \theta_e}{K\mathcal{R}} = \frac{\theta_{c-1} - \theta_{cw} + \Delta\theta_a}{\delta} \tag{29}$$

where

$$\Delta\theta_a = \frac{m\delta J^2R_e}{2K} \tag{30}$$

Equation 29 suggests the similar triangle construction shown in figure 8. Here we see that

$$\frac{BD}{DP} = \frac{AC}{CB}$$

which implies the relation expressed by equation 29. In this construction the point P is located at a distance $K\mathcal{R}$ length units away from the contact and at a temperature θ_e. Given the temperatures θ_{c-1} and θ_e at time t, the temperature at the junction, θ_{cw}, is determined by adding to the ordinate θ_{c-1} an amount $\Delta\theta_a$, thus obtaining the point A, and drawing the straight line AP. The intersection B of the straight line with the contact indicates the temperature of the junction, θ_{cw}.

CONTACT BETWEEN WORK AND ELECTRODE: HEATING OF ELECTRODE CONSIDERED

In this construction, both the electrodes and the work are divided into increments, and calculations are carried into the electrode regions.

The time interval, Δt, between successive temperature curves, must be the same for both electrode and work regions. Letting the subscripts e and w refer to the electrode and work respectively, we obtain from equation 20 the relation

$$\Delta t = \frac{C_e\rho_e\delta_e^2}{2K_e} = \frac{C_w\rho_w\delta_w^2}{2K_w} \tag{31}$$

To satisfy equation 31, the space increments in the two regions must be in the ratio

$$\frac{\delta_e}{\delta_w} = \sqrt{\frac{C_w\rho_wK_e}{C_e\rho_eK_w}} \tag{32}$$

The heat carried across the contact between the work and an electrode is taken, as before, to be equal to the heat conducted to the contact from the work plus one-half of the heat generated in the electrical resistance of the contact. Thus,

$$Q = \frac{K_w}{\delta_w}(\theta_{c-1} - \theta_{cw}) + \frac{mJ^2R_e}{2} \tag{33}$$

where

Q = rate of heat conduction across the contact
Q_{cw} = temperature of the work at the contact
θ_{c-1} = temperature of the work one interval removed from the contact
K_w = thermal conductivity of the work
R_e = electrical resistance of the contact between the work and the electrode, per unit area

The heat conducted from the contact into the electrode is equal to that passing through the contact plus the remainder of the heat generated at the contact, and may be expressed as

$$Q + \frac{mJ^2R_e}{2} = \frac{K_e}{\delta_e}(\theta_{ce} - \theta_{c+1}) \tag{34}$$

where

K_e = thermal conductivity of the work
θ_{ce} = temperature of the electrode at the contact
θ_{c+1} = temperature of the electrode one interval removed from the contact

The heat passing through the thermal resistance of the contact causes a temperature difference between the work and electrode sides of the contact, or

$$Q = \frac{\theta_{cw} - \theta_{ce}}{\mathcal{R}} \tag{35}$$

where

$\mathcal{R}$ = thermal resistance of the contact, per unit area

Equate (33) and (35) and obtain

$$(\theta_{cw} - \theta_{ce})\delta_w = K_w\mathcal{R}\left(\theta_{c-1} - \theta_{cw} + \frac{m\delta_wJ^2R_e}{2K_w}\right) \tag{36}$$

Eliminating Q between equations 33 and 34 and solving for θ_{ce}, there is

$$\theta_{ce} = \theta_{c+1} + \frac{\delta_eK_w}{\delta_wK_e} \times \left(\theta_{c-1} - \theta_{cw} + \frac{m\delta_wJ^2R_e}{2K_w}\right) \tag{37}$$

Substituting equation 37 into equation 36 to eliminate θ_{ce} and simplifying, we obtain the relation

$$\frac{\theta_{cw} - \theta_{c+1} - \Delta\theta_b}{K_w\mathcal{R} + \dfrac{K_w}{K_e}\delta_e} = \frac{\theta_{c-1} - \theta_{cw} + \Delta\theta_a}{\delta_w} \tag{38}$$

where

$$\Delta\theta_a = \frac{m\delta_wJ^2}{2K_w}R_e$$
$$\Delta\theta_b = \frac{m\delta_eJ^2}{2K_e}R_e \tag{39}$$

which will be used in the construction to obtain θ_{cw}.

Now eliminate Q between equations 34 and 35 and between equations 33 and 34, and from the resulting two equations eliminate θ_{cw}. The result can be expressed as

$$\frac{\theta_{c-1} - \theta_{ce} + \Delta\theta_a}{K_e\mathcal{R} + \dfrac{K_e}{K_w}\delta_w} = \frac{\theta_{ce} - \theta_{c+1} - \Delta\theta_b}{\delta_e} \tag{40}$$

which is the relation which will be used in the construction to obtain θ_{ce}.

Figure 9 illustrates the procedure for this case. A construction line is first drawn in the region of the work, parallel to the temperature axis, at a distance $K_e\mathcal{R} + \delta_wK_e/K_w$ from the contact, and a similar line is drawn in the region of the electrode at a distance $K_w\mathcal{R} + \delta_eK_w/K_e$ from the contact.

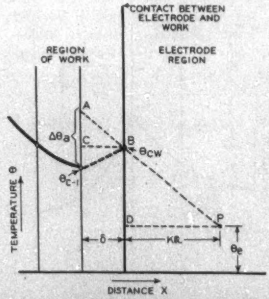

Figure 8. Construction at contact between work and electrode; electrode at constant temperature

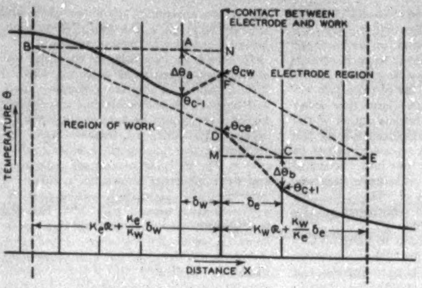

Figure 9. Construction at contact between work and electrode; heating of electrode considered

Given the temperatures θ_{c-1} and θ_{c+1} at the intervals on opposite sides of the contact, add to the ordinate θ_{c-1} an amount $\Delta\theta_a$ and add to θ_{c+1} an amount $\Delta\theta_b$, thus obtaining the new ordinates *A* and *C*.

Draw the horizontal line *CE*, and connect *E* and *A* with a straight line. The intersection *F* of this line with the contact indicates the temperature of the work at the contact, θ_{cw}. Inspection of figure 9 will show that, by similar triangles, $FM/ME = FN/NA$, which is the relation expressed by equation 38.

Draw the horizontal line *AB* and connect *B* with *C*. The intersection *D* of *BC* with the contact indicates the electrode temperature at the contact, θ_{ce}. In this construction, $DN/NB = DM/MC$, which is the relation given by equation 40.

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Discussion

R. H. Harrington (nonmember; General Electric Company, Schenectady, N. Y.): Mr. Johnson presents in this paper some very fine work concerning a difficult problem. The fundamental-research committee of the American Welding Society is extremely interested in this specific problem and the writer commends this fine paper to their detailed attention.

One of the fundamental problems still beset by disagreement among resistance-welding engineers is just this: "Is the best resistance weld achieved by actual melting at the interface of the work or by grain growth across this junction while the steel is in a plastic condition?"

From the metallurgical viewpoint the writer fully agrees with Mr. Johnson that the best welds should result from grain growth across the junction of the work while the steel is under pressure in the plastic range. Some applications, it is true, will require actual melting at the interface because of inherent conditions but these applications must then be satisfied with properties inferior to those of welds produced in the solid state. Of course, these statements apply only to steels.

Until quite recently we have all of us largely neglected the effect of strain (internal tension and pressure) on reactions in the solid state in metals and alloys. It is good to have Mr. Johnson call attention to this fact. The rates of grain growth, diffusion, and phase reactions are bound to be affected in some degree when the system is kept under a continuously applied strain. The tendency has been, in application of the phase rule to alloy systems, to assume that the pressure in the system is atmospheric when this is rarely true, and, in the case of resistance welding, far from true.

R. J. Bondley (nonmember; General Electric Company, Schenectady, N. Y.): The paper "Prediction of Temperatures in Resistance Welds" is an excellent bit of mathematical analysis. The author has verified by calculation the facts that have been found by experiment. The paper should be of interest to welding engineers or students doing advanced work on resistance welding. I feel, however, that the paper is of more academic interest than of a practical nature. The whole analysis is constructed around certain assumptions, such as a known contact resistance. These resistances will vary with:

1. Kind of material being welded.
2. Surface condition of the metals.
3. Nature of surface and size of electrodes.
4. Pressure of electrodes, especially during weld.

The pressure during weld is affected by:

1. Inertia of the moving parts.
2. Bearing friction of moving parts.
3. Inductive reaction of the welding arms.

When one considers that a ten per cent change in current may mean the difference between a good and a poor weld, it is clear that the assumptions may be well over ten per cent off.

As for the predetermination of welding capacity as claimed by the author, capacity of welders for any application is well known to persons acquainted with the resistance welding art.

Likewise as to quality of welds, I believe that with modern welding equipment, the results can be obtained much quicker and with greater accuracy by experiment than by calculation.

L. G. Levoy (General Electric Company, Schenectady, N. Y.): Mr. Johnson is to be congratulated for applying a method of analysis to this difficult problem, and seeing it through in spite of the difficulties presented by unknown relations, including variations in parameters which are beyond the scope of the analysis.

The value of such calculations lies in their ability to show what effect variation in time, current density, and other variables considered, have upon the work. Prior to now most ideas concerning these factors were more or less empirical and based mainly upon experimental work. Application of this analysis, though not precise, will give a better understanding of what goes on in making welds. This will lead to more valuable experimentation and to the production of better welds and better welding conditions.

Resistance-welding engineers and metallurgists will welcome this study, as it provides them with an analytical approach to the internal conditions existing during the welding process.

H. D. Snively (General Electric Company, Schenectady, N. Y.): Mr. Johnson is to be congratulated for his courage in attacking such a problem as this one and for his persistence in obtaining a solution in spite of the large number of variables which have frightened away many other investigators. We are glad to see that he was able to apply the graphical method of analysis to the problem, and we welcome the opportunity to make some general remarks concerning the method. Over a period of several years we have had considerable experience with the method and have developed it for use on various special problems; and we hope to be able to publish some notes in the near future describing more completely this type of analysis and some of its applications to problems of a more difficult nature than those of one-dimensional heat flow.

We agree with the author's conclusion that the graphical method can very readily be taught to some previously uninitiated person so that he can carry out the routine graphical constructions necessary to solve any particular problem. In fact, we have had some experience in teaching the method and have found that a half hour's description of the process will usually serve completely to acquaint the person with the method so that he can carry out the solution for any particular problem although he might not be able to formulate the routine by himself.

The advanced course in engineering of the General Electric Company has on various occasions during the last several years solved complex heat conduction problems by the use of an extended form of the analysis used by Mr. Johnson, and we have noted that in most cases, after the routine procedure of solution was once launched upon, the time required to carry the solution through to a finish was a matter of three or four hours. For more simple problems the time is considerably less. The method could therefore be described as being monotonous but not excessively time-consuming.

To check the accuracy of the method of analysis we have solved problems by the graphical method which could also be solved analytically, such as the problem of transient heating of a long circular cylinder suddenly subjected to a uniform temperature all over the cylindrical surface. In this particular problem we found that when the space region (here the radius of the cylinder) was divided into ten equal segments, ten increments of time in the process of solution would bring the graphical solution to within ten per cent of the accuracy of the exact analytical solution. Twenty-five steps in time would bring the accuracy to within five per cent and 50 steps would make the graphical solution almost coincide with the exact solution. Other problems that we have dealt with indicate the same degree of accuracy. Of course an increase in the number of space increments would improve the accuracy but it also would decrease the size of the time increment so that more steps would have to be taken to cover a given period of time. In general some compromise between accuracy and time available for solution will have to be made. The conclusion is, therefore, that within the accuracy of the assumptions on any particular problem the graphical method can give very accurate results indeed.

The method could have been adapted to take into account such things as the variation of thermal conductivity which as the author has pointed out has a range of about two to one in his problem. However, the amount of work required to obtain the solution would have been greatly increased, and probably his assumption of an average thermal conductivity leads to good enough results. Furthermore, the method could have been modified so as to take into account approximately the heat flow away from the assumed cylindrical region of the weld. That is, instead of considering the heat to flow linearly from one electrode to another, the heat flow out into the metal adjacent to the weld could have been approximately taken care of. Here, too, the probable effect of this refinement on the result would be negligible.

Again we take occasion to congratulate our former colleague, Mr. Johnson, on his excellent paper, and we hope to find that the graphical method will be applied by others in solving still other types of problems.

Walter C. Johnson: The author appreciates very much the discussions of his paper, some of which require no further comment. He is pleased to find that Mr. Snively agrees with him concerning the accuracy and simplicity of the graphical method of analysis. The method undoubtedly looks more complicated on paper than it really is. A complete understanding of the analysis requires some knowledge of mathematics, but the operations themselves are relatively simple and can be readily learned without such knowledge. Mr. Snively's account of his checks on the accuracy of the graphical method are also enlightening.

As Mr. Snively points out, the assumptions of a constant thermal conductivity and of an essentially one-directional heat flow in a central region are not essential to the graphical analysis, but can be eliminated, at the expense, of course, of a more complicated and laborious solution. These refinements will probably be desirable in some types of problems, for example, when the work is of considerable thickness. The author has thought it desirable to gain additional experience in the application of the simpler analysis to specific problems and to determine more exactly its limitations before proceeding to these refinements.

It should be remembered that the test calculations given in the paper are affected by the values of contact resistance used, and since these values are but imperfectly known, one cannot be sure of the accuracy of the final calculated temperatures. Investigators are turning their serious attention to the problem of contact resistance, and with increasing knowledge in this field, more accurate calculations will be possible.

The author agrees with Mr. Bondley that the capacity of present welding equipment is well known from experience. However, the development of new methods would be facilitated by a better understanding of what takes place in a resistance weld, and by a better knowledge of the relative effect of the various important factors. A case in point is the problem for which this analysis was originally developed, in which the root-mean-square current fluctuated widely during the welding period, so that the requirements were considerably different than for an ordinary weld. This analysis was developed to permit the design of the necessary apparatus without resorting to expensive cut-and-try procedure. As for the difficulties engendered by the variation in parameters: since these factors can be controlled well enough to make consistently good welds possible, their values can probably be determined with enough certainty for successful calculation. Furthermore, the effect of a variation in parameters could probably be determined more conveniently by analytical methods than by experiment alone, since the factors could be varied at will in calculations.

The practical significance of a method of calculating temperatures would seem to lie not so much in the calculation of each set of welds in a production line as in the possibility of a better understanding of fundamental resistance-welding problems, and in the provision of a helpful tool for their solution.

Electrical Equipment Used in Reflection-Seismograph Prospecting

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Synopsis: The reflection prospecting method measures depths to subsurface strata by recording the travel time of reflected sound waves. The acoustic energy of the sound wave is converted into electrical energy by a pickup, amplified, filtered, and recorded. The equipment described provides automatic gain control in the amplifier, filtering to eliminate frequencies outside the reflection spectrum, and means of recording reflections from several points on the earth's surface, together with the necessary timing mechanism.

The Nature of Reflection Seismograph Prospecting

IN THE present-day search for petroleum, the reflection seismograph is an important and powerful tool. The apparatus used is predominantly electrical, and as such should be of interest to all electrical engineers, particularly those having an especial interest in the electronic and communication branches of electrical engineering.

Before considering the instruments used in reflection work, it is well to learn something of the purpose for which they are used. The principle of operation of the reflection seismograph is identical with that of the sonic depth finder. That is, depths to subsurface strata are calculated from the times required for sound waves to travel down to them and be reflected back to the surface. Reflection seismograph prospecting consists in thus measuring the depths of the various strata at many stations over a large area, mapping their folding and faulting in the hunt for likely traps for petroleum. The depths in which the geophysicist is interested range from a few hundred to 10,000 or 20,000 feet. Since the acoustic attenuation of earth is much greater than that of water, a sound wave of high initial amplitude must be transmitted in order that the reflected wave will be larger than the natural unrest of the earth's surface. This energy is usually released by exploding a charge of dynamite at the bottom of a hole drilled in the ground to a depth of 40 or 50 feet, with the charge covered with water or dirt. The charge is fired on a signal from the recording station, using special equipment which at the instant of explosion transmits a signal, known as the time of break, to the recording station.

The explosion of the charge starts a sound wave in the earth which is initially of roughly spherical wave front. As this wave front expands and passes through formations having various acoustic properties, it becomes distorted and part of its energy is reflected from and refracted at each interface between beds of differing velocities of propagation. Upon the arrival of the waves at the earth's surface, their acoustic energy is converted into electrical energy by a pickup, amplified, filtered, and recorded on an oscillograph, called in geophysical terminology a recording camera or simply camera. Also recorded are the time of break and timing lines.

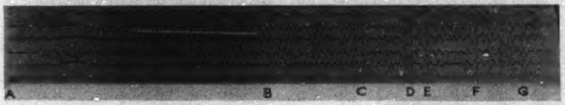

Figure 1. A six-channel reflection seismogram

Identification of Reflected Waves

In order to identify those waves which have suffered reflection, in the presence of refracted and direct disturbing waves, the practice is to record the arrival of the reflected waves at several different points in different directions and at different distances from the shot point. The distances are usually small compared to the depth from which it is desired to record reflections. Since the reflected wave front has a large radius of curvature (approximately twice the depth of the reflecting horizon) and usually arrives from almost directly below the recording station, it strikes the several pickups at almost the same time and the character of the movement of the surface will be nearly the same at all pickups. This sameness of appearance and arrival time is the chief criterion by which reflected waves are identified. Figure 1 shows a typical reflection-seismograph record. Six channels are used, the six pickups being placed in line with the shot point and 200 feet apart. At the point *A* is the time of break, when the charge was exploded. The first waves recorded on the lower six lines correspond to the arrival of the first disturbances of the earth's surface at the six pickups. The first wave on the uppermost trace is a direct wave because that pickup was very near the shot. On the other five the first wave arriving was refracted and traveled most of the way along the top of the first unweathered bed below the surface. In the region marked *B* the first identifiable reflection arrived. Other reflections are marked, the last marked *G*, arriving 2.323 seconds after the break, corresponding to a depth of approximately 10,150 feet.

From the record of figure 1 it is evident that a reflection is a transient disturbance and is therefore composed of waves of various frequencies. The predominant frequency of reflected waves generally ranges between 30 and 70 cycles per second. The frequencies of the major components of direct and refracted waves are, in general, below 30 cycles, and the frequencies of extraneous surface disturbances are usually higher than 70 cycles. It is usual practice to design reflection instruments to reject rather sharply frequencies below about 30 cycles, and to reject rather more broadly those above about 70 cycles.

The component parts of a typical reflection seismograph will be taken up in the order in which the waves pass through them. The first unit to be discussed will be the pickup.

Pickup

The most desirable pickup should be extremely rugged, light in weight, small, have a high sensitivity, and require very few connecting wires, these to be un-

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Paper number 39-83, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE South West District meeting, Houston, Tex., April 17–19, 1939. Manuscript submitted January 23, 1939; made available for preprinting March 14, 1939.

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affected by water or weather conditions. Since one pickup would have difficulty in possessing all of these requirements, some compromises must be made. These compromises will have to be chosen with much care because a small change in one requirement may cause a larger change in some other. For instance, a high impedance mechanical-to-electrical converter may be small and light in weight, but it will eventually give trouble from electrical disturbances caused by leakage through insulation and contact resistance. A pickup should be rugged above all else, owing to the fact that time lost in the field is very expensive. A seismograph crew costs between \$5,000 and \$10,000 per month to operate, and when trouble appears the whole crew remains idle while one or two men make the repairs. The rough terrain traversed by a crew during a survey will certainly test any piece of apparatus for ruggedness.

Practical reflection pickups, like nearly all earthquake seismographs, make use of the difference in movement or force between a suspended mass and its container resting on the earth. This movement is then converted into electrical energy in one of several ways. One method uses this movement to vary the separation of capacitor plates. The varying capacity produced this way can be made to drive a vacuum tube either directly, as is similarly done in a condenser microphone, or indirectly by its use in an oscillating circuit. Another way converts this movement into varying resistance by means of pressure on carbon as in the carbon-button microphone. In still another way, the piezoelectric effect of certain crystals is used to obtain this conversion. A mass is suspended on a crystal (usually Rochelle salt) and spring combination, thus flexing the crystal with movement of the earth. The magnetic induction method can be divided into two parts. One has a magnetic field of constant strength through which moves a coil of wire and the other a movable core which varies the reluctance of a magnetic circuit through which a coil of wire has been linked. The method most commonly used is the last one mentioned because its construction is simple, it can be made very rugged, and it has a reasonable sensitivity. Also, the output is proportional to the frequency of the ground motion, thus it acts as a filter and suppresses the low frequencies. This low-frequency discrimination may be moved up or down the frequency spectrum by corresponding changes of the natural frequency of the pickup. The natural frequency is usually made to fall in the range of the reflection frequencies. When this is done, it is necessary to overdamp the mechanical system to prevent the natural frequency from predominating on the record. Damping may be obtained electrically or mechanically. Mechanical damping is most common and consists of filling the pickup with oil of the proper viscosity to damp the movement to any degree desired. Of course, changes in temperature change the viscosity but it is not difficult to follow the seasons with the correct viscosity for each. The pickup shown in figure 2 uses permanent magnets as the mass and the coil wound on the core is designed to match a 200-ohm impedance line at 45 cycles per second. At 45 cycles per second, and an amplitude of 10^{-6} inches of ground movement, this pickup will deliver about 50 microvolts across a 200-ohm load.

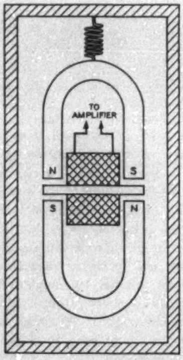

Figure 2. Variable-reluctance pickup

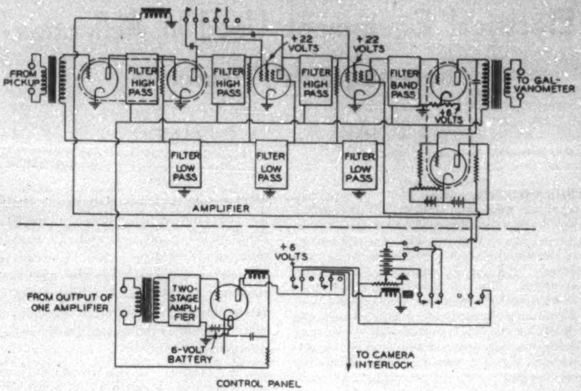

Figure 3. Schematic diagram of control panel and amplifier

Amplifier and Control Panel

The output of the individual pickups is led into the recording truck through multiconductor cables and terminated in an equal number of identical amplifiers. In addition, there is a main control panel from which the gain and frequency response of all amplifiers can be varied simultaneously. A schematic diagram of control panel and amplifier is shown in figure 3. A bank of 12 amplifiers with control panel ready for service is illustrated in figure 9.

The amplifier is probably the most important part of the reflection equipment. Its function is to amplify the output of the pickup to a sufficient level that rugged galvanometers may be used in the camera, to filter out undesired frequencies, and to so control the galvanometer deflections that the multiplicity of traces on the record may be compared over as large a part of the record as possible. The amplifier shown in figures 3 and 4 performs these tasks satisfactorily. It consists of five stages in cascade, followed by a rectifier. The first two stages are high-mu triodes, the third and fourth are remote-cutoff pentodes, the fifth stage is a triode output tube, and the rectifier is an over-biased triode. The output stage is coupled both to the rectifier grid and to an output transformer matching the low-impedance galvanometer. The rectifier output is filtered by a multisection filter and the resulting direct voltage is im-

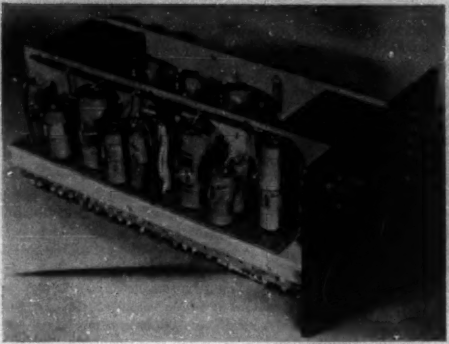

Figure 4. Reflection seismograph amplifier

pressed as a negative bias on the grids of the first four tubes. The maximum over-all voltage gain at 45 cycles from first grid to primary of the output transformer is approximately 315,000 times (110 decibels). The application of 17 volts negative bias by the rectifier system reduces the over-all gain to 10 times (20 decibels). Distortion in the output is negligible for voltages below 0.05 volts root-mean-square on the input grid. The output voltage increases only about 20 per cent when the voltage on the input grid is increased from 5 microvolts to 50,000 microvolts (80 decibels).

The design of a single-sided amplifier to meet these specifications is a rather involved process, but it is considerably simplified by the fact that in general the amplitude of the voltage input to the amplifier decreases with time in approximately an exponential fashion after the initial disturbance, or "first kick," arrives. These first kicks are useful in the computation of the data on the record, and it is desirable to record them clearly. They are often characterized by a very gradual onset, the apparent frequency varying continuously from zero up to some high value over the first half cycle. In order to record this kind of wave form, it is necessary that the amplifier have less low-frequency filtering at the time of arrival of the first kicks than is desirable for the recording of reflections. Also, it is desirable that the first kicks be recorded on a very quiet trace, in order that the time of onset may be determined to within 0.001 second. This is possible because the first kicks are much stronger than the subsequent reflections. These desires are attained through the use of a system of relays. Each amplifier contains a multipole relay which bypasses a large part of the low-frequency filter network. These relays are actuated by one of the first kicks through an auxiliary amplifier, a vacuum-tube relay, and a master relay, all incorporated in the main control panel. The vacuum-tube relay is a lock-up relay, interlocked with the recording camera so that it locks only when the recording camera is in operation. This prevents premature operation and locking from accidental disturbances, such as persons walking near the pickups, etc. This locking relay energizes a slow-acting master relay. The master relay is adjusted for a delay of 0.25 second in order that one of the pickups near the shot point may be used for actuating the relay system. This is desirable because the first kicks on the near pickups are much stronger than those on the pickups farther from the shot point. The operation of the master relay releases the individual amplifier relays, which fall out and remove the by-passes from the low-frequency filters. Another set of contacts on the master relay changes the bias on the amplifier tubes from a high negative value to a low one. The initial bias is adjustable and controls the sensitivity to first kicks. The final bias is fixed at the value which gives maximum amplifier gain. The use of a high initial bias provides quiet traces on which to record first kicks, and also prevents motorboating of the amplifiers when their low-frequency response is extended by the by-pass networks.

Filtering

A very important consideration in the design of a reflection amplifier is the degree of low-frequency filtering desirable. It must be borne in mind that reflection equipment is primarily intended to record a succession of transient waves in such a manner that they may be identified with the maximum of certainty. In particular, it is desirable that the first part of the wave be identifiable. The application of low-frequency filtering tends to minimize the first part of a reflection, but in many cases a considerable degree of such filtering is necessary in order to pick out any reflections at all. In most cases the beginning of the reflection is not sharp enough to pick reliably. Instead, an upward or downward peak is picked. Since a change in the amount of filtering changes the phase characteristic, the

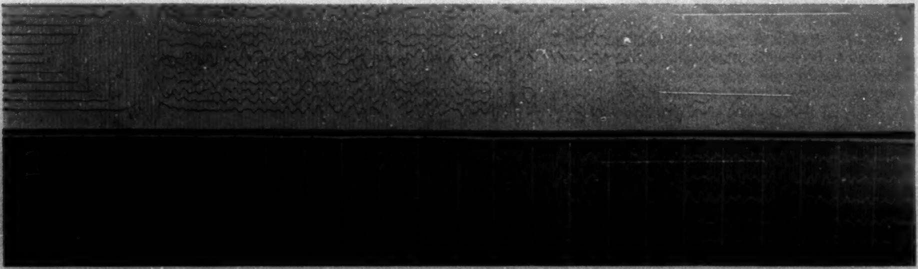

Figure 5. Twelve-channel record using automatic gain control, moving-coil galvanometers and shutter fork, and six-channel record using fixed gain, string galvanometer, and synchronous-motor timer

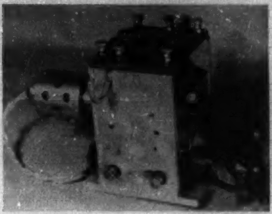

Figure 6. Six-element string galvanometer and single-element moving-coil galvanometer

times at which the peak is recorded will change. Thus records taken at the same place, but with different amounts of filtering, will record reflections at different times. Since it is desirable to correlate reflections at many points when prospecting an area, records at all points should be made with identical instruments. This makes it necessary to decide on the use of a certain degree of filtering which will give satisfactory records in the great majority of cases, compromising on the basis of past experience between elimination of undesired low frequencies and loss of reflection character.

Faithful response to transient waves of varying frequency requires that all filter networks be damped to a high degree. High damping, that is, near critical or beyond, in turn requires a greater number of filter sections to give the desired frequency response. Now a multisection damped network acts somewhat like a network having fewer meshes and less damping. Also the matter of size and weight becomes important. Thus some compromise is necessary in practical design. There has been some argument concerning the comparative merits of an underdamped filter of few sections and an overdamped filter having the proper number of sections to give approximately the same frequency response. There is no doubt that some degree of intentional damping is necessary, but it is sometimes difficult to decide what degree of damping and number of filter sections give the most satisfactory records.

Another solution to the problem of elimination of low-frequency waves is offered by the fact that these waves arrive at different distances from the shot point much farther out of phase than reflected waves. Thus if an amplifier is connected not to one pickup, but to several, at various distances from the shot point, reflected waves will be reinforced and other waves will be cancelled to some extent. Another method is to feed part of the signal in each amplifier to its adjacent amplifiers. These methods give directional characteristics to the system, which is just what is desired, as reflections generally arrive from almost directly below the instruments. The degree of elimination of undesired waves which may be thus obtained is sometimes astounding. Consequently, less filtering may be used, with corresponding improvement in reflection wave form.

Amplitude Control

The frequency response of the amplifier having been decided upon, there remains another problem to be solved. This may be called the amplitude response. Since the movement of the earth, and hence the output of the pickups, due to the shot is much greater for first kicks and shallow reflections than for deeper reflections, the use of a recording system having fixed sensitivity would permit the use of only a small portion of the record. This would require several records with different explosive charges or different instrument sensitivities in order to record all the desired reflections with usable amplitudes. Also, each time the equipment is moved to another shot point the sensitivity of the individual channels must be adjusted by means of trial shots to compensate for local irregularities in the transmission characteristics of the earth. This causes undesirable expenditures of time and material.

The incorporation of automatic gain control in the amplifiers remedies both of these evils. The use of automatic control is possible because in the case of the great majority of shots, the amplitude of the envelope of the voltage output of the pickup varies slowly compared with the frequency of the waves to be recorded. These waves are transient disturbances of continually changing frequency and amplitude and of only a few cycles duration. Since it is desirable that the general shape of these transients be retained, the gain-controlling mechanism must not respond instantaneously to changes in amplitude. Most satisfactory results are obtained when it responds more rapidly to increases in amplitude than to decreases. This makes the amplitude at any part of the record somewhat dependent on preceding amplitude, but if the rate of change of response to decreases in amplitude is made somewhat faster than the general rate of decay of the envelope of the pickup output very satisfactory control is obtained. Figure 5 illustrates records obtained with and without automatic gain control.

The means by which amplitude control is obtained are described below.

As before mentioned, each amplifier contains a triode rectifier. The grid of this rectifier is coupled to the plate of the output tube and its grid bias is continuously adjustable from zero to −22 volts. The value of this bias determines the a-c output voltage at which the rectifier begins to pass current, and thus the upper limit of the output voltage. Plate voltage for the rectifier is supplied by a battery, common to all amplifiers.

The negative bias developed across the load resistor of the rectifier is filtered and applied to the fourth-stage control grid, further filtered, and applied to the third-stage control grid. An adjustable portion of the third-stage bias is applied to the grids of the first and second stages. This permits some adjustment of the initial sensitivity of individual amplifiers to compensate for decreasing first-kick amplitude with increasing distance from the shot point. At the same time it allows the use of unmatched tubes in the first two stages. For input voltages of the order of those handled by reflection amplifiers, the over-all gain of the first two stages may be changed by as much as 50 to one by variation of grid bias with negligible distortion.

The second and third stages use remote-cutoff pentodes because of their superior voltage-handling ability. Control bias is applied to both control and suppressor grids of both tubes, but the bias rarely be-

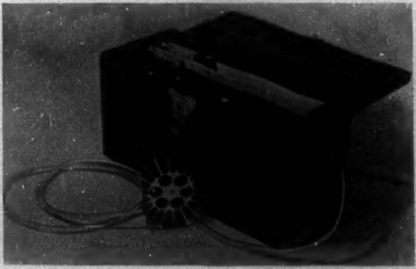

Figure 7. Fork-driven synchronous-motor timer

comes high enough for the suppressor grid to have much control of the tube gain. Screen voltage is obtained from a voltage divider across the plate-voltage supply. The use of a series dropping resistor for screen supply extends the cutoff characteristic considerably but requires the development of several times as much bias voltage as is required when a voltage divider is used. Some compromise is necessary between a choice of a very remote-cutoff tube, with its low distortion, and a sharper-cutoff tube, with its better control of output voltage. The use of tubes of the so-called "mixer" types, namely where the signal is applied to one grid and the control bias to another, gives very good control action but poor voltage-handling ability, and considerable difficulty is experienced in matching these tubes even within broad limits.

Recording Camera

The output of each amplifier is connected to the recording oscillograph by which a record is made of the ground motion that excited the pickups. This record is usually made on sensitized paper, which varies in width from about two inches to ten inches, depending upon the number of pickup stations used. The length of the record depends upon the greatest depth of the stratum from which reflections are desired and upon the speed at which the paper is driven. The average length of record is about four feet. Each pickup station makes one trace on the record, with each amplifier driving one unit of a galvanometer. The galvanometer may be the string type or the moving-coil-and-mirror type. The string galvanometer is nearly always a multiple unit of six or more strings as shown on the left-side of figure 6. This galvanometer is rather large in size because it requires an electromagnetic field. The strings occupy a space of about one-half inch or less in width and their movements are magnified optically with lenses. This means that it is rather difficult to focus all of the strings equally across the flat record. Also the deflection of the strings is not linear and with a large movement as usually happens when the first shock strikes the pickup, the strings interfere and sometimes tangle with each other. Since the illumination for this type of galvanometer is transmitted from the back, the strings appear as shadows on the paper. This gives white traces on a dark background, and such records are very tiring to the eyes of the computers.

The d'Arsonval galvanometer, on the other hand, uses a tiny mirror on a rotating coil to reflect a spot of light to the sensitized paper. This gives a black trace on a white background. One of these galvanometers is shown on the right side of figure 6. These galvanometers give an almost linear reproduction of the amplifier output since they are placed almost directly in front of the trace they are exposing on the record. They are rugged in construction and, since a coil of a large number of turns may be used as the moving element, the sensitivity is 25 or more times that of the string galvanometer even with the use of small permanent magnets. Of course, the moving-coil construction does not respond to high frequencies, but this is a desirable characteristic. High frequencies found at the pickup are usually noise, wind, or other movements not desired on the record. These galvanometer units may be added or subtracted from an oscillograph making for more flexibility. The difficulties found with them are mostly in the mirror. If a large mirror is used, there is much trouble from distortion of the glass, but, if it is small, not enough light is reflected to expose the record properly. A complete recording camera is illustrated in figure 10.

Timing

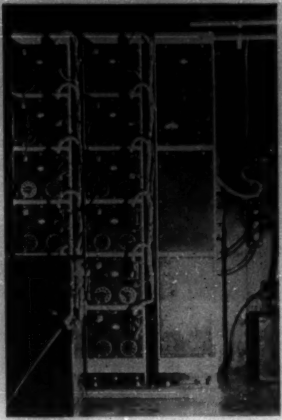

Figure 9. Twelve-channel reflection seismograph amplifier bank set up in truck

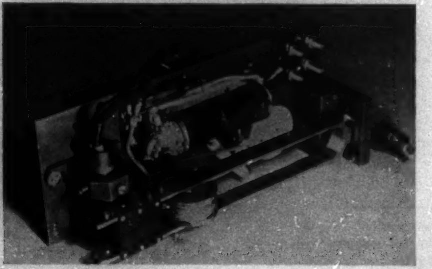

Figure 8. Shutter fork timer

When the charge of dynamite in the hole at the shot point is exploded, the electrical circuit to the cap is broken. This interrupts the current that was flowing in the cap line just previously and permits the collapse of the magnetic flux in a transformer carrying this current. The electrical impulse produced is sent by radio or wire to the recording oscillograph where it deflects one of the galvanometers. The galvanometer used may be a separate one or the impulse may be composited on one that is later to record the ground motion. This time of break, as was mentioned earlier, is zero on the record. Now in order to find the elapsed time to the reflections it is necessary to have time marks on the record. Time marks are lines exposed across the record at right angles to the galvanometer traces and ordinarily are spaced 0.01 second apart. Sometimes a distinction is made in the line representing 0.1 second, making more convenient the counting of time on the record. A tuning fork of usually two tines, but sometimes one, is almost universally used for obtaining these time lines. One method, a well-known system, uses an electromagnetically driven fork that interrupts an electric current at 50 cycles per second to drive a small synchronous motor and paddle wheel. Such a unit is shown in figure 7. With this method the 0.01- and 0.1-second time lines are easy to obtain by making each tenth spoke a little wider than the others. But otherwise this piece of equipment is too heavy and large and frequently gives feedover by induction from the motor windings. Also, the fork contacts burn and get out of adjustment. The other

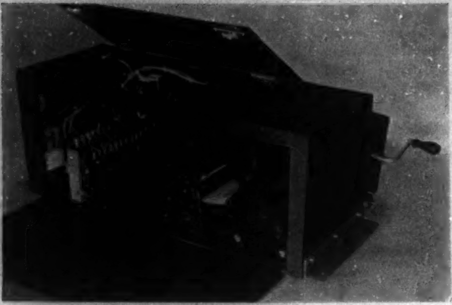

Figure 10. Fourteen-element recording camera

common method of timing makes use of a shutter interrupting a light beam to flash a mark across the record at 0.01-second intervals. This shutter is ordinarily mounted on a fork of one or two tines and the fork started by one pulse from an electromagnet and permitted to decay at will. It will continue to vibrate at a good amplitude long enough to obtain the usual record of three or four seconds duration. This timing unit is shown in figure 8 and by comparison with figure 7, it can be seen to be more compact, much smaller in size, and relatively foolproof.

Paper Drive

The number of records taken during a day's work depends almost entirely upon the kind of country being surveyed. If the shot holes are difficult to load or the country so rough or wet that much time is required to move, 12 records may represent a hard day's work. The average of good and bad days is about 40 records with about five feet of sensitized paper used for each, including some waste on each end of the record. It is common practice to use a supply roll of this paper and cut off and develop each record as it is taken. The driving mechanism for the paper consists of a motor, either electric or spring with a governor control, and a friction roller or a reel. The friction-roller drive is probably the better, owing to the fact that the paper speed is constant and therefore the time lines will be equally spaced, but the reel drive is easier to build and is more foolproof. In the friction-drive method the paper is fed between two rollers driven by the motor, thus feeding the paper at the peripheral speed of the rollers. In the reel method, the paper is attached to a spool driven by the motor and the paper wound on the spool in a number of layers. This means that the speed of the paper is a function of the length of the record. If the records in the latter case are started at about the same time, the time lines on all of them will be spaced approximately equally and the records may be compared easily.

Discussion

E. E. George (Tennessee Electric Power Company, Chattanooga): This paper is extremely interesting, especially in its description of the development of very sensitive and accurate measuring equipment suitable for rough handling in the field. The construction of an amplifier with 110 decibels gain for this service is particularly interesting. Apparently no use is made of feedback. While this would necessitate increased amplification it would add to stability and fidelity. It might also be possible to place filter circuits in the feed-back path and secure additional discrimination.

Many engineers would probably be interested in some of the selections of equipment for the severe service required of portable devices. For instance, are glass or metal tubes used? Are the tubes of the same type used in radio receivers, or are they of special types such as the 1620, 1621, and 1622? Has any use been made of the special types of tubes developed for train-control service or for telephone repeater applications where long life and reliability are of particular importance? Is the type of oscillograph used similar to that used by power companies? What type of power supply is used on the recording trucks?

The authors should be congratulated on presenting in such an interesting fashion a special industrial application of electronic equipment. It is apparent that the design has required an enormous amount of development work. It is to be hoped that the Institute will arrange for more papers on industrial applications of electronic devices, particularly in instances like this which are absolutely new to the average electrical engineer.

C. C. Nash, Jr., and **C. C. Palmer:** In the instruments described little use is made of inverse feed back, because of the greater number of parts required. Some experimental work has been done with amplifiers using feedback with filter networks in the feed-back path as suggested by Mr. George.

The equipment described uses ordinary glass radio receiving tubes throughout. The glass tubes have given more reliable service than metal ones. For this service there seems to be very little difference in the service life of ordinary radio tubes and special long-life tubes.

The oscillograph is not radically different from those used by power companies.

Batteries are usually used to supply power to the instruments, storage batteries being used for amplifier heater power and dry batteries for plate power. Some use has been made of motor generators and vibrator units for plate supply. Storage batteries are used to light the oscillograph lamps and to supply power to the paper-driving motor. A recording truck usually contains four or five heavy-duty storage batteries. These batteries are charged by heavy-duty automotive generators belted to the truck engine.

Harmonics in the A-C Circuits of Grid-Controlled Rectifiers and Inverters

R. D. EVANS MEMBER AIEE

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Synopsis: This paper summarizes the results of an investigation of the harmonic currents and voltages in the a-c circuits of grid-controlled rectifiers and inverters. The principal part of the paper presents the development of a theoretical method for predetermining the magnitude of the harmonics in terms of the d-c load current, the commutating reactance, the rectifier transformer secondary voltage, and the amount of grid control. Harmonic voltages in the supply circuit may then be calculated from the harmonic currents and the supply-circuit reactances at the various harmonic frequencies.

General curves are given to facilitate the calculation of the harmonic currents for the range of conditions usually encountered. In addition, curves are included for the easy determination of the product of the supply-circuit current and its own telephone influence factor, the $I \cdot T$ product, which quantity is useful in inductive co-ordination studies. Comparisons are given between the results of tests and the results of calculations by the theoretical method presented in this paper. These and other comparisons show very satisfactory checks so that the method may be considered to be established.

The case of the inverter is treated in a manner similar to that used for the grid-controlled rectifier. The general curves for the rectifier may also be applied to the inverter by appropriate choice of the defining angles.

The case of a-c circuits with nonlinear frequency-reactance characteristics is briefly considered, and an empirical modification of the theoretical method is suggested. This modification may be applied to the case of a rectifier provided with a-c filtering equipment. The wave forms of current and voltage in one such supply circuit were obtained from oscillograms. A comparison of these oscillograms shows the great improvement in wave form which may be accomplished by the addition of filters.

A WELL-KNOWN property of all rectifier- and inverter-type apparatus is that they produce harmonic distortions in both the current and voltage wave shapes on both the supply and output circuits. Several investigators have made contributions to the problem of determining these harmonic voltages and currents under various conditions of rectifier operation. There are four distinct steps in this development which are:

1. D-c harmonics without grid control.
2. D-c harmonics with grid control.
3. A-c harmonics without grid control.
4. A-c harmonics with grid control.

These will be reviewed briefly in order to show the relation of the present investigation to that of previous work.

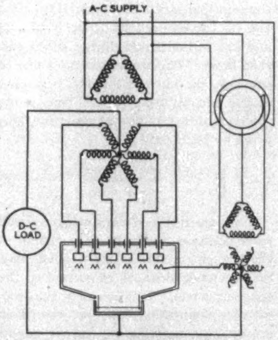

Figure 1. Schematic diagram of six-phase star grid-controlled rectifier

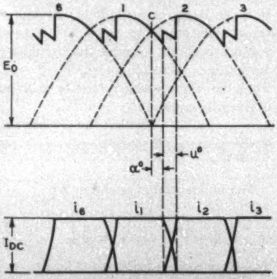

Figure 2. Voltage and current wave shapes of a six-phase star grid-controlled rectifier under load

Harmonics on the d-c side of a rectifier are commonly determined on the basis of an "internal harmonic voltage" whose magnitude may be expressed as a function of the output current, angle of overlap, and amount of grid control. The harmonic currents in the d-c load circuit may then be computed from the harmonic voltages and the d-c load circuit constants including the "internal inductance" of the rectifier. The internal harmonic voltages are estimated by a theoretical method assuming an infinitely high inductance in the d-c load circuit. The formulas for the case of a rectifier without control grids have been given by several authors, for example, Prince and Vogdes,[1] and Marti and Winograd.[2] Recently the corresponding case for grid-controlled rectifiers has been analyzed by Stebbins and Frick,[3] who have extended the previous theoretical method to cover the range of grid control.

Harmonics on the a-c side of rectifiers without control grids may be estimated either by empirical or by theoretical methods. In the empirical method developed by Blye and Kent,[4] the harmonic currents are considered as being caused by internal harmonic voltages acting on the harmonic impedances of the supply system and a fictitious resistance varying with the load. The theoretical method for rectifiers without control grids, presented by Brown and Smith,[5] assumes (1) an inductive supply circuit through which commutation takes place, and (2) a constant output current due to infinitely high inductance in the load circuit. Using these assumptions, analytical expressions are derived for the anode currents which have a "flat top" wave form except during the commutating period when the current is transferred from the outgoing anode to the incoming anode in accordance with the voltage available for circulating the current through the inductance of the commutating circuit. The harmonic voltages in the supply system may then be computed on the basis of the voltage drops due to the harmonic currents flowing through the various circuit elements, considering the rectifier as the source of these harmonic currents.

The fourth step is given in this paper which presents a theoretical method for estimating harmonic currents and voltages in the supply circuits of grid-controlled rectifiers. It may be viewed as a generalization of the theoretical method for rectifiers without control grids.

The determination of harmonic currents and voltages in the a-c and d-c

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Paper number 39-38, recommended by the AIEE committees on communication and power transmission and distribution, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 4, 1938; made available for preprinting December 16, 1938.

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1. For all numbered references, see list at end of paper.

circuits of inverters is a problem which has received relatively little attention in the technical literature. However, the harmonic problem in the inverter is closely allied to the problem in the grid-controlled rectifier, and the same theoretical methods of analysis may be applied. In fact, the solution of the grid-controlled rectifier may be applied to the inverter provided appropriate choice is made of the angles corresponding to the angles of overlap and retardation for the rectifier.

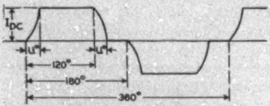

Figure 3. Line-current wave shape in primary of six-phase star grid-controlled rectifier under load

Determination of Harmonic Currents and Voltages From Theoretical Wave Shape Under Load

Consider a grid-controlled rectifier supplied from a six-phase star-connected transformer as shown in figure 1. Assume that the output circuit is of infinitely high inductance so that the output current may be taken as constant. Assume that the impedances of the supply circuit vary linearly with frequency so that they correspond to those of a simple inductance. Assume also that the rectifier is fully symmetrical in all phases so that the harmonic voltage and current waves are identical in the different phases.

On the basis of the foregoing assumptions, it is possible to prepare the diagram of figure 2 in accordance with conventional rectifier theory. Figure 2 applies to the condition for the transfer of current from anode to anode. The secondary crest voltage is E_0 and the constant value of d-c load current is I_{dc}. The angle of retardation and the angle of overlap are shown by the symbols α and u respectively. The voltage and current for any particular anode are designated with the arabic numeral subscripts from 1 to 6. In an ordinary rectifier, anode 1 would carry current to the point c but with energized grids commutation would be delayed by the angle α. Commutation lasts through a period corresponding to the angle u. In this interval the current is transferred from anode 1 to anode 2 as shown in the diagram. After 60 electrical degrees or $\pi/3$ radians, anode 3 begins to carry current and the commutation is identical with that of the previous pair of anodes. The line current on the primary side, shown in figure 3, consists of currents supplied from two adjacent anodes, such as anodes 1 and 2 of figure 2, assuming the transformer delta voltage is equal to the secondary star voltage.

The magnitude of the anode current is:

$$I = E_0 \frac{\sin \frac{\pi}{p}}{X} [\cos \alpha - \cos (u + \alpha)] \qquad (1)$$

where p is the number of phases in each secondary phase group of the rectifier. The total d-c current for a phase group is given by the above expression which for the particular case of the six-phase rectifier shown in figure 1 is the same as the output current.

The shape of the anode-current wave thus includes (1) a "flat top" wave during the principal part of the conducting period and (2) curved portions at the beginning and end of the period during which current flows. The instantaneous value of primary line current for the beginning of the commutating period, considering the start of current flow at zero time, is as given by Herskind:[6]

$$i = I \left[\frac{\cos \alpha - \cos (\theta + \alpha)}{\cos \alpha - \cos (u + \alpha)}\right] \qquad (2)$$

Since the d-c load current is assumed to be constant, the increase in current in the incoming anode is accompanied by a corresponding decrease of current in the outgoing anode. Consequently, the process of commutation may be viewed as arising from a circulating current caused by the voltage available in the circuit acting through the circuit inductance. The total circuit inductance is twice the commutating inductance since by definition the latter is one-half of the inductance from anode to anode. The total voltage available to cause the circulating current is equal to $2E_0 \sin \pi/p \sin (\theta + \alpha)$. Thus the relation which must be satisfied is given by equation 3.

$$2L \frac{di}{dt} = 2E_0 \sin \frac{\pi}{p} \sin (\theta + \alpha) \qquad (3)$$

The differential of equation 2 is

$$\frac{di}{dt} = I \left[\frac{\omega \sin (\theta + \alpha)}{\cos \alpha - \cos (u + \alpha)}\right] \qquad (4)$$

The substitution of equation 4 in equation 3 results in equation 1 which verifies the relation assumed. Equation 2 thus gives the basic relation for the expressions of current during the commutating period.

Equation 2 gives the current for the beginning of flow in the incoming anode, but it is obvious that the same relation can also be used for current in the outgoing anode, since the sum of the currents in these two anodes is equal to the output current. Similarly, the expressions for current flow at other commutating periods may be expressed if correction is made in the angles defining the commutating period in that particular pair of anodes. Referring to figure 3, it becomes possible with the aid of equation 2 to write an analytical expression for the current wave form. This leads to the result given in table I for 0 to π radians covering one-half cycle of the a-c wave. It may be shown that the following half cycle is of identical wave shape.

With an analytical expression for the current wave for a half cycle, it is possible to carry out a Fourier expansion with the individual harmonic currents given by the following relations:

$$m = \begin{bmatrix} a_1 \sin \theta + a_5 \sin 5\theta + a_7 \sin 7\theta + \\ a_{11} \sin 11\theta + \ldots \\ b_1 \cos \theta + b_5 \cos 5\theta + b_7 \cos 7\theta + \\ b_{11} \cos 11\theta + \ldots \end{bmatrix} \qquad (5)$$

The solution of the Fourier expansion is given by the following:

$$a_m = \frac{4I}{\pi}\left[\frac{+ P_m \sin \frac{\pi m}{3} + Q_m \cos \frac{\pi m}{3}}{m (m^2 - 1) [\cos \alpha - \cos (u + \alpha)]}\right] \times \sin \frac{\pi m}{3} \qquad (6)$$

$$b_m = \frac{4I}{\pi}\left[\frac{+ Q_m \sin \frac{\pi m}{3} - P_m \cos \frac{\pi m}{3}}{m (m^2 - 1) [\cos \alpha - \cos (u + \alpha)]}\right] \times \sin \frac{\pi m}{3} \qquad (7)$$

where P_m and Q_m are expressed as follows:

$$P_m = m \sin (u + \alpha) \cos mu - \cos (u + \alpha) \sin mu - m \sin \alpha \qquad (8)$$

$$Q_m = m \sin (u + \alpha) \sin mu + \cos (u + \alpha) \cos mu - \cos \alpha \qquad (9)$$

The root-mean-square value of the individual harmonic current is given by equation 10.

$$I_m = \frac{\sqrt{a_m^2 + b_m^2}}{\sqrt{2}} = \frac{2\sqrt{2}I}{\pi} \times \left[\frac{\sqrt{P_m^2 + Q_m^2} \sin \frac{\pi m}{3}}{m(m^2 - 1)[\cos \alpha - \cos (u + \alpha)]}\right] \qquad (10)$$

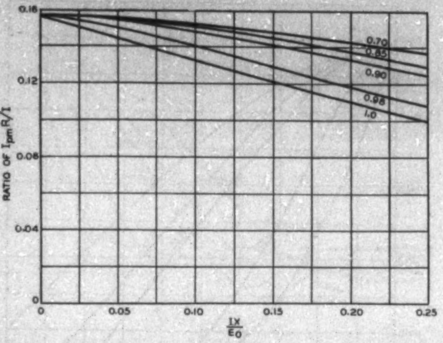

Figure 4. Fifth harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

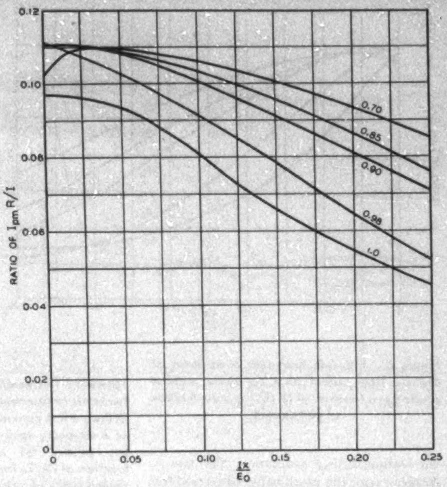

Figure 5 (right). Seventh harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

The terms a_m and b_m, being respectively the sine and cosine components of the harmonic expansion, permit the determination of the phase angle which may be expressed in terms of the angle at which maximum value is reached with respect to zero time, which angle is expressed as follows:

$$\theta_H = -\frac{\pi m}{3} - \tan^{-1}\frac{Q_m}{P_m} \text{ radians} \qquad (11)$$

The formulas for a_m and b_m do not apply to the fundamental because the expressions of equations 6 and 7 become indeterminate. It is necessary, therefore, to compute the components of the fundamental separately and this has been done with the following results:

$$a_1 = \frac{\sqrt{3}I}{2\pi}\left[\frac{1}{\cos\alpha - \cos(u+\alpha)}\right] \times \left\{\begin{matrix}\cos\alpha\,[+\sin^2 u - \sqrt{3}(u - \sin u\cos u)] \\ +\sin\alpha[-\sqrt{3}\sin^2 u + u + \sin u\cos u]\end{matrix}\right\} \qquad (12)$$

$$b_1 = \frac{\sqrt{3}I}{2\pi}\left[\frac{1}{\cos\alpha - \cos(u+\alpha)}\right] \times \left\{\begin{matrix}+\cos\alpha[\sqrt{3}\sin^2 u + (u - \sin u\cos u)] \\ +\sin\alpha[\sin^2 u + \sqrt{3}(u + \sin u\cos u)]\end{matrix}\right\} \qquad (13)$$

The root-mean-square value of the fundamental is given in equation 14.

$$I_1 = \frac{\sqrt{3}I}{\sqrt{2}\pi}\left[\frac{1}{\cos\alpha - \cos(u+\alpha)}\right] \times \left\{\begin{matrix}\sqrt{u^2 - 2u\sin u\cos u\cos 2\alpha + \sin^2 u} \\ \qquad + 2u\sin u\sqrt{\cos\alpha\sin\alpha}\end{matrix}\right\} \qquad (14)$$

Examination of the above expressions for the harmonic and fundamental components of the a-c wave will show that for the condition of a rectifier without control grids for which $\alpha = 0$, the expressions reduce to a form identical with that given by Brown and Smith.[5]

While the above expressions have been derived on the basis of a 6-phase rectifier, the same expansion with appropriate multipliers may be used for double three-phase rectifiers. In addition, it is possible to apply the same expansions to 12-phase rectifiers operating with similar connections if the harmonics of the series which includes the 5th, 7th, 17th, 19th, 29th, 31st, etc., are considered to be reduced theoretically to zero and actually to 25 per cent of their 6-phase magnitudes to account for values encountered under practical operating conditions. This factor of 25 per cent for the harmonics of this series is empirical but is based on the values recommended in the Edison Electric Institute report on "Rectifier Wave Shape."[7]

General Curves for Determination of Harmonics

The analytical solution for the root-mean-square magnitude of the individual harmonic currents given by equation 10 is rather tedious for practical use. Accordingly, the results have been expressed in graphical form[*] by means of figures 4 to 13 inclusive. For the application of these curves, it is necessary to obtain for any particular rectifier merely the d-c load current for an anode group,

Table I. Analytical Expression for Anode Current

Part	Range in Radians	i
a	$0 < \Theta < u$	$\frac{\cos\alpha - \cos(\theta+\alpha)}{\cos\alpha - \cos(u+\alpha)}I$
b	$u < \Theta < 2\frac{\pi}{3}$	I
c	$2\frac{\pi}{3} < \Theta < \left(2\frac{\pi}{3} + u\right)$	$\frac{\cos\left(\Theta + \alpha - 2\frac{\pi}{3}\right) - \cos(u+\alpha)}{\cos\alpha - \cos(u+\alpha)}I$
d	$\left(2\frac{\pi}{3} + u\right) < \Theta < \pi$	0

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[*] Since this paper was submitted to the Institute the authors' attention has been called to "The Mercury-Arc Rectifier Considered in Relation to the Power-Supply System; An Analysis of Its Operation," by P. G. Laurent, *R.G.E.*, volume 44, number 2, pages 47–60, July 9, 1938. The scope of this article is quite different from that of the present paper; however, it does include certain general curves which although of restricted range are similar in form to those presented in this section.

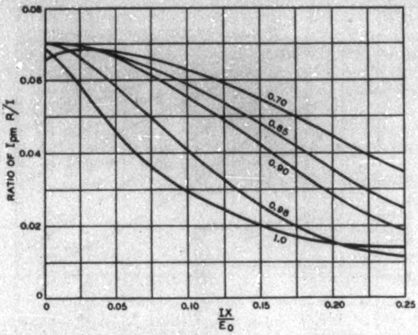
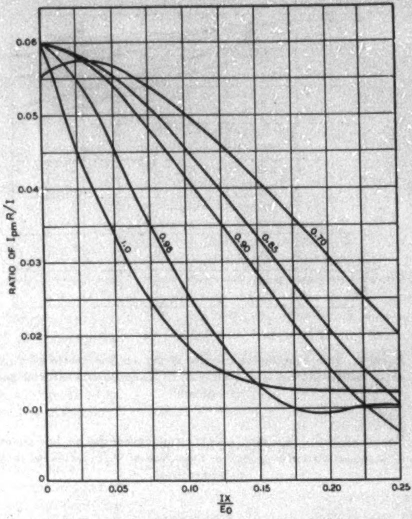


Figure 6. Eleventh harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E₀ for several ratios of grid control

Figure 7. Thirteenth harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E₀ for several ratios of grid control



the commutating reactance, the transformer ratio, the crest value of secondary voltage, and the amount of grid control. The last named may be defined as cos α which is the ratio of the d-c circuit voltage with grid control to the voltage without grid control, other conditions remaining the same. These curves give a factor which is equal to the ratio of the harmonic current in the supply line to the direct current per secondary phase group, provided the line-to-neutral voltages on the primary and secondary sides are equal. If the actual ratio of primary to secondary voltages-to-neutral is R, the root-mean-square value of the harmonic current in the supply circuit becomes

$$I_{pm} = \frac{KI_{dc}}{2R} = \frac{KI}{R} \ldots \text{6-phase from double-wye with interphase transformer} \qquad (15)$$

$$I_{pm} = \frac{0.483KI_{dc}}{R} = \frac{1.932KI}{R} \ldots \text{12-phase from quadruple-wye with interphase transformers} \qquad (16)$$

where K is the factor obtained from the Fourier analysis. This factor is the ratio $I_{pm}R/I$ which is plotted in figure 4 to figure 13 inclusive.

The general curves for the determination of harmonic currents apply to the common types of 6- and 12-phase con-

Figure 8. Seventeenth harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E₀ for several ratios of grid control

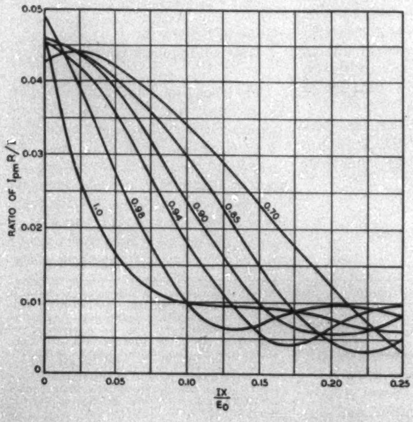
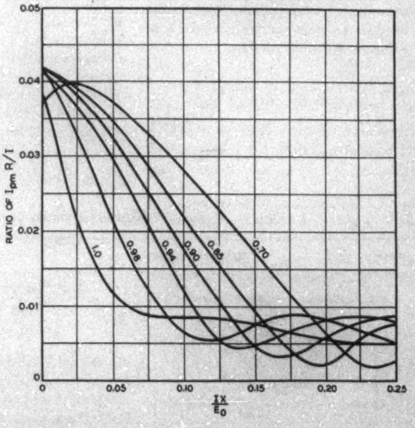


Figure 9. Nineteenth harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E₀ for several ratios of grid control



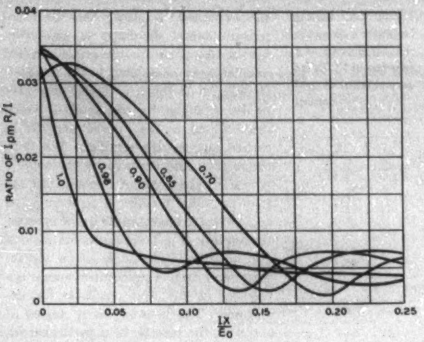


Figure 10. Twenty-third harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

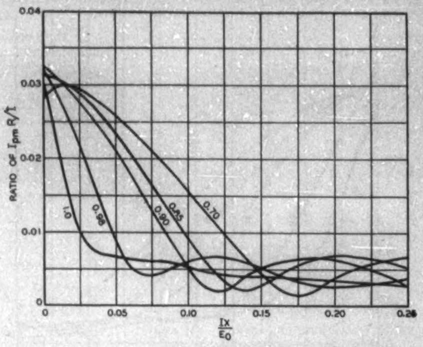


Figure 11. Twenty-fifth harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

nections of power rectifiers. These curves may also be applied to other connections, provided the sum of the angles u and α is not greater than 41 degrees for $p = 6$ and 20 degrees for $p = 12$. This limitation is necessary to avoid the condition for which an additional anode begins to carry current in the rectifier with the larger number of phases.

Comparison With Test Results

The accuracy of the theoretical method just described for estimating harmonic currents in the supply circuits of grid-controlled rectifiers, is best shown by comparison with the results of harmonic measurements made during actual operation. Table II gives a comparison of test and theoretical values for four different conditions of rectifier load and ratios of grid control. It will be noted that exceptionally good checks are obtained. The last three columns on the right-hand side of table II apply to the rectifier operating without grid control. A comparison can thus be made between tests, and calculations by both the theoretical and empirical methods previously discussed. On the whole, the theoretical method gives in this case somewhat closer checks than the empirical method. Table III gives a similar comparison for another rectifier for a wider range of load and wider range of grid control.

It will be noted that the theoretical

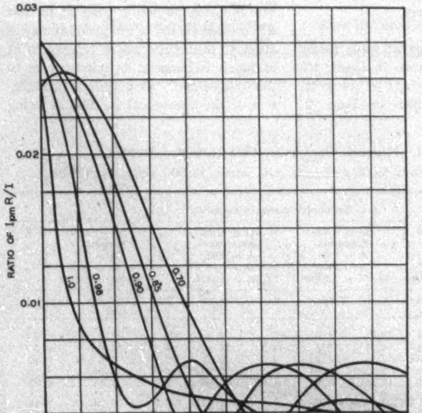


Figure 12. Twenty-ninth harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

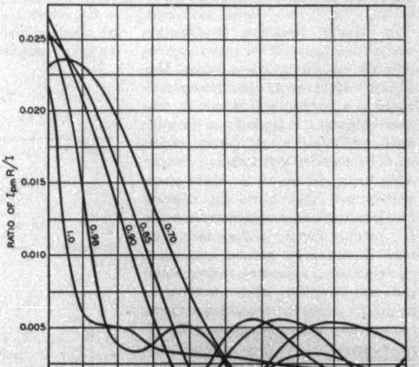


Figure 13. Thirty-first harmonic component of the a-c line current of a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

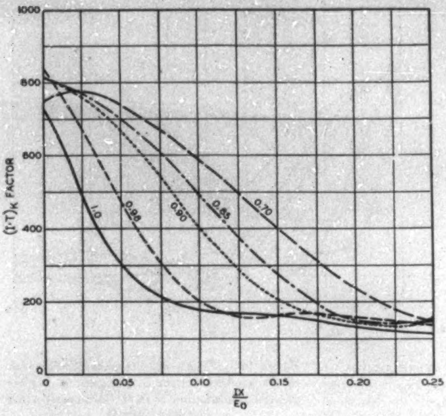


Figure 14. $(I \cdot T)_k$ factor for a six-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

method gives very satisfactory comparisons with tests for a wide range of loads and grid control ratios. The comparisons in table II are better than those of table III because in the former tabulation the tests were made on a system whose supply-circuit reactance varied linearly from 60 to 2,000 cycles, and in this respect conformed more closely to one of the basic assumptions of the theoretical method.

It will be noted that the harmonic distortions for grid-controlled rectifiers may be appreciably greater than those for rectifiers without control grids. This ratio for certain values of grid control and load may be as great as four to one.

I·T Product Curves

In detailed inductive co-ordination studies it is generally necessary to consider the influence of the individual harmonics. However, in preliminary estimates it is frequently adequate to consider only the $I \cdot T$ product; a quantity equal to the a-c supply current multiplied by its telephone influence factor.[8] Accordingly, figure 14 for the 6-phase rectifier and figure 15 for the 12-phase rectifier have been prepared to show the $I \cdot T$ product factor, $(I \cdot T)_k$, for given conditions of output current, commutating reactance, secondary voltage, and grid control ratios. The expression for the $I \cdot T$ product, considering transformer ratio, is given by equation 17.

$$I \cdot T = \frac{(I \cdot T)_k I}{R} \tag{17}$$

where factor $(I \cdot T)_k$ is obtained from figures 14 and 15. The curves given in figure 15 for the 12-phase rectifier are obtained from harmonic currents for the 6-phase rectifier plotted in figures 4 to 15 inclusive, but with the 5th, 7th, 17th, 19th, 29th, and 31st harmonics reduced to 25 per cent of their 6-phase magnitudes. This factor of 25 per cent, as mentioned previously, is empirical but is based on the recommendations of the EEI report on "Rectifier Wave Shape."[7] It should be pointed out that these curves can be used only where the impedance of the supply circuit is linear with frequency.

Application of Theoretical Method to Inverters

The voltage and current wave forms in the inverter, as shown in figure 16, are closely related to those of the grid-controlled rectifier shown in figure 2. In fact, it may be shown that the theoretical method discussed in an earlier part of this paper for the grid-controlled rectifier, may be applied to the inverter. In order to do this it is merely necessary to define angles for the inverter which can be used in the Fourier analysis as carried out for the rectifier. For both the rectifier and the inverter the angle u represents the angle of overlap. In the grid-controlled rectifier α represents the angle of retardation, while in the inverter the angle $(u + \alpha')$ represents the angle of grid advance. The angle α' for the inverter is in itself of significance since it is a measure of the time available for deionizing. The notation using angles α' and u for the inverter is of particular advantage in the study of the harmonic problem, since its adoption permits the use of the exact formulas derived for the grid-controlled rectifier by the mere substitution of these values for α and u respectively. The general curves of figures 4 to 15 inclusive may also be applied to the inverter by the substitution of the corresponding quantities for the inverter, being careful to use $\cos \alpha'$ in place of $\cos \alpha$, the constant A, for the rectifier.

Supply Circuits of Nonlinear Frequency-Reactance Characteristics

The theoretical method, including the formulas and curves, has been derived on the assumption that the supply-circuit reactances vary linearly with frequency. The question naturally arises as to how this method should be modified in order to apply it to circuits whose reactances at harmonic frequencies do not conform to this assumption. An empirical modification of the theoretical method of doing

Table II. Harmonic Currents for Grid-Controlled Rectifier

3,125 Kw, 625 Volts, 5,000 Amperes, A-C Supply, 12,000 Volts, Three-Phase

	A-C Harmonic Current in Amperes								
	I_{dc} = 1,000 Amperes A = 0.96 α = 16.5° IX/E_0 = 0.0106		I_{dc} = 2,500 Amperes A = 0.96 α = 16.5° IX/E_0 = 0.0266		I_{dc} = 4,000 Amperes A = 0.96 α = 16.5° IX/E_0 = 0.0425		I_{dc} = 4,000 Amperes A = 1 α = 0° IX/E_0 = 0.0425		
Frequency	Test	Theoret.	Test	Theoret.	Test	Theoret.	Test	Theoret.	Empirical
300	4.5	5.94	13.7	14.8	23	23.4	20.8	22.3	16.6
420	2.7	4.24	9.0	10.4	15.2	16.4	14.2	14.6	10.7
660	2.4	2.68	6.3	6.42	9.8	9.85	7.8	7.4	5.46
780	1.7	2.26	5.2	5.34	8.2	7.95	5.3	5.65	4.15
1,020	1.4	1.70	3.35	3.91	4.6	5.50	2.6	3.05	2.57
1,140	1.0	1.51	3.15	3.40	4.6	4.52	1.9	2.12	2.14
1,380	1.0	1.25	2.55	2.75	3.4	3.37	1.05	1.23	1.50
1,500	.9	1.14	2.30	2.42	3.0	2.77	.95	1.09	1.29
1,740	.8	.97	1.90	1.90	2.1	1.96	.95	.88	.97
1,860	.4	.88	1.42	1.73	1.85	1.66	.78	.77	.85
Root-mean-square	40		100		160		142		

Table III. Harmonic Currents for Grid-Controlled Rectifier

750 Kw, 600 Volts, 1,250 Amperes; A-C Supply, 2,300 Volts, Three-Phase

Frequency	I_{dc} = 1,250 Amperes, A = 1.0, α = 0°, IX/E_0 = 0.070		I_{dc} = 1,250 Amperes, A = 0.945, α = 19°, IX/E_0 = 0.070		I_{dc} = 1,250 Amperes, A = 0.85, α = 31.7°, IX/E_0 = 0.070		I_{dc} = 1,250 Amperes, A = 0.75, α = 40.3°, IX/E_0 = 0.070	
	Test	Theoret.	Test	Theoret.	Test	Theoret.	Test	Theoret.
300	39.4	37.4	45.3	40.5	52.7	41.6	58.6	41.7
420	23.6	24.5	22.1	27.7	23.2	28.9	22.1	29.3
660	12.1	10.1	15.7	15.9	18.8	17.3	18.2	17.8
780	7.35	7.17	10.8	12.1	10.8	14.0	11.0	14.5
1,020	3.74	3.46	9.74	7.65	11.5	10.0	12.6	10.4
1,140	2.06	2.66	5.41	6.02	4.25	8.31	5.27	9.0
1,380	2.33	1.84	3.07	3.86	7.25	5.87	8.16	6.80
1,500	1.89	1.74	2.58	2.30	2.98	5.10	3.38	5.77
1,740	1.43	1.33	2.57	1.45	4.34	3.48	5.36	4.52

Frequency	I_{dc} = 625 Amperes, A = 1.0, α = 0°, IX/E_0 = 0.035		I_{dc} = 625 Amperes, A = 0.945, α = 19°, IX/E_0 = 0.035		I_{dc} = 625 Amperes, A = 0.89, α = 27.1°, IX/E_0 = 0.035		I_{dc} = 1,875 Amperes, A = 0.945, α = 19°, IX/E_0 = 0.1045	
	Test	Theoret.	Test	Theoret.	Test	Theoret.	Test	Theoret.
300	18.1	19.8	23.1	20.8	23.1	21.0	65.9	58.8
420	10.8	12.0	9.0	14.6	9.90	14.8	33.8	39.0
660	7.38	7.11	8.45	9.02	8.84	9.25	19.4	19.8
780	5.40	5.45	5.18	7.33	5.67	7.66	12.3	14.35
1,020	3.42	3.11	6.39	5.36	6.07	5.72	7.47	6.60
1,140	2.33	2.30	2.95	4.45	2.94	4.90	2.46	4.02
1,380	1.54	1.21	4.91	3.60	4.42	3.88	1.78	1.58
1,500	1.06	1.02	2.11	3.10	2.58	3.44	1.09	1.10
1,740	1.03	.83	3.54	2.48	3.31	2.81	1.54	1.48

this for the case of a rectifier without control grids was proposed by Brown and Smith in the closing discussion of the paper[5] previously referred to. An extension of this method may be applied to the general case of a grid-controlled rectifier. In the modified theoretical method presented in this paper, the harmonic currents are estimated by the aid of figures 4 to 13 inclusive, using a fictitious reactance for the determination of the ratio IX/E_0. This fictitious reactance for each harmonic frequency is equal to the actual reactance at that frequency divided by the order of the harmonic. The value of the suggested modification of the theoretical method is yet to be determined, although by its use somewhat better checks are obtained for cases like that of table III. In that case, as mentioned previously, the supply-system reactance had nonlinear frequency-reactance characteristics.

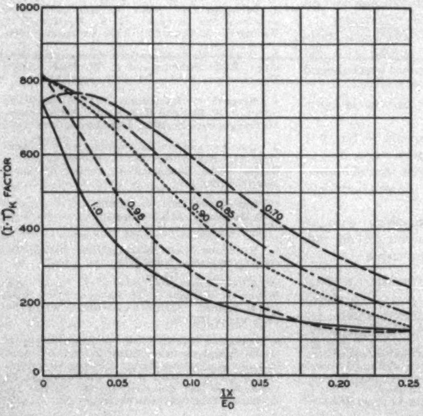


Figure 15. $(I \cdot T)_k$ factor for a 12-phase rectifier plotted as a function of IX/E_0 for several ratios of grid control

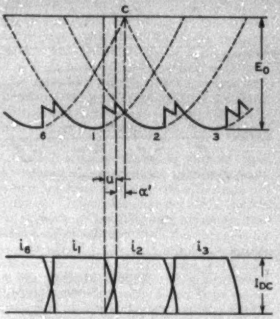
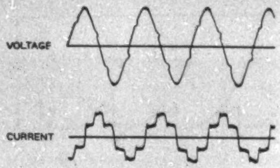


Figure 16. Voltage and current wave shapes of a six-phase star inverter under load

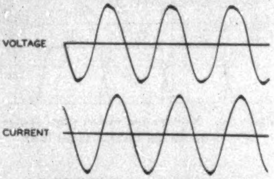
Voltage and Current Wave Shapes in Supply Circuits With and Without A-C Filtering Equipment

When a-c filtering equipment is added to the supply circuit of a rectifier, this constitutes a special case of a supply circuit with nonlinear frequency-reactance characteristics. Some test results[9] are available from an installation including a-c filtering equipment consisting of seven three-phase resonant shunts tuned for the principal harmonic frequencies present. These resonant shunts are installed at the rectifier location and connected on the supply side of the rectifier transformer. For this installation the values of the harmonic currents supplied to the resonant shunts and the harmonic currents in the supply circuit are listed in table IV. In this table the second column gives the test values of harmonic currents supplied to the filtering equipment. Columns 3 and 4 give the corresponding harmonic currents calculated by the empirical method, and the modified theoretical method discussed in the preceding paragraph. Column 5 gives the theoretical values of harmonic currents in the supply lines for the condition without a filter. It will be noted that reasonable checks are obtained by both the modified theoretical and empirical methods. While in this particular case the empirical method gives somewhat better results, the modified theoretical method may find a wider field of application including that of the grid-controlled rectifier, although as pointed out previously only limited experience has been obtained using this method.

The a-c filtering equipment for the installation mentioned in connection with table IV was the first device to be built using tuned shunt elements for power rectifier application. This device accomplished a striking improvement in wave shape as may readily be seen by reference to figure 17. In this figure the primary current and voltage wave shapes from oscillograms are shown both without



A-C RESONANT SHUNTS DISCONNECTED



A-C RESONANT SHUNTS CONNECTED

Figure 17. The a-c line current and voltage wave shapes of a six-phase rectifier without and with an a-c filter

Table IV. Harmonic Currents in the 4,600-Volt Supply Circuit of a Six-Phase 1,500-Kw Rectifier with A-C Filtering Equipment

A-C Harmonic Current in Amperes

I_{p1} = 120 Amperes; I_{dc} = 1,440 Amperes; A = 1.0

	Amperes in Resonant Shunts			Amperes in Supply Lines		
Frequency	Test	Empirical	Modified* Theoret.	Theoret.** Shunts Off	Tests Shunts Off	Tests Shunts On
300	18.5	20.5	23.3	21.1	15.7	0.50
420	11.0	13.0	15.0	13.4	7.6	0.70
660	7.3	7.0	8.9	5.4	5.2	0.49
780	6.0	5.7	6.9	3.6	3.3	0.23
1,020	3.4	3.8	3.9	1.7	1.7	0.19
1,140	2.5	3.1	3.2	1.4	1.4	0.09
1,380	1.75	2.2	1.95	1.0	1.4	0.13
$I \cdot T$					38,000	3,600

* From 300 to 1,380 cycles equivalent reactance corresponds to that of transformer alone. Based on $IX/E_s = 0.0275$ for these harmonic frequencies.

** Based on ratio of $IX/E_s = 0.083$ for all frequencies.

and with the a-c filtering equipment connected in the circuit. The effectiveness of the filter in improving the wave shape may also be seen from a comparison of the last two columns of table IV which show an over-all reduction in harmonics of more than ten to one.

Conclusions

The harmonic distortion of voltage and current in the supply circuits of grid-controlled rectifiers varies considerably with the load and the grid-control ratio. Certain harmonics may be increased to as much as four times the values without grid control.

A theoretical method is presented for the determination of the harmonic currents and voltages in the supply circuits of grid-controlled rectifiers and inverters. General curves are given for the quick determination of the harmonic currents and $I \cdot T$ products for the range of conditions usually encountered in actual operation.

This method has been shown to give values which provide a very satisfactory comparison with actual test results.

Notation

E_{dc} = average d-c circuit voltage

I_{dc} = total direct current load, assumed constant

I = direct current per secondary phase group

E' = root-mean-square value transformer primary voltage, line-to-neutral

E = root-mean-square value of transformer secondary voltage, anode-to-neutral

E_0 = crest value of transformer secondary voltage, anode-to-neutral $= \sqrt{2}E$

R = E'/E = transformer ratio, supply side to rectifier side, line-to-neutral voltages

p = number of phases in each secondary phase group*

q = total number of rectifier phases

X = commutating reactance, one half of anode-to-anode reactance

L = X/ω henries commutating inductance

ω = $2\pi f$ angular velocity of supply

θ = angular velocity, radians in formulas $= \omega t$

u = angle of overlap, radians in formulas

α = angle of grid delay for rectifiers, radians in formulas

α' = angle corresponding to the time available for deionizing for inverters, radians in formulas

$u + \alpha'$ = angle of grid advance for inverters, radians in formulas

A = $\cos \alpha$ = grid-control ratio, ratio of d-c circuit voltage with grid control to value without grid control

i = instantaneous current in reference anode

m = order of harmonic $= nq \pm 1$ where n = integer 1, 2, 3, ...

a_m = coefficient of sine term of Fourier expansion for the mth harmonic, crest value

b_m = coefficient of cosine term of Fourier expansion for the mth harmonic, crest value

a_1 = coefficient of sine term of Fourier expansion for the fundamental, crest value

b_1 = coefficient of cosine term of Fourier expansion for the fundamental, crest value

I_m = $\frac{I}{\sqrt{2}}\sqrt{a_m^2 + b_m^2}$ = root-mean-square value of the mth harmonic of the Fourier expansion

I_1 = $\frac{I}{\sqrt{2}}\sqrt{a_1^2 + b_1^2}$ = root-mean-square value of the fundamental of the Fourier expansion

I_{pm} = primary line current for the mth harmonic, root-mean-square value

I_{p1} = primary line current for the fundamental, root-mean-square value

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* p may also be defined as

$$p = \frac{360 \text{ degrees}}{(\text{conducting period in degrees}) - (\text{angle of overlap in degrees})}$$

The values of p for some of the common connections are: $p = 3$ for 6-phase double-wye and 12-phase quadruple-wye with interphase transformers; $p = 6$ for 6-phase star and 12-phase in double 6-phase relation; and $p = 12$ for 12-phase star.

Discussion

F. O. Stebbins (nonmember; General Electric Company, Schenectady, N. Y.): The paper by Messrs. Evans and Muller gives one more step in developing methods for estimating the wave shapes of mercury-arc rectifiers. The authors point out that the problem is divided into four different types of calculations. The first three parts consist of the wave shapes on the d-c side of a rectifier without grids, the wave shapes on the a-c side of a rectifier without grids, and the wave shapes on the d-c side of a rectifier equipped with grids. The authors now have covered the final phase of this problem, namely the wave shapes on the a-c side of a rectifier equipped with grids.

Subsequent to publication by the writer of the paper on the wave shapes on the d-c side of a rectifier equipped with grids, reference 3 in the authors' paper, work was continued and formulas similar to those given in this paper were derived for the wave shapes of the current on the a-c side. The results obtained with these formulas check the theoretical calculations given by the authors.

As a suggestion for practical applications the results presented in the authors' paper might be plotted in terms of per cent harmonic. The amount of harmonic current can then be determined directly from the curves for different rectifier loads and voltage control ratios.

In connection with the data given in table III of the paper it would be interesting to know if the test measurements were made in the factory or in the field. It has been our experience that in tests made in the factory where the impedance of the power-supply circuit approximates a straight line a good check between theoretical and measured values is obtained. However, in tests made in the field where the power-supply circuit may have several resonance points the agreement obtained between theoretical and test values was not as close, but was acceptable for practical purposes.

O. K. Marti (Allis-Chalmers Manufacturing Company, Milwaukee, Wis.): The authors of this interesting paper have summarized in an instructive way the results of a very important investigation. In large installations it is very essential to predetermine the magnitude of the harmonic currents, especially if grid voltage control is used. The analytical solution to determine the individual harmonic currents is rather tedious, and therefore the curves given by the authors will be of great assistance in analyzing systems in the future.

About two years ago we were confronted by such a problem—a very involved one. This was especially complicated by the fact that a large rectifier capacity was concentrated in one station. I am referring to the 55,000-kw rectifier installation at the Alcoa plant of the Aluminum Company of America. This station has ten 5,500-kw converting units consisting each of one transformer with two 12-anode rectifier tanks. The systems feeding the rectifiers sometimes have to be connected with systems spreading over a wide territory, and therefore the exposures existing between the transmission system and the telephone lines were of necessity of considerable magnitude.

Preliminary investigations showed that it would be necessary to go to a multiphase system far more elaborate than hitherto attempted. It was therefore decided to tackle the problem by building up a miniature test set instead of approaching it mathematically as was done by the authors of the paper. The set consisted of five units, each unit having a model rectifier transformer with six grid-controlled tubes (each tube representing an anode) and other auxiliary equipment.

Our first preliminary study disclosed the fact that the use of shunt filters as described by the authors could not be considered the way for solving our problem, as the cost of such filters proved to be prohibitive. A system of 30 phases was therefore set up

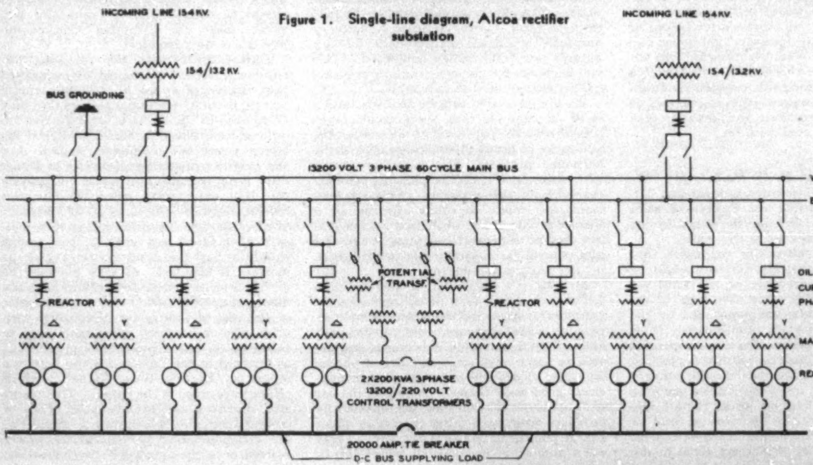

Figure 1. Single-line diagram, Alcoa rectifier substation

Figure 2. Twenty grid-controlled mercury-arc rectifiers, each rated 2,750 kw, 600 volts, at Alcoa plant of Aluminum Company of America

using autotransformers as phase shifters in order to obtain the necessary displacement between the individual units. A thorough investigation was carried out in co-operation with the engineers of the Bell Telephone Laboratories, Inc., using this model set-up at our test laboratory.

Since very little power was required, the study of the wave shapes with oscillograph and wave-shape analyzer for this system and these load characteristics could be carried out very easily and with the greatest convenience. Some of the results obtained with this set will be shown and described in a paper under preparation by T. A. Taylor of the Bell Telephone Laboratories and the writer, and will deal in detail with this installation in regard to co-ordination of power and telephone systems.

The method of phase shifting by means of autotransformers was finally adopted for all three plants. A single-line diagram with all the main equipment, such as phase-shifters, reactors, rectifier transformers, and tanks, is shown in figure 1 of this discussion. Figure 2 shows the rectifier room with control cubicles, heat exchangers, high-speed d-c breakers, and rectifier tanks.

The results obtained with this model 30-phase system set-up checked very favorably with the actually measured harmonic currents, and its usefulness exceeded our expectations. We therefore felt that such a way of approaching this problem to determine the harmonics in the a-c circuits of grid-controlled rectifiers and inverters may deserve further consideration.

R. D. Evans and **H. N. Muller, Jr.:** We were very much interested to learn that Mr. Stebbins has carried out theoretical work which checks the formulas developed by the authors and presented in the paper.

Mr. Stebbins offers the suggestion that the individual harmonic currents might be plotted as a percentage of the total a-c line current. This alternative form of expressing the results was considered by the authors. For certain applications it is necessary to determine the a-c line current and when this is done the method of plotting suggested by Mr. Stebbins is quite convenient. However, for other studies, such as inductive co-ordination work, the a-c line current need not be determined and it becomes somewhat simpler and more direct to use the method of plotting given in the paper. The quantities which have been used for plotting the general curves are those which are always available for any particular rectifier installation and it seemed preferable to use only these quantities instead of an additional derived quantity, the a-c line current, even though this must frequently be calculated. Perhaps it should be pointed out that both methods of plotting the general curves have the characteristic which permits one to visualize the ratios to be expected as a percentage or per unit ratio and with a little experience a sense of the proper magnitude to be expected is readily obtained.

In response to Mr. Stebbins' request we would like to point out that the data given in table III of the paper were obtained from factory tests and also that the data for tables II and IV were obtained from field tests. The supply circuit used in connection with the factory tests had a harmonic impedance characteristic which did not vary linearly over the important range of frequencies. This fact caused some of the comparisons between calculated and test values to vary considerably, although the results are still acceptable for practical purposes. The supply circuit used in the field tests of table II was selected because its harmonic impedance characteristic did vary linearly over the range measured. This fact accounts for the exceptionally accurate checks encountered in this table.

We are interested to note that Mr. Marti is of the opinion that the general curves presented in the paper will be of considerable assistance in future analytical studies of the harmonic problem. His laboratory work with miniature rectifiers presents another approach to this problem. This arrangement was made to apply directly to a specific problem, but additional work would be required to permit presenting the results in a general form comparable to that given by the analytical solution.

Mr. Marti's discussion of the use of a scheme involving multiphase operation of rectifiers for Alcoa is of considerable interest. Because of the large number of anodes available in this installation it becomes practicable to use a very large number of phases and thus to provide an installation of relatively good wave shape. Of course, the use of 6-phase rectifiers which are electrically displaced from each other to give in effect a large number of phases is not new. There are a number of installations where two 6-phase units have been operated from delta and star transformers to give the equivalent of 12-phase operation. We are informed that in one European installation two 12-phase rectifiers were connected through phase-shifting transformers to give the equivalent of 24-phase operation. This arrangement was adopted for the purpose of eliminating objectionable 11th and 13th harmonic currents which were amplified by partial resonance in the supply system. The particular scheme described by Mr. Marti is of interest because of the size of the installation and the number of units involved. The installation which he has shown in figure 1 of his discussion would appear to be the equivalent of a 60-phase rectifier system when all the units are in service and carrying symmetrical loads.

Mr. Marti comments on the use of connections giving the equivalent of a large number of phases as preferable to the use of an a-c filter. The authors recognize that where this multiplicity of phases is available the solution mentioned presents a more economical arrangement than filters. However, in the more usual case the number of units and particularly the number of anodes would be considerably less and this fact would make the foregoing solution impossible or uneconomical.

In connection with the foregoing discussion it should of course be recognized that the use of an a-c filter is only one of several possible remedial measures that may be applied in the event of an inductive co-ordination problem being encountered between power and telephone circuits. For the majority of rectifier installations an a-c filter is not required and would not provide the best engineering solution. For this reason, rectifier equipment is sold without filters. In case co-ordination studies indicate that filters are needed their design should be based on results of tests after the rectifier is installed. It may however be desirable for the interested utilities to obtain during the co-ordination studies an estimate of the cost of a filter for comparison with other measures. Such an estimate may be based on certain recommended proportions as outlined in the Edison Electric Institute report on "Rectifier Wave Shape" to which reference is given in the paper. This report also discusses all the available types of remedial measures which may be applied to the communication system, to the power system, or to the coupling between systems.

Equivalent Circuit Impedance of Regulating Transformers

J. E. CLEM
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AS SYSTEMS grow in complexity and service standards grow higher, the need for more accurate determination of system characteristics increases. From time to time the question of how to handle the impedance introduced into the circuit by a regulating transformer has arisen. This paper has been written to present general equations by means of which the equivalent circuit impedance of regulating transformers may readily be determined.

Regulating transformers are in effect autotransformers, but differ from them in that the series winding is on a separate core and receives its excitation from a section of the shunt winding. This difference introduces considerable complexity into the determination of the impedance introduced into the circuit.

1. INPUT TO SHUNT WINDING—OUTPUT FROM SERIES WINDING

Refer to figure 1 for the determination of the impedance from the shunt winding to the shunt plus series winding. The output voltage E_y is the input voltage E_x plus for step up or boost, or minus for step down or buck, the voltage E_a introduced into the circuit by the series transformer. As an equation

$$E_y = E_x \pm E_a \qquad (1)$$

But the voltage E_a is the voltage induced in the series transformer less the impedance drop:

$$E_a = kE_s \mp IZ_a \qquad (2)$$

and the voltage E_s is equal to the voltage induced in the excitation section less the impedance drop

$$E_s = m(E_x \mp mkIZ_{ps}) \qquad (3)$$

Now substitute (3) into (2) and (2) into (1) and get

$$E_y = (1 \pm mk)E_x - (m^2k^2Z_{ps} + Z_a)I \qquad (4)$$

from which

$$(1 \pm mk)E_x - E_y = (m^2k^2Z_{ps} + Z_a)I \qquad (5)$$

The left-hand term of (5) is the voltage drop E from the primary to the secondary circuit reckoned at the external terminal of the series transformer, so that

$$E = (m^2k^2Z_{ps} + Z_a)I \qquad (6)$$

To express this on a per-unit basis, both sides of (6) should be divided by the no-load ratio-voltage at the external terminal of the series transformer reckoned from the voltage of the shunt coil, that is by $(1 \mp mk)E_p$. And then

$$Z_{xy} = \frac{(m^2k^2Z_{ps} + Z_a)I}{(1 \pm mk)E_p} \qquad (7)$$

The plus sign is to be used for step up and the minus sign for step down.

2. INPUT TO SERIES WINDING—OUTPUT FROM SHUNT WINDING

Refer to figure 2 for the determination of the impedance from the shunt plus series winding to the shunt winding The output voltage E_y is the input voltage E_x minus, for step down or buck, or plus, for step up or boost, the voltage E_a introduced into the circuit by the series transformer

$$E_y = E_x \mp E_a \qquad (8)$$

Since the power flow is into the series coil and out from the shunt coil, the relationship between E_a and E_s is now

$$E_a = kE_s \pm IZ_a \qquad (9)$$

For the same reason the relationship between E_y and E_s becomes

$$E_s = m(E_y \pm mkIZ_{ps}) \qquad (10)$$

When (10) is substituted into (9) and (9) in turn substituted into (8) there results

$$E_y = E_x \mp mkE_y - (m^2k^2Z_{ps} + Z_a)I \qquad (11)$$

from which

$$E_x - (1 \pm mk)E_y = (m^2k^2Z_{ps} + Z_a)I \qquad (12)$$

The left-hand term of (15) is the voltage drop E from the primary to the secondary circuit, reckoned at the external terminal of the series transformer, so that

$$E = (m^2k^2Z_{ps} + Z_a)I \qquad (13)$$

To express this on a per-unit basis, both sides of (13) should be divided by the no-load ratio-voltage at the external terminal of the series transformers, reckoned from the voltage of the shunt coil. Then

$$Z_{xy} = \frac{(m^2k^2Z_{ps} + Z_a)I}{(1 \pm mk)E_p} \qquad (14)$$

The plus sign is to be used for step down and the minus for step up.

Equation 14 is identical with 7 indicating that direction of power flow has no effect. This should have been expected but circumstances indicated that it was desirable to demonstrate it.

3. EQUIVALENT PRIMARY TO SECONDARY IMPEDANCE BY SHORT-CIRCUIT TEST

Equations 7 and 14 can be obtained in a slightly different manner. In (4) let E_y be zero, that is, corresponding to an impedance test with the output side short-circuited. Then E_x becomes the impedance voltage E corresponding to the condition of current I in the series coil, so that (4) becomes

$$(1 \pm mk)E = (m^2k^2Z_{ps} + Z_a)I \qquad (15)$$

$$E = \frac{(m^2k^2Z_{ps} + Z_a)I}{1 \pm mk} \qquad (16)$$

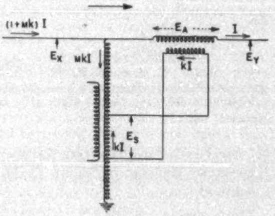

Figure 1

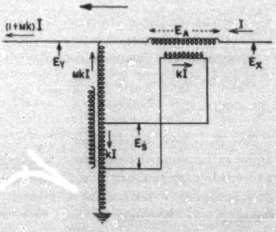

Figure 2

Paper number 39-100, recommended by the AIEE committee on electrical machinery, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3-5, 1939. Manuscript submitted March 24, 1938; made available for preprinting March 28, 1939.

J. E. CLEM is in the engineering division of the central-station department, General Electric Company, Schenectady, N. Y.

and on a per-unit basis

$$\mathbf{Z}_{xy} = \frac{E}{E_p} = \frac{(m^2k^2Z_{ps} + Z_a)}{(1 \pm mk)} \cdot \frac{I}{E_p} \qquad (17)$$

Reference to (7) shows that (17) is identical with it.

Now in (11) let E_y be zero, that is, corresponding to an impedance test with the output side short-circuited. Then E_x becomes the impedance voltage E corresponding to the condition of current I in the series coil, so that (11) becomes

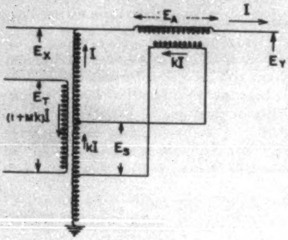

Figure 3

$$E = (m^2k^2Z_{ps} + Z_a)I \qquad (18)$$

Since the measurement is made on the series transformer side the no-load voltage is $(1 \pm mk)E_p$. To convert into a per-unit value both sides must be divided by $(1 \pm mk)E_p$.

$$\mathbf{Z}_{xy} = \frac{E}{(1 \pm mk)E_p} = \frac{(m^2k^2Z_{ps} + Z_a)I}{(1 \pm mk)E_p} \qquad (19)$$

Reference to (14) shows that (19) is identical with it.

4. Impedance From Tertiary Winding to Shunt Winding of Shunt Transformers

This is simply

$$\mathbf{Z}_{tz} = Z_{tp}(1 \pm mk)\frac{I}{E_p} \qquad (20)$$

5. Impedance From Tertiary Winding of Shunt Transformer to Shunt Winding Plus Series Winding

Refer to figure 3. As before

$$E_y = E_x \pm E_a \qquad (21)$$

But E_x is now derived from E_t. It is equal to E_t minus the drop from winding t to winding p, minus or plus the mutual drop produced by the current in the exciting section of the shunt winding.

$$E_x = E_t - IZ_{tp} \mp mkIM_{ps} \qquad (22)$$

In which

$$M_{ps} = (Z_{tp} - Z_{ps} + Z_{st})/2 \qquad (23)$$

And in the same manner

$$E_a = kE_s \mp IZ_a \qquad (24)$$

and

$$E_s = m(E_t \mp mkIZ_{ts} - IM_{ps}) \qquad (25)$$

The upper sign corresponds to the condition when the voltage at the external terminal of the series transformer is greater than the voltage across the shunt transformers, the bottom sign when it is less.

Now substitute (25) into (24) and then both (24) and (22) into (21) and

$$E_y = (1 \pm mk)E_t - I(Z_{tp} + m^2k^2Z_{ts} + 2mkM_{sp} + Z_a) \qquad (26)$$

and then

$$(1 + mk)E_t - E_y = (Z_{tp} + m^2k^2Z_{ts} + 2mkM_{sp} + Z_a)I \qquad (27)$$

As before the left-hand term of this equation is the impedance drop E as it appears at the external terminal of the series transformer where the no-load ratio-voltage is $(1 \pm mk)E_p$. And so

$$E = (Z_{tp} + m^2k^2M_{sp} + 2mkM_{sp} + Z_a)I \qquad (28)$$

and on a per-unit basis

$$\mathbf{Z}_{ty} = \frac{E}{(1 \pm mk)E_p} \qquad (29)$$

$$\mathbf{Z}_{ty} = \frac{Z_{tp} + m^2k^2Z_{ts} \pm 2mkM_{ps} + Z_a}{1 \pm mk}\,\frac{I}{E_p} \qquad (30)$$

6. Summary of Equations—Boost Condition

$$\mathbf{Z}_{xy} = \frac{r^2Z_{ps} + Z_a}{1 + r}\,\frac{I}{E_p} \qquad (31)$$

$$\mathbf{Z}_{yt} = \left(Z_{tp} + rZ_{st} + \frac{rZ_{ps} + Z_a}{1 + r}\right)\frac{I}{E_p} \qquad (32)$$

$$\mathbf{Z}_{tz} = Z_{tp}(1 + r)\frac{I}{E_p} \qquad (33)$$

The relationship

$$M_{ps} = Z_t = (Z_{tp} - Z_{ps} + Z_{st})/2 \qquad (23)$$

has been inserted in (30) to get (32). Also r has been written for mk.

7. Summary of Equations—Buck Position

$$\mathbf{Z}_{xy} = \frac{r^2Z_{ps} + Z_a}{1 - r} \cdot \frac{I}{E_p} \qquad (34)$$

$$\mathbf{Z}_{yt} = \left(Z_{tp} - rZ_{st} + \frac{rZ_{ps} + Z_a}{1 - r}\right)\frac{I}{E_p} \qquad (35)$$

$$\mathbf{Z}_{tz} = Z_{tp}(1 - r)\frac{I}{E_p} \qquad (36)$$

The impedances of the various branches can be determined by the conventional three-winding theory, when care is taken to see that the equations are properly handled. Equations 31 and 34 give the positive- and negative-sequence impedances and 31, 32, 33 or 34, 35 and 36 as a network give the zero-sequence impedances.

Effect of Angular Component

The foregoing development applies to a regulating transformer for in-phase, or direct, regulation only. The same method of analysis may be applied to a regulating transformer that has both direct and angular regulation. Only the final equations will be given. Refer to figure 4—phase sequence is ABC.

$$\mathbf{Z}_{ps} = \mathbf{Z}_1 + \mathbf{Z}_2 + \mathbf{Z}_3 \qquad (37)$$

$$\mathbf{Z}_1 = m_1k_1\left(m_1k_1Z_{p1} - am_2k_2M_{12} + a^2m_3k_3M_{31} + \frac{Z_{a1}}{m_1k_1}\right)\frac{I}{E_s} \qquad (38)$$

$$\mathbf{Z}_2 = m_2k_2\left(m_2k_2Z_{p2} - am_3k_3M_{23} - a^2m_1k_1M_{12} + \frac{Z_{a2}}{m_2k_2}\right)\frac{I}{E_s} \qquad (39)$$

$$\mathbf{Z}_3 = m_3k_3\left(m_3k_3Z_{p3} + am_1k_1M_{31} - a^2m_2k_2M_{23} + \frac{Z_{a3}}{m_3k_3}\right)\frac{I}{E_s} \qquad (40)$$

$$E_s = (1 \pm m_1k_1 \mp a^2m_2k_2 \pm am_3k_3)\,E_p \quad \text{at no load} \qquad (41)$$

$$\mathbf{Z}_{st} = \mathbf{Z} + \mathbf{Z}_1 + \mathbf{Z}_2 + \mathbf{Z}_3 \qquad (42)$$

$$\mathbf{Z} = (Z_{pt} \pm m_1k_1M_{p1} \mp m_2k_2M_{p2} \pm m_3k_3M_{p3})\frac{I}{E_s} \qquad (43)$$

$$\mathbf{Z}_1 = m_1k_1\left(m_1k_1Z_{t1} \pm M_{p1} - m_2k_2M_{12} + m_3k_3M_{31} + \frac{Z_{a1}}{m_1k_1}\right)\frac{I}{E_s} \qquad (44)$$

$$\mathbf{Z}_2 = m_2k_2\left(m_2k_2Z_{t2} \mp M_{p2} - m_3k_3M_{23} - m_1k_1M_{12} + \frac{Z_{a2}}{m_2k_2}\right)\frac{I}{E_s} \qquad (45)$$

$$\mathbf{Z}_3 = m_3k_3\left(m_3k_3Z_{t3} \pm M_{p3} + m_1k_1M_{31} - m_2k_2M_{23} + \frac{Z_{a3}}{m_3k_3}\right)\frac{I}{E_s} \qquad (46)$$

$$\mathbf{Z}_{tp} = Z_{tp}\frac{I_p}{E_p} \qquad (47)$$

Equation 47 should be handled carefully. The (+) applies when the boosters increase the voltage or increase the phase angle, that is, for boost in voltage or boost in phase angle.

Note that the quantities $\mathbf{Z}_1$, $\mathbf{Z}_2$, and $\mathbf{Z}_3$ in (42) are not the same as in (37).

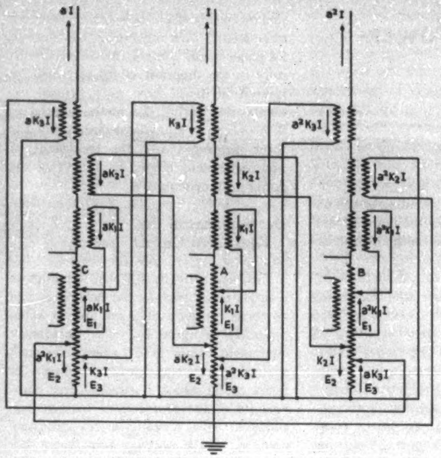

Figure 4

$\mathbf{Z}_{ps}$ = per-unit impedance from circuit which excites shunt winding, to circuit supplied from booster winding. See figure 4

$\mathbf{Z}_{st}$ = per-unit impedance from circuit supplied from booster winding, to tertiary winding on shunt transformer

$\mathbf{Z}_{tp}$ = per-unit impedance from tertiary winding on shunt transformer, to circuit which excites shunt winding

Subscripts 1, 2, 3, respectively refer to in-phase boosters, a^2 phase boosters, and a phase boosters, respectively; see figure 4.

Subscripts p, t, refer to total shunt windings and tertiary windings on shunt transformers.

k = ratio of turns in booster transformers, series line coil to excited coil

m = ratio of turns in exciting sections of shunt winding to total turns in shunt winding

All impedances of windings in the shunt transformers are in ohms, and on the basis of the total turns in the shunt windings.

Impedances of the booster transformer are in ohms, and on the basis of the turns in the series line coil.

Mutual impedances in (37), (38), (39), are referred to in the shunt winding p.

Mutual impedances in (43), (44), (45), (46), are referred to the tertiary winding t.

For example, M_{12} in (38) and (39) is given by

$$M_{12} = \frac{Z_{p1} - Z_{12} + Z_{2p}}{2} \tag{48}$$

M$_{12}$ in (44) and (45) is given by

$$M_{12} = \frac{Z_{t1} - Z_{12} + Z_{2t}}{.2} \tag{49}$$

Any two of the booster coils may be combined. Frequently the two angle boosters are combined to obtain a quadrature component. In this case k_q would be used to designate the turn ratio of the quadrature booster transformers and would replace both k_2 and k_3 in the equations, no other change being needed.

The impedance given by equation 37 is for positive sequence. For negative sequence interchange the operators a and a^2. For zero-sequence place the operators a and a^2 in (37), (38), and (39), equal to 1 to get the through impedance circuit to circuit, and use in the conventional three-circuit network with the other two zero-sequence impedances, circuit to ground, to get the proper branch impedances.

When the transformer neutral is grounded through an impedance of Z_n ohms, the impedances of the branches of the zero-sequence network are affected. They become, with the subscript g added to indicate the impedance when grounded through a neutral grounding impedance,

$$\mathbf{Z}_{gps} = \mathbf{Z}_{ps} + 3(m_1k_1 - m_2k_2 + m_3k_3)^2\mathbf{Z}_n \tag{50}$$

$$\mathbf{Z}_{gst} = \mathbf{Z}_{st} + 3\mathbf{Z}_n \tag{51}$$

$$\mathbf{Z}_{gtp} = \mathbf{Z}_{tp} + 3\mathbf{Z}_n \tag{52}$$

$$\mathbf{Z}_n = Z_n \frac{I}{E_s} \tag{53}$$

List of Symbols

E_x = primary, or input, voltage

E_y = secondary, or output, voltage

E_a = voltage introduced into the circuit by the series transformer

E_p = voltage across total winding of shunt transformer

E_s = voltage across exciting section of shunt transformer

E_t = voltage across tertiary winding of shunt transformer

k = ratio of turns, series winding to excited winding, of series transformer

m = ratio of turns, excitation section to total turns, of shunt transformer

r = mk

Z_a = impedance of series transformer, in ohms based on turns in series winding

Z_{ps} = impedance of shunt transformer from total winding to excitation section, in ohms based on total turns of shunt winding

Z_{st} = impedance of shunt transformer from excitation section to tertiary winding, in ohms based on total turns of shunt winding

Z_{tp} = impedance of shunt transformer from tertiary winding to total shunt winding in ohms based on total turns of shunt winding

$\mathbf{Z}_{xy}$ = per-unit impedance from input to output side

$\mathbf{Z}_a$, $\mathbf{Z}_{ps}$, $\mathbf{Z}_{st}$, $\mathbf{Z}_{tp}$ = per-unit impedances based on the total turns of the shunt coil, except $\mathbf{Z}_a$ which is based on the turns in the series line coil. Based on current and voltage of circuit connected to external terminal of series coil

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Discussion

W. A. Lewis (Cornell University, Ithaca, N. Y.): The paper by Mr. Clem covers a similar subject to the paper by Doctor Hobson and myself ("Regulating Transformers in Power System Analysis, AIEE TRANSACTIONS, volume 58, 1939, pages 874–86), and a comparison of the two may serve to bring out a little more strongly the principles involved. Mr. Clem develops the equivalent circuits for one general type of regulating transformer, in which the exciting windings are star-connected. His method is basically the same as ours, and his equations could be applied directly to the figures in our paper.

If the exciting transformers are connected in delta, a 30-degree phase shift is introduced in the voltage added to the incoming voltage by the series winding, and a transformer which originally had a star-connected exciting winding becomes a phase-angle regulator, and the transformer which was originally designed for phase-angle regulation can be arranged for voltage regulation when the exciting windings are connected in delta. The unusual phenomena described in our paper are characteristic primarily of regulating transformers using the delta connection for the exciting circuit, and therefore do not appear when only the star-connected case is considered.

In Mr. Clem's paper the final results are given in per-unit or per-cent notation, but the basic data on the transformer elements are introduced in ohms. In our paper, on

Regulating Transformers in Power-System Analysis

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IN THE analysis of power systems it is customary to represent generators, transformers, and transmission lines by their equivalent circuits. The resulting circuit network is solved either by representation on a network calculator, from which the solution is read directly, or analytically by successive simplification of the network. When the network has been simplified as far as possible, it is usually relatively easy to solve the simplified network, by the application of Kirchhoff's laws, for the currents in the remaining circuits and the voltages at the remaining terminals. When this solution has been obtained, the steps of simplification are reversed until the original network is restored, the currents and voltages being calculated at each step, until the desired quantities are finally obtained for the original network. When the system is unbalanced, as by an unbalanced fault condition, the method of symmetrical components[1] is widely used. Equivalent networks are derived for the positive-, negative-, and zero-sequence components of current and voltage. Interconnections between the sequence networks are made, corresponding to the unbalanced condition, and the resulting network is solved by the methods just described.

The equivalent circuits used to represent complicated power and regulating transformers have not previously been published and they often constitute an important element of the power-system representation. Furthermore, the analysis of systems involving such transformers has brought to the attention of the authors several unusual problems, whose solution is not well known. Believing the solution of these problems would be of general interest, the authors have presented several examples in this paper, which are illustrative of the problems encountered.

The regulating transformer may introduce a phase shift in the voltage and current as well as a change in magnitude. This differs from the usual star-delta power transformation in that the angle of phase shift is not fixed but depends on the tap position. Also, it quite frequently happens that there are connections between the two circuits (connected by the regulating transformer) other than through the regulating transformer itself. The other connection (or connections) may be direct, as is usually the case when the regulator is used to control the flow of power in a closed loop, or may be through another transformer having transformation characteristics differing from those of the regulator with which it is essentially connected in parallel. A number of the winding connections used in regulating transformers result in a transformation ratio for zero-sequence current and voltage which is unequal to the transformation ratio for positive- or negative-sequence quantities. The regulating transformer for phase-angle control, shown schematically in the diagram of figure 1 will be treated in detail here as a typical example, illustrating the methods used in developing the equivalent circuits for the three sequences and the treatment of each equivalent circuit as a part of the power-system sequence network.

Development of the Equivalent Circuit

Two magnetically coupled windings of a single-phase transformer having n_1 and n_2 turns, respectively, are shown schematically in figure 2*a*. The customary equivalent circuit used to represent such a single-phase transformer is shown in figure 2*b*, in which Z_A and Z_B are components of the transformer leakage impedance, with a more-or-less arbitrary division of the leakage impedance between Z_A and Z_B. Z_M is the so-called "magnetizing shunt branch." Since the numerical value of Z_M is very large compared to Z_A and Z_B, for most calculations, figure 2*b* is approximated by figure 2*c*, where Z_M is considered infinite. Either of these circuits has serious deficiencies as a device representing the actual transformer; the voltage and current transformation effected by transformer action is not represented in the equivalent circuit, and the circuit terminals *a* and *a'* are not insulated from each other as in the actual transformer. These disadvantages are evidenced particularly when analyzing transformer circuits wherein several windings or phases are interconnected. To overcome these deficiencies the authors have found it expedient to use the equivalent circuit shown in figure 2*d*, which combines the circuit of figure 2*b* with an ideal transformer. The ideal transformer is defined as having infinite

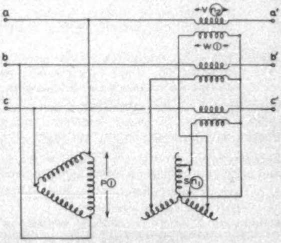

Figure 1. Typical phase-angle regulating transformer

Paper number 39-98, recommended by the AIEE committee on power transmission and distribution, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3-5, 1939. Manuscript submitted February 27, 1939; made available for preprinting March 28, 1939.

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The authors wish to acknowledge the assistance of their associates, particularly Clayton F. Hall, whose aid in checking the derivations and the illustrations has been invaluable.

1. For numbered reference, see end of paper.

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the other hand, when our results are expressed in per cent, the basic data on the transformer elements are also expressed in per cent, on the kilovolt-ampere parts which that particular element will be required to carry when the regulating transformer as a whole is carrying rated kilovolt-amperes. When these differences are reconciled, our results are in agreement with the results given by Mr. Clem. We have felt that the form we have given would be somewhat more useful for preliminary estimating purposes, since the first step in the design of a regulating transformer is to determine the voltage and kilovolt-ampere rating of each transformer element. With this information at hand, it is usually possible to estimate from typical designs a per-cent reactance of each transformer element on its own kilovolt-amperes, and these values may be immediately introduced into the formulas to determine the over-all characteristics, on the kilovolt-amperes of the entire regulating transformer as a basis.

exciting impedance (zero exciting current or no load current) and zero leakage impedance, and serves to transform voltage and current without impedance drop or power loss; the ideal transformer thus restores actual voltage and current relationships at the terminals *a* and *a′*. The circuit of figure 2*e* is obtained from figure 2*d* by converting the impedance Z_B to the E_a' voltage base (by multiplying Z_B by the square of the voltage ratio). This process may be thought of as "sliding the ideal transformer through" the impedance Z_B. If the exciting, or no load, current may be neglected (Z_M considered as infinite) the circuit of figure 2*e* becomes figure 2*f*.

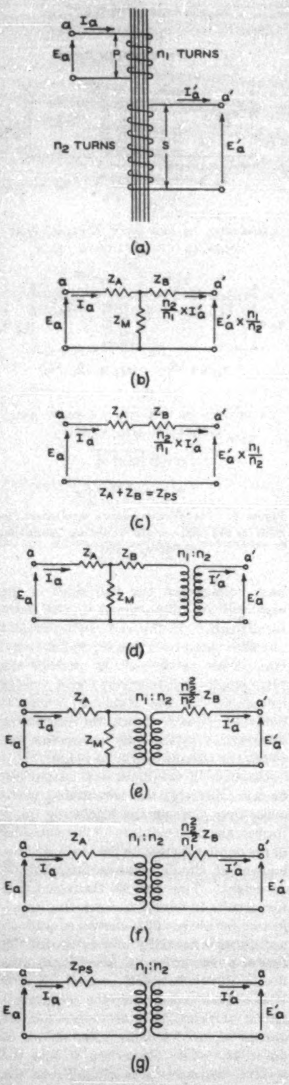


Figure 2. Equivalent circuits for a two-winding transformer

Finally, if Z_M is considered infinite, the circuit of figure 2*d* becomes figure 2*g*, in which the two parts of the leakage impedance, Z_A and Z_B, combine into the complete leakage impedance Z_{PS}, where

$$Z_{PS} = Z_A + Z_B$$

In most developments the circuit of figure 2*g* will be found most convenient, although in some cases it becomes desirable to have part of the leakage impedance associated with each winding, and the circuit of figure 2*f* may be used.

To be perfectly definite, Z_{PS} is understood to mean the leakage impedance, as measured in ohms, with the *S* winding short-circuited, and voltage applied to the *P* winding. When the test is reversed, with voltage applied to the *S* winding, and the *P* winding short-circuited, the impedance is denoted by Z_{SP}. It is obvious from the development given that, when Z_M may be considered infinite,

$$Z_{SP} = \frac{n_2^2}{n_1^2} Z_{PS}$$

Phase-Angle Regulator

The diagram of figure 1 is redrawn in figure 3, using the type of equivalent circuit shown in figure 2*g* for each of the single-phase units involved. It will be noted that each exciting transformer is denoted by the letters *PS*, *P* representing the primary winding and *S* the secondary. Also, each series transformer is denoted by *VW*, *V* denoting the winding in series with the line, and *W* the winding connected to the exciting transformer. The impedances Z_{PS} and Z_{VW} are the leakage impedances of the exciting and series transformers, respectively. For convenience, the relative numbers of turns of the transformer windings are taken as $1{:}n_1$, for the exciting transformer and $1{:}n_2$ for the series transformer, and each winding is assumed to have a number of turns proportional to the associated number or letter enclosed in a circle on the diagram. As the tap position of winding *S* changes, it must be noted that n_1 and also Z_{PS} change. Care must be taken to use the proper values for each tap.

It is obvious that the circuit shown is symmetrical, that is, the construction of all three phases is the same. It is well known in the theory of symmetrical components, that in such cases, positive-sequence currents flowing produce only positive-sequence reactions, so that the equivalent circuit for the positive-sequence diagram may be derived by circulating positive-sequence currents through the regulating transformer and computing the voltage relations, using the diagram of figure 3. The details of this procedure are given in appendix I.

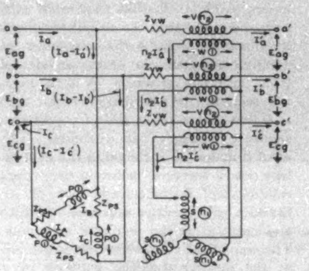
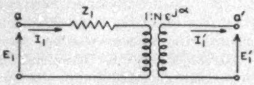


Figure 3. Three-phase equivalent circuit for the transformer of figure 1

Equations 15 and 17 of appendix I give the transformer action of the regulator to positive-sequence votlages and currents, *N* being the transformation ratio and α being the angle of phase shift. Equation 18 gives the impedance of the regulating transformer; namely, $Z_1 = 1/N^2 \times (n_1^2 n_2^2 Z_{PS} + Z_{VW})$ ohms, on the voltage base of the left-hand, or input, circuit as viewed from the terminals *a*, *b*, and *c*. It should be noted that the transformation ratio, the angle of phase shift, and the impedances Z_{PS} and Z_1 are each functions of the tap setting (that is, functions of n_1), and vary with the tap being used. When the tap changer mechanism is in the neutral position $N = 1$, $\alpha = 0$, and $Z_1 = Z_{VW}$ ohms.

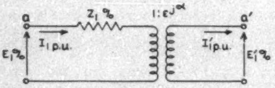
Any circuit satisfying equations 15, 17, and 18, will be a positive-sequence equivalent circuit for the regulating transformer. Those who are familiar with the use of symmetrical components know that a single-phase diagram may be used to represent the three phases if the phase quantities are balanced and symmetrical as, for example, under the positive-sequence conditions assumed in appendix I. The suggested positive-sequence equivalent circuit is given in figure 4*a* in which E_1 and I_1 are, respectively, the positive-sequence components of the line-to-ground voltages and line currents, at the input terminals *a*, *b*, and *c*; E_1' and I_1' are, respectively, the positive-sequence components of the line-to-ground voltages and currents at the output terminals *a'*, *b'*, and *c'*; Z_1 is the impedance to positive sequence, in ohms, as viewed from the input terminals; N is the positive-sequence transformation ratio; and α is the phase-angle shift for positive-sequence voltages and currents. The ideal transformer included in the equivalent circuit serves to maintain the proper relationship, in magnitude and phase, between the positive-sequence voltages and currents at the regulator terminals. The equivalent circuit may be connected in the positive-sequence network of a system including the regulator for phase-angle control, to maintain the same relationships as the regulating transformer exhibits between positive-sequence quantities at the input and output terminals.

The equivalent circuit as derived has introduced one new conception, that of incorporating a phase-angle shift in the ideal transformer. The circuit including the ideal transformer is easy to handle analytically as a part of the positive-sequence network; but, unfortunately, it is not convenient at the present time to represent such a phase-shifting transformer on the network calculator, and the customary procedure is to include a transformer with ratio N and take the phase shift into consideration by analytical methods in the final results, or to make a special setup, using two power sources of the network calculator to absorb and reintroduce power at the proper phase positions, giving the effect of the phase shift. However, the equivalent circuit as given is very useful in visualizing the action of the regulating transformer as it affects positive-sequence quantities.



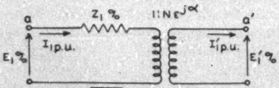
$$Z_1 = \frac{1}{N^2}(n_1^2 n_2^2 Z_{PS} + Z_{VW})$$

(a)
EXPRESSED IN OHMS



$$Z_1\% = \frac{\sqrt{N^2-1}}{N}(Z_{PS}\% + Z_{VW}\%)$$

(b)
EXPRESSED IN PER CENT, CIRCUIT BASE VOLTAGES PROPORTIONAL TO N



$$Z_1\% = \frac{\sqrt{N^2-1}}{N}(Z_{PS}\% + Z_{VW}\%)$$

(c)
EXPRESSED IN PER CENT, CIRCUIT BASE VOLTAGES THE SAME.

$$N = \sqrt{1+3n_1^2 n_2^2}$$

$$\alpha = \tan^{-1}\sqrt{3}\, n_1 n_2$$

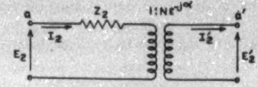
Figure 4. Positive-sequence equivalent circuits for the phase-angle regulating transformer of figures 1 and 3

Percentage Representation Positive Sequence

Many investigators of analytical problems prefer to use a per-unit or percentage system of representation, in which all quantities such as voltage, current, kilovolt-amperes, and impedance, are expressed as a fraction or percentage of some base or normal value. In some cases, also, it is advantageous to make numerical calculations in per-unit or percentage terms. Consequently, it becomes desirable to express the equivalent circuits in similar notation.

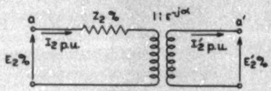
If the percentage or per-unit diagram is to be useful, it is essential that the base quantities used throughout be consistent. This is best achieved by selecting a minimum number of quantities and solving for the corresponding values of the remaining quantities. Thus, it is usually best to select a base power or kilovolt-ampere value for the entire system, and a voltage value for each metallically connected circuit, which is usually taken as the nominal voltage of that part of the system. It then becomes readily possible to express the basic equations of the equivalent circuit, such as equations 15, 17, and 18 of appendix I, in percentage values. The development is given in appendix II, for the phase-angle regulator previously discussed. Two sets of equations are derived; the first given by equations 24 and 25, for which the equivalent circuit is given in figure 4*b*; and the second given by equations 26 and 27, for which the equivalent circuit is given by figure 4*c*. The essential difference between the two cases lies in the voltages selected as the base voltages for the two sides of the regulating transformer.

The maximum convenience in the use of the percentage system results when the base voltages on the two sides of the regulator are proportional to the transformer ratio. In this case the equivalent circuit is given by figure 4*b*, and the ideal transformer serves only to provide the phase-angle shift of current and voltage introduced by the regulator. This is normally possible when the regulating transformer is the only connection between the circuits on the two sides.



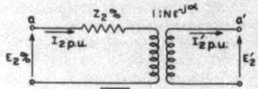
$$Z_2 = Z_1 = \frac{1}{N^2}(n_1^2 n_2^2 Z_{PS} + Z_{VW})$$

(a)
EXPRESSED IN OHMS



$$Z_2\% = \frac{\sqrt{N^2-1}}{N}(Z_{PS}\% + Z_{VW}\%)$$

(b)
EXPRESSED IN PER CENT, CIRCUIT BASE VOLTAGES PROPORTIONAL TO N



$$Z_2\% = \frac{\sqrt{N^2-1}}{N}(Z_{PS}\% + Z_{VW}\%)$$

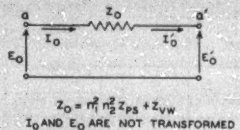
(c)
EXPRESSED IN PER CENT, CIRCUIT BASE VOLTAGES THE SAME.

$$N = \sqrt{1+3n_1^2 n_2^2}$$

$$\alpha = \tan^{-1}\sqrt{3}\, n_1 n_2$$

Figure 5. Negative-sequence equivalent circuits for the phase-angle regulating transformer of figures 1 and 3

However, if the input and output circuits are directly interconnected by paths other than through the regulating transformer, the base voltages for the two sides of the regulator must be the same, and the equivalent circuit must be adjusted accordingly. This will be the usual case for circuits involving a regulating transformer for phase-angle control, since such apparatus is normally used to control the flow of power around a closed loop, or to divide power between parallel circuits. Under such conditions the equivalent circuit of figure 4*b* is no longer suitable and is replaced by the form shown in figure 4*c*, before connecting it into the system sequence network. From the figure it is seen that the only difference between the two circuits is the inclusion

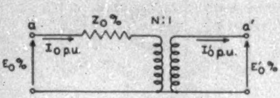


$$Z_0 = n_1^2 n_2^2 Z_{PS} + Z_{VW}$$

I_0 AND E_0 ARE NOT TRANSFORMED

(a)

EXPRESSED IN OHMS

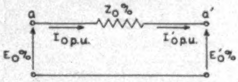


$$N = \sqrt{1+3n_1^2 n_2^2}$$

$$Z_0\% = N\sqrt{N^2-1}\,(Z_{PS}\% + Z_{VW}\%)$$

(b)

EXPRESSED IN PER CENT, CIRCUIT BASE VOLTAGES PROPORTIONAL TO N.



$$Z_0\% = N\sqrt{N^2-1}\,(Z_{PS}\% + Z_{VW}\%)$$

(c)

EXPRESSED IN PER CENT, CIRCUIT BASE VOLTAGES THE SAME.

Figure 6. Zero-sequence equivalent circuits for the phase-angle regulating transformer of figures 1 and 3

of a transformation ratio in the idea transformer.

The above discussion points out the possibilities of confusion which are created when system sequence networks involving regulating transformers are set up using percentage quantities. To avoid this confusion, and to keep the regulator action in the different sequences clearly in mind, it is usually preferable to set up the sequence network in ohms, and to use volts, amperes, and ohms in the system analysis.

Negative-Sequence Equivalent Circuit

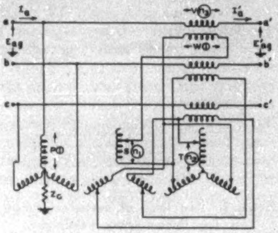
The negative-sequence equivalent circuit for the same regulating transformer may be derived by assuming negative-sequence currents to be circulated through the regulator diagram of figure 3. The derivation will not be given here in detail, since it is very similar to that just given for positive sequence. The results are given in figures 5*a*, *b*, and *c*, which show the negative-sequence equivalent circuits expressed in ohms and in per cent. Figures 5*b* and 5*c* correspond, respectively, to figures 4*b* and 4*c*, and the discussion with regard to the two forms of the positive-sequence equivalent circuit in per cent applies directly. It will be noted that the only difference between the positive- and negative-sequence equivalent circuits is in the phase-angle shift, the angle for negative sequence being the negative of that for positive sequence. This is a general rule for all power or regulating transformers; the positive and negative equivalent circuits are identical, except that the phase shift (if any is involved by the transformer) for negative sequence is negative to that for positive sequence. The impedances to positive and negative sequence are always identical; as, indeed, is the case for all static apparatus.

Zero-Sequence Equivalent Circuit

The zero-sequence equivalent circuit for the regulating transformer may be derived by assuming zero-sequence current to be circulated through the regulator. The derivation of the zero-sequence relationships is given in appendix III. Equations 39 and 46 reveal that neither zero-sequence current nor zero-sequence voltage are transformed by the regulating transformer; there is no transformer action on zero-sequence quantities, and the transformer acts merely as a reactor in the zero-sequence network. Again, it should be noted that Z_{PS} and the impedance to zero sequence, $Z_0 = n_1^2 n_2^2 Z_{PS} + Z_{VW}$, are functions of the tap position and Z_0 becomes Z_{VW} (the leakage impedance of the series transformer referred to the line-side winding) when the tap changer mechanism is in the neutral position. It should also be noted here that the zero-sequence impedance of the regulator has the same ohmic value viewed from either the input or the output circuit terminals. This is shown in the zero-sequence equivalent circuit given in figure 6*a*, which is set up to satisfy the equations in appendix III. There is no ideal transformer in this circuit, since zero-sequence current and voltage are not transformed.

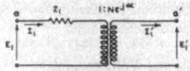
To obtain the zero-sequence equivalent circuit, expressed in per cent, the same procedure is followed as given in appendix II for positive sequence. Again two types are required, depending upon whether the base voltages on the two sides of the transformer can be chosen proportional to the ratio transformation of the regulator, *N*, or are required to be the same because the circuits are directly connected by a parallel path. To be consistent for any given problem, the base voltages used must be the same as used in the corresponding positive- and negative-sequence equivalent circuits.

The zero-sequence equivalent circuit, expressed in per cent, is given for the first case in figure 6*b* to satisfy equations 50 and 51. It is necessary to include an ideal transformer in this circuit, having an inverse transformation ratio to that in the positive- or negative-sequence



(a)

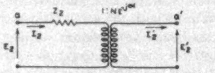
THREE PHASE EQUIVALENT CIRCUIT



$$Z_1 = \frac{1}{N^2}\left(Z_{VW} + n_1^2 n_3^2 Z_{PS} + 3n_2^2 n_3^2 Z_{PT}\right)$$

(b)

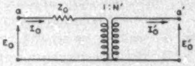
POSITIVE SEQUENCE, EXPRESSED IN OHMS



$$Z_2 = Z_1 = \frac{1}{N^2}\left(Z_{VW} + n_1^2 n_3^2 Z_{PS} + 3n_2^2 n_3^2 Z_{PT}\right)$$

(c)

NEGATIVE SEQUENCE, EXPRESSED IN OHMS



$$N' = 1 + n_1 n_3 = \sqrt{N^2 - 3n_2^2 n_3^2}$$

$$Z_0 = \frac{1}{(N')^2}\left[Z_{VW} + n_1^2 n_3^2 (Z_{PS} + 3Z_G)\right]$$

(d)

ZERO SEQUENCE, EXPRESSED IN OHMS

$$N = \sqrt{(1+n_1 n_3)^2 + 3n_2^2 n_3^2}$$

$$\alpha = \tan^{-1}\frac{\sqrt{3}\,n_2 n_3}{1+n_1 n_3}$$

Figure 7. Regulating transformer for voltage and phase-angle control, with the sequence equivalent circuits expressed in ohms

The parts ratios are:

$$\frac{U_V}{U_C} = \frac{U_P}{U_C} = \frac{n_3\sqrt{n_1^2 + 3n_2^2}}{N}$$

$$\frac{U_S}{U_C} = \frac{n_1 n_3}{N} \qquad \frac{U_T}{U_C} = \frac{\sqrt{3}\,n_2 n_3}{N}$$

equivalent circuits expressed in ohms, to maintain the proper relationships between per-unit zero-sequence currents and percentage zero-sequence voltages at the terminals. For the second case, in which the base voltages are the same, the equivalent circuit is given in figure 6*c*, to satisfy equations 52 and 53.

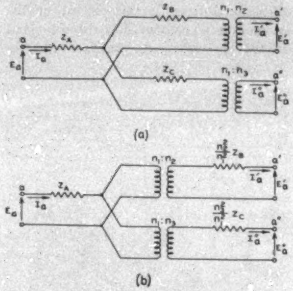


Figure 8. Two forms of equivalent circuits for the three-winding transformer

Regulator for Voltage and Phase-Angle Control

The example just given, in which zero-sequence voltage and current are not transformed with the same ratio as positive- and negative-sequence voltages and currents (or, as in the example, are not transformed at all) is quite common among regulating transformers. For regulating transformers used for voltage control alone, or for phase-angle control alone, it is found that zero-sequence current and voltage are transformed in the same ratio as positive, or are not transformed at all. In some regulating transformers for combination voltage and phase-angle control, it is found that zero-sequence quantities are transformed, but with a different ratio from that for positive- and negative-sequence quantities. In all cases, however, it is found that the zero-sequence equivalent circuit involves no phase shift, regardless of the phase shift introduced in the positive- and negative-sequence circuits.

A typical example involving different ratios is the regulating transformer for combined voltage and phase-angle control shown in figure 7*a*. The same notation is followed, to designate individual windings, as was used in figure 3. Z_G is the neutral impedance between ground and the neutral of the star-connected primary winding of the exciting transformer.

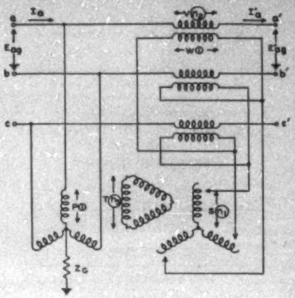
It will be observed that the exciting transformer in this case is a three-winding transformer, having the windings *P*, *S*, and *T* on the same core. The conventional equivalent circuit for such a transformer, assuming that the exciting impedance is infinite, has been developed a number of times before,[1] but is subject to the same limitations that characterize the circuit of figure 2*c*, namely, that the actual ratio of transformation is not provided for, and the terminals of the windings are not insulated from each other. These difficulties are again overcome by introducing ideal transformers of the proper turn ratios. Two forms of such equivalent circuits are shown in figure 8, *a* and *b*. It should be noted that the leakage impedances are broken up into components Z_A, Z_B, and Z_C, which are no longer arbitrary, but each is a definite function of the three leakage impedances between pairs of windings. Formulas for determining the components are given in the reference cited.[1]

Using this type of equivalent circuit for the exciting transformer, the positive-, negative-, and zero-sequence equivalent circuits for the entire regulating transformer can be derived by a process essentially similar to that already described. Space does not permit including the development, but the results are included in the figures.

The positive-sequence equivalent circuit expressed in ohms is given in figure 7*b*. To make certain the definitions of the quantities are clearly understood, they are given here (following the same convention for subscripts as used in the first example).

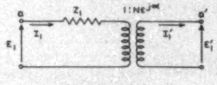
Z_{VW} = leakage impedance, in ohms, between the *V* and *W* windings as measured on the *V* winding with the *W* winding short-circuited
Z_{PS} = leakage impedance, in ohms, between the *P* and *S* windings as measured on the *P* winding with the *S* winding short-circuited and the *T* winding open-circuited
Z_{PT} = leakage impedance, in ohms, between the *P* and *T* windings as measured on the *P* winding with the *T* winding short-circuited and the *S* winding open-circuited

The negative-sequence equivalent circuit, expressed in ohms, is shown in figure 7*c*. The zero-sequence equivalent circuit expressed in ohms is shown in figure 7*d*. It will be noted from figure 7*d* that the transformation ratio for zero-sequence, *N′*, is not equal to the transformation ratio for positive-sequence, *N*. This results from the fact that the *T* winding carries no zero-sequence current, and would not carry zero-sequence current even if the neutral of the *T* winding were connected to ground.



(a)

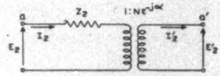
THREE PHASE EQUIVALENT CIRCUIT



$$Z_1 = \frac{1}{N^2}(Z_{VW}+3n_1^2n_2^2Z_{PS})$$

(b)

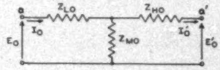
POSITIVE SEQUENCE, EXPRESSED IN OHMS



$$Z_2 = Z_1 = \frac{1}{N^2}(Z_{VW}+3n_1^2n_2^2Z_{PS})$$

(c)

NEGATIVE SEQUENCE, EXPRESSED IN OHMS



$$Z_{MO} = Z_{PT} + 3Z_G$$
$$Z_{LO} = 0$$
$$Z_{HO} = Z_{VW}$$

(d)

ZERO SEQUENCE, EXPRESSED IN OHMS

$$N = \sqrt{1+3n_1^2n_2^2}$$
$$\alpha = \tan^{-1}\sqrt{3}\,n_1n_2$$

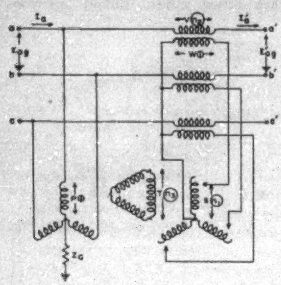
Figure 9. Regulating transformer for phase-angle control, with the sequence networks expressed in ohms

The parts ratios are:

$$\frac{U_V}{U_G} = \frac{U_P}{U_G} = \frac{\sqrt{3}n_1n_2}{N}$$

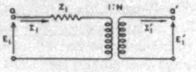
When the tap changer mechanism for voltage control, operating on the *S* winding, is in the neutral position, $n_1 = 0$, and zero-sequence current passes through the regulator without transformation. For this condition, regardless of the position of the tap changer for phase-angle control, the zero-sequence impedance of the regulator in ohms is Z_{VW}, the leakage impedance of the series transformer.

In order to conserve space, the corresponding equivalent circuits, expressed in per cent, have been omitted. There will be, of course, the two types, depending upon the choice of base voltages, just as in the previous example. As before, the transformer ratio required in the ideal transformer for the first type will be $1/N$ times the ratio in the ohmic diagram. Thus for the first type of zero-sequence diagram expressed in per cent, the ratio will become $1{:}N'/N$, where N' and N are given in figure 7, instead of the ratio $1{:}N'$ as in the ohmic diagram. In every case it will, of course, be found that the transformation ratios in the second type of percentage diagram are the same as in the corresponding ohmic diagrams, since the base voltages used as reference are the same for both circuits of the transformer.



(a)

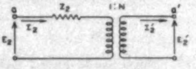
THREE PHASE EQUIVALENT CIRCUIT



$$Z_1 = \frac{1}{N^2}\left[Z_{VW} + (N-1)^2 Z_{PS}\right]$$

(b)

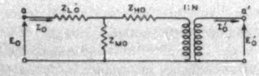
POSITIVE SEQUENCE, EXPRESSED IN OHMS



$$Z_2 = Z_1 = \frac{1}{N^2}\left[Z_{VW} + (N-1)^2 Z_{PS}\right]$$

(c)

NEGATIVE SEQUENCE, EXPRESSED IN OHMS



$$Z_{M0} = \frac{N+1}{2N} Z_{PT} - \frac{N-1}{2N}\left(Z_{PS} - \frac{1}{n_1^2} Z_{ST}\right) + \frac{3Z_G}{N}$$

$$Z_{L0} = \frac{N-1}{2N}\left(Z_{PS} + Z_{PT} - \frac{1}{n_1^2} Z_{ST}\right) + \frac{3(N-1)}{N} Z_G$$

$$Z_{H0} = \frac{Z_{VW}}{N^2} + \frac{N-1}{2N^2}\left[(N-2) Z_{PS} - N\left(Z_{PT} - \frac{Z_{ST}}{n_1^2}\right)\right] - \frac{3(N-1)}{N^2} Z_G$$

(d)

ZERO SEQUENCE, EXPRESSED IN OHMS

$$N = 1 + n_1 n_2$$

Figure 10. Regulating transformer for voltage control, with the sequence equivalent circuits expressed in ohms

The parts ratios are:

$$\frac{U_P}{U_C} = \frac{U_S}{U_C} = \frac{U_V}{U_C} = \frac{N-1}{N}$$

It is evident that the analysis of power systems involving such regulators is considerably more complex than for the simpler cases, but the use of ideal transformers again makes a solution readily possible.

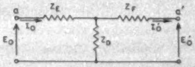
Phase-Angle Regulator Acting as Grounding Transformer

In what has preceded, the zero-sequence equivalent circuit has the same form as the positive-sequence equivalent circuit, except that there is no zero-sequence phase shift and the circuit may involve a different transformation ratio. In some cases the regulating transformer may also act as a source of ground current. When this is true, the zero-sequence equivalent circuit becomes entirely different. A typical example of this is the regulating transformer for phase-angle control, the circuit for which is shown in figure 9*a*. The positive- and negative-sequence equivalent circuits expressed in ohms are given in figures 9*b* and 9*c*. The same conventions as previously used for winding designations and leakage impedance definitions are used. Since the delta winding shown in the diagram is idle under balanced conditions, it has no effect on the positive- and negative-sequence equivalent circuits, and may be disregarded in these cases. Thus, no new problems are involved, and the derivation is omitted.

However, the delta has a decided effect upon the flow of zero-sequence current from lines to ground in the *P* winding, and the method used in deriving the zero-sequence equivalent circuit must be appreciably modified. The simplest procedure is to assume zero-sequence voltages to be applied to the input (*a*, *b*, *c*) terminals with the output (*a'*, *b'*, *c'*) terminals open-circuited. An equation is then found relating E_0 and I_0, and an equation relating E_0' and I_0'. The procedure is repeated with zero-sequence voltages applied to the output (*a'*, *b'*, *c'*) terminals with the input (*a*, *b*, *c*) terminals open-circuited, and a third equation written involving E_0' and I_0'. If, under these conditions, a fourth equation is written for E_0 as a function of I_0', the latter equation will be found convenient for checking the results of the first three equations. The form of the equations may suggest the type of equivalent circuit to be tried, such as figure 9*d*, and similar equations are written for the circuit selected, first by applying a voltage to the terminals *a*, and then to the terminals *a'*. If the circuit selected is a possible equivalent circuit, direct comparison between the equations derived for the actual transformer and for the equivalent circuit being tried will reveal values for Z_{L0}, Z_{M0}, and Z_{H0} which will satisfy the equations of the actual transformer. The required values for the case in question are given in the figure. It will be noted that there is no direct transformation of zero-sequence current and voltage; that is, I_0 and I_0', are not related by a fixed ratio. There is impedance coupling between the circuits, however caused by the *mutual* branch Z_{M0}, so that E_0 and E_0' are dependent upon the currents in both circuits, I_0 and I_0'.

Regulator for Voltage Control

The regulating transformer for voltage control shown in figure 10*a* has the equivalent circuits for positive and negative sequence shown in figures 10*b* and 10*c*. The equivalent circuit for zero sequence, expressed in ohms, may be derived following an analysis similar to that just described. Since zero-sequence transformation occurs, the simplest equivalent circuit to derive is that shown in figure 10*d*. This circuit is of the same form as that of figure 9*d*, except that it involves an ideal transformer as well as a shunt branch. This may be the most desirable form, but if the regulating trans-



$$Z_D = \frac{N+1}{2} Z_{PT} - \frac{N-1}{2}\left(Z_{PS} - \frac{1}{n_1^2} Z_{ST}\right) + 3Z_G$$

$$Z_E = \frac{N-1}{2N}\left(Z_{PS} + Z_{PT} - \frac{1}{n_1^2} Z_{ST}\right) + \frac{3(N-1)Z_G}{N} - \left(\frac{N-1}{N}\right) Z_D$$

$$Z_F = Z_{VW} + \frac{N-1}{2}\left[(N-2) Z_{PS} - N\left(Z_{PT} - \frac{Z_{ST}}{n_1^2}\right)\right] - 3(N-1)Z_G + (N-1)Z_D$$

Figure 11. Alternate form of the zero-sequence equivalent circuit for the regulating transformer of figure 10

former is connected in a loop circuit, the reduction of the zero-sequence network for the system is simplified if there is no direct transformation of zero-sequence current and voltage, and consequently no ideal transformer. For such case, the equivalent circuit may be converted to the more useful form, without the direct transformation, shown in figure 11. The derivation of this equivalent circuit from that shown in figure 10*d* is given in appendix IV. It will be found that, for zero sequence, many regulating transformers can be represented by equivalent circuits of the type shown in figures 9*d* or 10*d*. Which type is most convenient will depend upon circumstances, but the derivation given in appendix IV shows that either form may be readily converted to the other, as needed, as long as Z_{MO} is not infinite.

Equivalent Circuits in Power System Analysis

In setting up the sequence networks of systems for analysis, the common procedure is to select some one circuit voltage as a base, and transfer the impedances of the various circuit elements from their actual values to equivalent values on the base voltage selected, by multiplying each impedance by the square of the ratio of the base voltage to the nominal voltage at the point where the circuit element is located. When regulating transformers of variable ratio are involved, this process often becomes confusing and difficult to follow. The process becomes even more complicated when zero-sequence quantities are not transformed, or have a different ratio of transformation from the positive- and negative-sequence quantities, because of the uncertainty regarding what voltage ratio should be applied to the zero-sequence impedance.

The equivalent circuits here outlined permit a straightforward approach to the problem, because they are set up to include ideal transformers where necessary, so that the circuit used is the exact equivalent of the actual transformer in every important respect, and may be substituted for it without making any conversions in the impedances of the other circuit elements. Then, where simplification is possible, the ideal transformers may be shifted from one location to another, with corresponding changes in circuit impedances, until in many cases the ideal transformers can be dropped out of the final diagram altogether. The process can best be described by means of two typical examples.

Figure 12*a* is the single-line diagram for a typical simple system, including a regulating transformer for voltage control, connected between points *a* and *b*. The sequence networks for the system, connected to represent a single-line-to-ground fault at *F*, are shown in figure 12*b*. The equivalent circuits of the regulator are of the type shown in figures 10*b* for positive sequence, 10*c* for negative sequence, and 11 for zero sequence.

To illustrate the method more fully, the delta-star step-up transformer bank is represented by equivalent circuits of the type described in this paper, in which ideal transformers are included to take care of the actual transformation ratio and the 30-degree phase shift introduced by the delta-star connection. It will again be noted that if the positive-sequence phase shift is +30 degrees, represented by ϵ^{j30}, the negative-sequence shift must be −30 degrees, represented by ϵ^{-j30}, in the ratios of the ideal transformers. As is well known in the theory of symmetrical components, the zero-sequence current will flow in the star side of the transformer, circulate in the closed delta, but will not flow out of the bank on the delta side, so that no ideal transformer is required in the zero-sequence circuit if the equivalent circuits are set up and viewed from bus *A* on the star side, as in this case.

The line impedance Z_{L1} and Z_{L0} for the line between *A* and *a* are included at their actual values, without any conversion, since all circuits are still represented on their actual voltage base. Since the lines are static equipment, the negative-sequence impedance is the same as the positive, and Z_{L1} is used in the negative-sequence network. The same comments apply for the impedances of the line between *B* and *F*, Z_{M1} and Z_{M0}.

The solution of the network shown can be facilitated by first eliminating the ideal transformers. This may be done by shifting them to the left a step at a time. First the transformer of ratio 1:*N* is moved to the left past Z_1, Z_{L1}, and Z_{T1}. Of course, as this is done, the impedances must be multiplied by the square of the ratio, as shown in figure 12*c*, and the currents in the circuits affected become 1/*N* times their actual values, and the voltage *N* times as large as actual.

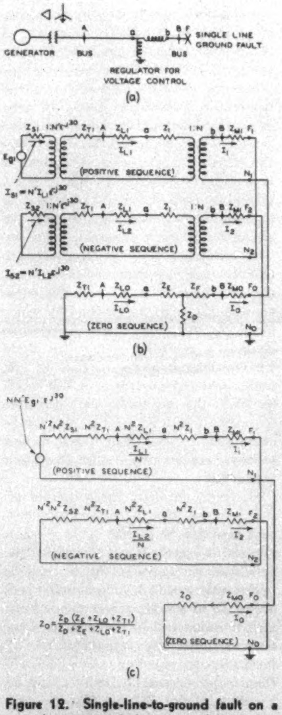


Figure 12. Single-line-to-ground fault on a typical system including a regulating transformer for voltage control

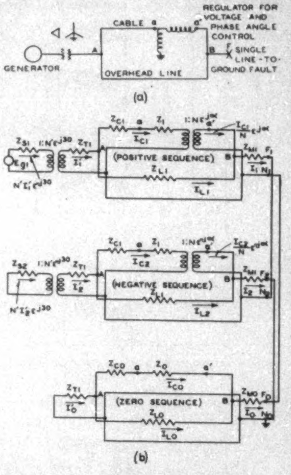


Figure 13. Single-line-to-ground fault on a typical system including a regulating transformer for phase-angle control

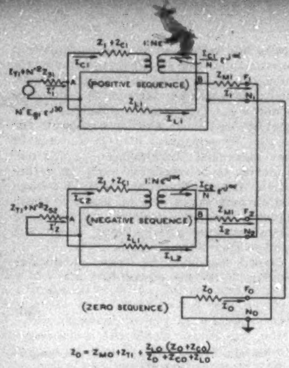


Figure 14. Simplified sequence networks for the system of figure 13

The ideal transformers of ratio $1:N$ may now be combined with the ones of ratio $1:N'\epsilon^{j30}$ and $1:N'\epsilon^{-j30}$ in the positive- and negative-sequence networks, respectively, to give new transformers of ratios $1:N'N\epsilon^{j30}$ and $1:N'N\epsilon^{-j30}$, respectively. These transformers may now also be moved to the left, past the impedances Z_{S1} and Z_{S2} of the generator. In the process the impedances are multiplied by $(N')^2N^2$, the square of the scalar ratio of transformation, but are not changed in phase angle, since the voltage and current are shifted in phase similarly, and no change results in their phase relations to each other. The voltages and currents are of course changed in magnitude, and also rotated in phase position, as indicated in the figure. When the ideal transformer reaches the generated voltage, E_{g1}, that too is altered in magnitude and phase, and the ideal transformer may now be dropped altogether, to give the final equivalent circuit shown in figure 12*c*.

In the zero-sequence network there were no ideal transformers, so the process described was not necessary. However, the two parallel circuits represented by Z_D and $Z_{T1}+Z_{L0}+Z_E$ can be combined to give Z_0 as shown.

The point to be emphasized is that, in the final network, Z_{L1} is multiplied by N^2, but in the zero-sequence network Z_{L0} is unchanged. The method outlined gives a straightforward approach to this result, but by methods in common use heretofore it is difficult to visualize the result and apply the proper conversion factors to Z_{L1} and Z_{L0}.

After the network of figure 12*b* is solved by straightforward methods, the ideal transformers can again be introduced, and moved to the right until their original positions are reached, the currents and voltages being converted to their actual values in the process. The phase currents and voltages can then be readily determined by combining the sequence quantities in accordance with the usual formulas.

Figure 13 represents a similar case except that two parallel circuits are involved, and, because one is underground cable and the other overhead line, a phase-angle regulator has been introduced to control the division of power. The sequence networks for the fault condition are set up in figure 13*b* in the same way as before, using equivalent circuits involving ideal transformers. However, when simplification of the network is attempted, it is found impossible to shift the ideal transformers of ratios $1:N\epsilon^{j\alpha}$ and $1:N\epsilon^{-j\alpha}$ out of the closed loop. The other simplifications are carried out as before, giving the network of figure 14. Although this network is not as simple as in the preceding case, it may be solved analytically, making use of the properties of ideal transformers in writing the equations, or on a network calculator. In the latter case, with present equipment, a power source must be used to represent each winding of an ideal transformer involving phase shift, and the voltage and phase position of each must be adjusted by trial and error until the currents and voltages at the points representing the terminals of the ideal transformer bear the required relations to each other.

Summary

It has been shown how complicated regulating transformers can be represented by simple equivalent circuits, which fall into certain fairly well-defined types. By introducing the concept of ideal transformers to supplement the commonly used equivalent circuits for transformers, many problems can be more readily visualized and solved with much less chance for error. The method outlined can be used to derive suitable equivalent circuits for all the types of regulating transformers in common use, but obviously space does not permit including them here. However, the authors expect to publish these equivalent circuits in the near future.

Appendix I. Derivation of Positive-Sequence Equations for the Regulating Transformer Shown in Figure 3

Balanced positive-sequence currents are assumed to be circulated through the regulator. Since the windings are all symmetrical there will be no interaction between sequences, and the terminal voltages will consist of only a positive-sequence component. In the following development the vector operators a and a^2 will be used frequently. $a=\epsilon^{j120}=-{}^1/_2+j\sqrt{3}/2$, and $a^2=\epsilon^{j240}=-{}^1/_2-j\sqrt{3}/2$.

Thus at the left-hand terminals of figure 3:

$$\left.\begin{aligned} E_{ag} &= E_1 & I_a &= I_1\\ E_{bg} &= a^2E_1 & I_b &= a^2I_1\\ E_{cg} &= aE_1 & I_c &= aI_1 \end{aligned}\right\}\quad(1)$$

and at the right-hand terminals:

$$\left.\begin{aligned} E_{ag}' &= E_1' & I_a' &= I_1'\\ E_{bg}' &= a^2E_1' & I_b' &= a^2I_1'\\ E_{cg}' &= aE_1' & I_c' &= aI_1' \end{aligned}\right\}\quad(2)$$

Let e_p denote the voltage across the ideal transformer winding P, in the phase carrying I_A. Similarly let e_s denote the voltage of winding S in the corresponding phase, which causes n_2I_a'. Likewise, let e_v and e_w denote the voltages of the V and W windings, respectively, in the phase associated with I_a'. By applying Kirchhoff's voltage law to the diagram of figure 3,

$$\begin{aligned} E_{ag} &= E_1 = E_{ag}'+e_v+Z_{VW}I_a'\\ &= E_1'+e_v+Z_{VW}I_1' \end{aligned}\quad(3)$$

$$e_w = e_s \quad(4)$$

$$\begin{aligned} e_p &= E_{bg}-E_{cg}+I_AZ_{PS}\\ &= (a^2-a)E_1+I_AZ_{PS}\\ &= -j\sqrt{3}E_1+I_AZ_{PS} \end{aligned}\quad(5)$$

the last form of equations 3 and 5 resulting by substituting from equations 1 and 2, and making use of the properties of a.

From the required relations of the ideal transformers

$$e_s = n_1e_p \quad(6)$$

$$e_v = n_2e_w \quad(7)$$

$$I_A = n_1n_2I_a' = n_1n_2I_1' \quad(8)$$

Eliminating e_v, e_w, e_s, e_p, and I_A, by substituting first (8) in (5), (5) in (6), (6) in (4), then (4) in (7), and finally (7) in (3), gives

$$E_1(1+j\sqrt{3}\,n_1n_2) = E_1' + (n_1^2n_2^2Z_{PS}+Z_{VW})I_1' \quad(9)$$

At no load, $I_1'=0$, giving

$$E_1' = (1+j\sqrt{3}n_1n_2)E_1 \quad(10)$$

or, changing to polar form,

$$E_1' = \sqrt{1+3n_1^2n_2^2}\epsilon^{j\alpha}E_1 = N\epsilon^{j\alpha}E_1 \quad(11)$$

where

$$\alpha = \tan^{-1}\sqrt{3}n_1n_2$$

$$N = \sqrt{1+3n_1^2n_2^2}$$

Again, from the diagram:

$$I_A-I_B = I_C-I_C' = a(I_1-I_1') \quad(12)$$

Since I_A, I_B, and I_C are positive-sequence currents, $I_B=a^2I_A$ and

$$(1-a^2)I_A = a(I_1-I_1') = (1-a^2)n_1n_2I_1', \text{ from equation 8} \quad(13)$$

From (13)

$$I_1 = \left(1 + \frac{1 - a^2}{a} n_1 n_2\right) I_1'$$

$$= (1 - j\sqrt{3}n_1 n_2) I_1' = N\epsilon^{-j\alpha} I_1' \quad (14)$$

or

$$I_1' = \frac{1}{N}\epsilon^{j\alpha} I_1 \quad (15)$$

Since $1 + j\sqrt{3}n_1 n_2 = N\epsilon^{j\alpha}$, equation 9 becomes

$$N\epsilon^{j\alpha}E_1 = E_1' + (n_1^2 n_2^2 Z_{PS} + Z_{VW}) I_1' \quad (16)$$

Substituting from (15) and rearranging

$$E_1' = N\epsilon^{j\alpha}E_1 - \frac{1}{N}(n_1^2 n_2^2 Z_{PS} + Z_{VW})\epsilon^{j\alpha} I_1$$

$$= N\epsilon^{j\alpha}\left[E_1 - \frac{1}{N^2}(n_1^2 n_2^2 Z_{PS} + Z_{VW}) I_1\right]$$

$$= N\epsilon^{j\alpha}[E_1 - Z_1 I_1] \quad (17)$$

where

$$Z_1 = \frac{1}{N^2}(n_1^2 n_2^2 Z_{PS} + Z_{VW}) \quad (18)$$

= impedance of the regulating transformer to positive-sequence, as viewed from the input (*a*, *b*, *c*) terminals

Appendix II. Derivation of the Positive-Sequence Equivalent Circuit in Percentage Quantities

Selecting the base quantities, let

U_N = base three-phase kilovolt-amperes for the system considered

E_N = base voltage of the input circuit line-to-neutral, expressed in volts

$E_N' = kE_N$ = base voltage of the output circuit, line-to-neutral, expressed in volts

The value to be assigned to k, that is, the ratio of the two base voltages, will be discussed later.

With voltage and kilovolt-amperes defined, the corresponding base currents become

$$I_N = kI_N' = \frac{1{,}000\,U_N}{3E_N} \quad (19)$$

The impedance which will produce I_N when the input circuit base voltage is applied is the base impedance for that circuit, and becomes

$$Z_N = \frac{E_N}{I_N} = \frac{3E_N^2}{1{,}000\,U_N} = \frac{E_{(L-L)}^2}{1{,}000\,U_N} \quad (20)$$

where $E_{(L-L)}$ is the line-to-line voltage corresponding to the line-to-neutral voltage E_N. Similarly,

$$Z_N' = \frac{E_N'}{I_N'} = \frac{3(E_N')^2}{1{,}000\,U_N}$$

$$= \frac{(E'_{(L-L)})^2}{1{,}000\,U_N} = k^2 Z_N \quad (21)$$

Equation 17 is expressed in percentage quantities by multiplying by $100/E_N'$, or its equivalents, $100/kE_N$ and $100/kI_N Z_N$, giving

$$\frac{100E_1'}{E_N'} = \frac{N}{k}\epsilon^{j\alpha}\left[\frac{100E_1}{E_N} - \frac{100Z_1}{Z_N}\frac{I_1}{I_N}\right]$$

or

$$E_1'\% = \frac{N}{k}\epsilon^{j\alpha}(E_1\% - Z_1\% I_1 \text{ p.u.}) \quad (22)$$

where $E_1'\%$, $E_1\%$, and $Z_1\%$ denote E_1', E_1, and Z_1 expressed in percentage quantities, and I_1p.u. denotes I_1 expressed in per unit or as a fraction of its normal value. This combined notation, expressing voltage and impedance in per cent, and current in per unit, eliminates any factors of 100 in the final equation.

Similarly, equation 15 can be expressed in per unit by multiplying by $1/I_N'$, or its equivalents k/I_N, giving

$$\frac{I_1'}{I_N'} = \frac{k}{N}\epsilon^{j\alpha}\frac{I_1}{I_N}$$

or

$$I_1'\text{p.u.} = \frac{k}{N}\epsilon^{j\alpha} I_1 \text{ p.u.} \quad (23)$$

Although k is actually arbitrary, there are two values which are of special interest in connection with regulating transformers, namely, $k = N$ and $k = 1$. For $k = N$, equations 22 and 23 become

$$E_1'\% = \epsilon^{j\alpha}(E_1\% - Z_1\% I_1 \text{ p.u.}) \quad (24)$$

$$I_1' \text{ p.u.} = \epsilon^{j\alpha} I_1 \text{ p.u.} \quad (25)$$

The equivalent circuit which satisfies these equations is given by figure 4*b*. For $N = 1$, equations 22 and 23 become

$$E_1'\% = N\epsilon^{j\alpha}(E_1\% - Z_1\% I_1 \text{ p.u.}) \quad (26)$$

$$I_1'\text{p.u.} = \frac{1}{N}\epsilon^{j\alpha} I_1 \text{ p.u.} \quad (27)$$

The equivalent circuit which satisfies these equations is given by figure 4*c*.

The value of $Z_1\%$ is obtained by multiplying equation 18 by $100/Z_N$, as suggested n equation 22, giving

$$Z_1\% = \frac{1}{N^2}\left(n_1^2 n_2^2\frac{100Z_{PS}}{Z_N} + \frac{100Z_{VW}}{Z_N}\right) \quad (28)$$

It is customary to express the impedance of individual transformers in per cent, so that it would seem logical to express Z_{PS} and Z_{VW} directly in per cent of Z_N. However, the usual practice is to express the impedance in per cent on the kilovolt-amperes of the individual transformer as a base. In a regulating transformer such as this it is obvious that the kilovolt-amperes carried by either the series transformer or the exciting transformer is quite different from the kilovolt-amperes carried by the circuit. The current in the series transformer is the outgoing line current, but the voltage across the transformer winding is only that fraction of the line-to-neutral voltage supplied by the exciting transformer to produce the phase shift desired. Similarly, the exciting transformer has normal line-to-line voltage impressed, but carries a current equal only to $1/\sqrt{3}$ times the vector difference of the input and output currents. Hence the base impedance for Z_{PS} and Z_{VW} is not Z_N. Two entirely different values must be determined which correspond to the voltage, current, and kilovolt-amperes of the individual transformers concerned. However, to be consistent with the values for the entire circuit, the normal voltage and current values selected must be those applying when the voltage, current, and kilovolt-amperes of the input circuit are equal to their normal values.

Considering first the series transformer winding, V, its base current is

$$I_{NV} = \frac{1}{N} I_N \quad (29)$$

and its base voltage (no load) is, in magnitude,

$$E_{NV} = \sqrt{3}n_1 n_2 E_N \quad (30)$$

from appendix I by determining e_v when $E_1 = E_N$. The corresponding base impedance and base kilovolt-amperes become

$$Z_{NV} = \frac{E_{NV}}{I_{NV}}$$

$$= \frac{\sqrt{3}n_1 n_2 N E_N}{I_N} = \sqrt{3}n_1 n_2 N Z_N$$

or

$$\frac{1}{Z_N} = \frac{\sqrt{3}n_1 n_2 N}{Z_{NV}} \quad (31)$$

and the corresponding three-phase kilovolt-amperes becomes

$$U_V = \frac{3E_{NV}I_{NV}}{1{,}000} = \frac{3\sqrt{3}n_1 n_2 E_N I_N}{1{,}000N}$$

$$= \frac{\sqrt{3}n_1 n_2}{N} U_N \quad (32)$$

Similarly, for the exciting transformer the base current in the P winding is, in magnitude

$$I_{NP} = \frac{n_1 n_2}{N} I_N$$

by considering equation 8 when $I_1 = I_N$. The voltage across the P winding is $\sqrt{3}E_N$, which is equal to the line-to-line voltage. Hence the base impedance becomes

$$Z_{NP} = \frac{E_{NP}}{I_{NP}} = \frac{\sqrt{3}NE_N}{n_1 n_2 I_N} = \frac{\sqrt{3}N}{n_1 n_2} Z_N$$

or

$$\frac{1}{Z_N} = \frac{\sqrt{3}N}{n_1 n_2}\frac{1}{Z_{NP}} \quad (33)$$

and the three-phase base kilovolt-amperes becomes

$$U_P = \frac{3E_{NP}I_{NP}}{1{,}000} = \frac{3\sqrt{3}n_1 n_2 E_N I_N}{1{,}000N}$$

$$= \frac{\sqrt{3}n_1 n_2}{N} U_N \quad (34)$$

Comparing equations 32 and 34 shows that both the exciting transformer and the series transformer carry the same kilovolt-amperes under any given load condition.

The values of $1/Z_N$ may now be substituted in equation 28, giving

$$Z_1\% = \frac{1}{N^2}\left[\frac{n_1^2n_2^2\sqrt{3}N}{n_1n_2}\,\frac{100Z_{PS}}{Z_{NP}} + \sqrt{3}n_1n_2N\,\frac{100Z_{VW}}{Z_{NV}}\right]$$

$$= \frac{\sqrt{3}n_1n_2}{N}\,(Z_{PS}\% + Z_{VW}\%)$$

$$= \frac{\sqrt{N^2-1}}{N}(Z_{PS}\% + Z_{VW}\%) \qquad (35)$$

where $Z_{PS}\%$ and $Z_{VW}\%$ are now the leakage impedances of the exciting and series transformers expressed in per cent on the kilovolt-amperes carried by those transformers when the circuit carries the base kilovolt-amperes, U_N (in all three phases). The ratio of the base kilovolt-amperes of the transformers to the base kilovolt-amperes of the circuit is obtained by rewriting equations 32 and 34 as

$$\frac{U_P}{U_N} = \frac{U_V}{U_N} = \frac{\sqrt{3}n_1n_2}{N} = \frac{\sqrt{N^2-1}}{N} \qquad (36)$$

from appendix I.

Appendix III. Derivation of Zero-Sequence Equivalent Circuit for the Regulating Transformer Shown in Figure 3

Balanced zero-sequence currents are assumed to be circulated through the regulator. Because of symmetry the terminal voltages will likewise be balanced zero-sequence voltages.

Referring to figure 3,

$$\begin{aligned} I_a &= I_b = I_c = I_0\\ E_{ag} &= E_{bg} = E_{cg} = E_0\\ I_a' &= I_b' = I_c' = I_0'\\ E_{ag}' &= E_{bg}' = E_{cg}' = E_0' \end{aligned} \qquad (37)$$

Since zero-sequence current cannot flow into the corners of a closed delta winding without taps,

$$I_a - I_a' = I_b - I_b' = I_c - I_c' = 0 \qquad (38)$$

It immediately follows that

$$I_0 = I_0' \qquad (39)$$

that is, zero-sequence current is not transformed by the regulating transformer.

From the diagram, by Kirchhoff's voltage law,

$$E_0 = E_0' + Z_{VW}I_0 + e_v \qquad (40)$$

and

$$e_w = e_s \qquad (41)$$

$$e_p = E_{bg} - E_{cg} + I_AZ_{PS} = 0 + I_AZ_{PS} \qquad (42)$$

From the required relations of the ideal transformers

$$e_v = n_2e_w \qquad (43)$$

$$e_s = n_1e_p \qquad (44)$$

$$I_A = n_1n_2I_0' = n_1n_2I_0 \qquad (45)$$

Combining to eliminate e_v, e_w, e_p, e_s, and I_A gives

$$E_0 = E_0' + (n_1^2n_2^2Z_{PS} + Z_{VW})I_0$$

or

$$\begin{aligned} E_0' &= E_0 - (n_1^2n_2^2Z_{PS} + Z_{VW})I_0\\ &= E_0 - Z_0I_0 \end{aligned} \qquad (46)$$

where

$$Z_0 = n_1^2n_2^2Z_{PS} + Z_{VW} \qquad (47)$$

To express the above relations in per cent, the same process is used as in appendix II. Multiplying equation 39 by $1/I_N'$, or its equivalent, k/I_N, gives

$$\frac{I_0'}{I_N'} = \frac{kI_0}{I_N}$$

or

$$I_0' \text{ p.u.} = kI_0 \text{ p.u.} \qquad (48)$$

Similarly, multiplying equation 46 by $100/E_N'$, or its equivalent, gives

$$\frac{100E_0'}{E_N'} = \frac{1}{k}\left[\frac{100E_0}{E_N} - \frac{100Z_0}{Z_N}\,\frac{I_0}{I_N}\right]$$

or

$$E_0'\% = \frac{1}{k}\,(E_0\% - Z_0\%I_0 \text{ p.u.}) \qquad (49)$$

For the first case, when the base voltages are proportional to N, so that $k = N$, equations 48 and 49 become

$$I_0' \text{ p.u.} = NI_0 \text{p.u.} \qquad (50)$$

and

$$E_0'\% = \frac{1}{N}\,(E_0\% - Z_0\%I_0 \text{ p.u.}) \qquad (51)$$

For the second case, $k = 1$, and

$$I_0' \text{ p.u.} = I_0 \text{ p.u.} \qquad (52)$$

$$E_0'\% = E_0\% - Z_0\%I_0 \text{ p.u.} \qquad (53)$$

The value of Z_0, expressed in per cent, is found in the same way as for Z_1 in appendix II. In fact the only difference occurs in the coefficient, and the result is

$$Z_0\% = N\sqrt{N^2-1}(Z_{PS}\% + Z_{VW}\%) \qquad (54)$$

Appendix IV. Derivation of Equivalent Circuit With Shunt Branch and No Direct Transformation From Circuit With Shunt Branch and With Direct Transformation

Refer to figure 10*d* and figure 11, which are to be made equivalent. The two circuits will be equivalent if terminal measurements for the two circuits are identical. Consider first that the terminal a' in both circuits is open-circuited, and the voltage E_0 applied to the terminal a:

From figure 10*d*:

$$E_0 = I_0(Z_{L0} + Z_{M0}) \qquad (55)$$

$$E_0' = NI_0Z_{M0} \qquad (56)$$

From figure 11:

$$E_0 = I_0(Z_E + Z_D) \qquad (57)$$

$$E_0' = I_0Z_D \qquad (58)$$

Now consider that the terminal a in both circuits is open-circuited, and the voltage E_0' applied to the terminal a':

From figure 10*d*:

$$E_0' = -I_0'N^2(Z_{H0} + Z_{M0}) \qquad (59)$$

$$E_0 = -NI_0'Z_{M0} \qquad (60)$$

From figure 11:

$$E_0' = -I_0'(Z_F + Z_D) \qquad (61)$$

$$E_0 = -I_0'Z_D \qquad (62)$$

If the two circuits are to be equivalent, by equating (56) and (58), or (60) and (62):

$$Z_D = NZ_{M0} \qquad (63)$$

Equating (55) and (57):

$$Z_{L0} + Z_{M0} = Z_E + Z_D$$

$$\therefore Z_E = Z_{L0} + Z_{M0} - Z_D = Z_{L0} - \frac{(N-1)}{N}Z_D \qquad (64)$$

Equating (59) and (61):

$$N^2(Z_{H0} + Z_{M0}) = Z_F + Z_D$$

$$\begin{aligned} \therefore Z_F &= N^2(Z_{H0} + Z_{M0}) - Z_D\\ &= N^2Z_{H0} + (N-1)Z_D \end{aligned} \qquad (65)$$

Equations 63, 64, and 65 provide relationships existing between the branches of the circuit shown in figure 11 and the branches of the circuit shown in figure 10*d*, so that the two circuits are equivalent.

Reference

1. Symmetrical Components (a book), C. F. Wagner and R. D. Evans, McGraw-Hill Book Company, 1933.

Discussion

A. N. Garin (General Electric Company, Pittsfield, Mass.): The material presented in this very interesting and timely paper falls naturally into two distinct parts.

Seen from outside, the regulating transformer, consisting of two separate transformers properly interconnected and placed in a common tank, has the physical appearance of a single transformer. To be able to treat this combined unit as a single transformer in the analysis of the system it is necessary first to determine its terminal characteristics. Thus, the first problem is that of determining the characteristics of the combined unit from the characteristics of the two separate transformers forming the unit.

The second problem is that of representing the combined unit, which may now be treated as a single transformer, in the equivalent circuits of the system. I shall limit my discussion of this part of the paper to the observation that the difference in positive- and zero-sequence transformation ratios, the flow of zero-sequence currents through transformer windings without transformation, the various phase shifts, etc., are not exclusive prerogatives of regulating units consisting of two interconnected transformers, but often will be found in individual power transformers. The methods of analysis and representation described by the authors have, therefore, a broader significance and a wider field of application than they have indicated.

The procedure used for deriving the per cent positive-sequence impedance of the circuit shown on figure 1 is straightforward, explicit, and does not presuppose any familiarity with the modern methods of transformer circuit analysis on the part of the reader. Since, however, several pages and 35 numbered equations are required to arrive at the value of the per-cent positive-sequence impedance of the very simple circuit of figure 1, it is obvious that the labor involved in deriving by this procedure the impedances of more complicated circuits, such for instance as the circuit of figure 7, must be prodigious.

The number and variety of regulating-transformer circuits, that are either in actual use or have been proposed, are quite impressive so that it is fortunate that no lengthy and complicated calculations are really required to determine their effective impedances. Thus the positive-sequence impedance of the regulating transformer of figure 1 may be obtained in the following manner. Assume that this transformer is designed to give a maximum quadrature voltage of ($r \times 100$) per cent and that it is desired to find the value of the positive-sequence impedance it will introduce in the system when operated on a tap connection giving a quadrature voltage of ($q \times 100$) per cent, where q may have any value from 0 to r. In terms of the system kilovolt-amperes, the kilovolt-amperes in the series transformer is given by $\frac{q}{\sqrt{1+q^2}}$, as indicated by the voltage vector diagram, and by the law of conservation of kilovolt-amperes the same figure must apply for the exciting transformer. The impedance introduced into the system, expressed in per cent at system kilovolt-amperes, is given by the sum of the impedances of the exciting and the series transformers, expressed in per cent at system kilovolt-amperes, multiplied by the square of the ratio of the kilovolt-amperes in each transformer to the system kilovolt-amperes.

If the per cent impedance of the exciting transformer, connected on the tap giving ($q \times 100$) per cent quadrature voltage, is denoted by $\%Z_{P-S}$ and the per cent impedance of the series transformer at the excitation corresponding to ($r \times 100$) per cent quadrature voltage is denoted by $\%Z_{V-W}$, the positive-sequence impedance of the combined unit may be written as:

$$\%Z_1 = \frac{q^2}{1+q^2}\left[(\%Z_{P-S}) + \frac{r^2}{q^2}\ (\%Z_{V-W})\right]$$

$$= \frac{1}{1+q^2}[q^2(\%Z_{P-S}) + r^2(\%Z_{V-W})]$$

This equation corresponds to equation 35 of the paper.

As another example, the positive-sequence impedance of the circuit of figure 7 may be derived as follows: let the regulating transformer be connected on taps giving ($p \times 100$) per cent in-phase and ($q \times 100$) per cent quadrature voltage and, as in the previous example, denote the maximum total voltage for which the series transformer is designed by ($r \times 100$) per cent. The kilovolt-amperes in various parts of the circuit in terms of the system kilovolt-amperes are readily seen to be:

Series transformer (by the voltage vector diagram)

$$\frac{\sqrt{p^2+q^2}}{\sqrt{(1 \pm p)^2 + q^2}}$$

Winding P of the exciting transformer (by the kilovolt-ampere conservation law)

$$\frac{\sqrt{p^2+q^2}}{\sqrt{(1 \pm p)^2 + q^2}}$$

Winding S of the exciting transformer (since it supplies only the in-phase component of the series transformer voltage)

$$\frac{p}{\sqrt{(1 \pm p)^2 + q^2}}$$

Winding T of the exciting transformer (since it supplies only the quadrature component of the series transformer voltage)

$$\frac{q}{\sqrt{(1 \pm p)^2 + q^2}}$$

Since the currents in windings S and T must be in time quadrature the impedance between these windings will not appear in the formula and the positive-sequence impedance of the combined unit may be written as

$$\%Z_1 = \frac{1}{(1 \pm p)^2 + q^2}[p^2(\%Z_{P-S}) + q^2(\%Z_{P-T}) + r^2(\%Z_{V-W})]$$

where all impedances are at system kilovolt-amperes, the upper sign before p is to be taken for boost and the lower for buck connections, $\%Z_{P-S}$ and $\%Z_{P-T}$ are for taps giving ($p \times 100$) per cent and ($q \times 100$) per cent voltage components, and $\%Z_{V-W}$ is taken at maximum excitation corresponding to ($r \times 100$) per cent series transformer voltage. Translated into ohms the above formula is identically the same as the formula appearing in figure 7b of the paper.

Formulas for effective values of zero-sequence impedances in terms of leakage impedances of the two transformers may be obtained by the same process, which need not be further enlarged upon since it has been presented to the Institute in 1936 in a paper entitled "Transformer Circuit Impedance Calculations" by K. K. Paluev and the writer.

In conclusion I should like to ask the authors a question that may have little bearing on the main subject of their paper, but is of considerable practical importance: do they consider the circuit of figure 7 to be a safe circuit from the viewpoint of the third harmonic?

Edward W. Kimbark (Polytechnic Institute of Brooklyn, Brooklyn, N. Y.): The authors have given a very clear derivation of the equivalent circuits of regulating transformers, and their results will prove valuable to any one who carries out, either by paper calculations or by use of a network analyzer, studies of power systems having such transformers. But there still remains the unsolved problem of how conveniently to represent on the network analyzer a regulating transformer for phase-angle control. The series impedance of the authors' equivalent circuit is readily represented, and the ideal transformer of complex ratio may be represented, as they mention, by two adjustable electromotive forces. As an alternative, one of the electromotive forces (the one which absorbs power) may be replaced by an impedance element. The disadvantage of any such scheme is the large number of adjustments to be made—adjustments which affect one another. Each electromotive force must be adjusted both in phase and in magnitude (or the impedance element which replaces one of them adjusted in both resistance and reactance), giving altogether four degrees of freedom. If studies are to be made of single-phase faults or other unbalanced conditions, then the equipment and adjustments of the positive-sequence network must be duplicated in the negative-sequence network, giving eight degrees of freedom. The adjustment of the regulating transformer itself, if it controls both ratio and phase angle, has only two degrees of freedom; or, if it controls phase angle alone, has but one degree of freedom; thus its network-analyzer representation has from two to seven superfluous degrees of freedom, which make the adjustments very laborious. The adjustments not only must be so made that they introduce the required phase shift and ratio of transformation into the two sequence networks, but they must also be made to satisfy two conditions which are satisfied automatically in the actual transformers, namely to give active and reactive powers on one side of the transformers equal to those on the other side.

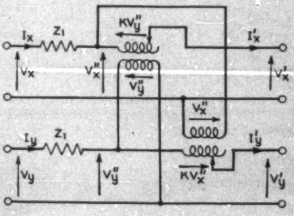


Figure 1

I wish to suggest a method of representing a regulating transformer which obviates these difficulties of adjustment. In the actual regulating transformer the phase shift is produced by taking voltage from one phase and introducing it in series with another phase. A three-phase representation could be used on the network analyzer. However, it is simpler to use a two-phase representation. A paper entitled "Two-Phase Co-ordinates of a Three-Phase Circuit," which I presented at the same meeting as the authors' paper (AIEE TRANS-

ACTIONS, volume 58, 1939, pages 894–904), describes a set of substituted variables analogous to symmetrical components but different from them. Briefly, the positive- and negative-sequence networks of the method of symmetrical components are replaced by two other substitute networks, called the x and y networks, which jointly constitute a two-phase network equivalent to the original three-phase network after the zero-sequence currents and voltages have been abstracted from it. The equivalent circuit of the regulating transformer shown in figure 1 of the paper is given in terms of two-phase co-ordinates by figure 1 of this discussion. An adjustable voltage, proportional to the shunt voltage of each network, is introduced in series with the other network; and since under balanced conditions the x and y voltages are in quadrature, this cross connection effects an equal phase shift in each network as shown by the vectors of figure 2 of the discussion. With balanced negative-sequence conditions, the phase shift produced by the equivalent circuit is equal and opposite to that for positive-sequence conditions. Since unbalanced conditions are equivalent to the superposition of positive and negative sequence, the circuit also gives correct phase shifts under unbalanced conditions. Furthermore, the small inherent change in ratio produced by this type of regulating transformer when a phase-angle adjustment is made, is reproduced automatically by this equivalent circuit. For representing a regulating transformer with both phase-angle and ratio control, such as that shown in figure 7 of the paper, for example, autotransformers may be added to the output terminals of the x and y networks. Both these autotransformers and the adjustable series transformers may be gang-operated, so that the number of adjustments to be made on the network analyzer is the same as that on the actual transformer plus adjustment of the series impedances Z_1.

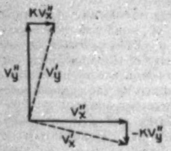


Figure 2

The disadvantage of the network-analyzer representation just presented, for a study of balanced conditions, is that two networks (the x and y networks) must be set up instead of one (the positive-sequence network), requiring additional apparatus and duplication of generator adjustments. For unbalanced conditions the same number of networks are required in either the symmetrical-component or the two-phase method, but in the latter method both networks contain generators. It is believed that in many cases the disadvantages stated will be more than offset by the ease of adjustment of the circuit representing the regulating transformer.

For paper calculations the symmetrical-component representation presented by the authors is definitely preferable, because the sequence networks of the transformer are independent.

Edith Clarke (General Electric Company, Schenectady, N. Y.): In volume 49, AIEE TRANSACTIONS, April 1930, page 819, I. H. Summers gave an equivalent circuit for a regulating transformer for voltage and phase-angle control. This circuit is not published under its own title, but is given as part of a discussion of the paper, "Inversion Currents and Voltages in Autotransformers," by A. Boyajian. Since one would not expect to find the equivalent circuit for a regulating transformer under this heading, it is not surprising that this valuable contribution has not received the attention it merits.

Mr. Summers' equivalent circuit is a static delta in which the sum of the three branch impedances is zero. With this circuit, a boost or buck in voltage and a forward or back shift in phase can be secured. Circuit base voltages may be the same or different, there is no restriction. When there is a shift in phase in the regulating transformer, the resistance of one of the branches of the equivalent delta must be negative. This makes the circuit inconvenient for use on an a-c network analyzer, but offers no difficulty in an analytic solution. It is an ideal circuit for analytic work with symmetrical components, since it can exactly replace the regulating transformer in each of the three sequence diagrams when exciting currents are neglected.

It may be well to point out that the symbol indicating division has been omitted between "Rated transformer kva" and "System base kva" in the equation (not numbered) for x.

It is customary to give transformer impedances in per cent based on their ratings; and for system studies, to express the impedances of the various circuits in per cent or per unit based on a chosen system kilovolt-ampere base and the base voltages of the individual circuits, determined at no load. The introduction of ohms and the use of base impedances is unnecessary, if the following principles are applied: (1) With exciting current neglected, the impedance of a two-winding transformer in per cent is the same referred to either side of the transformer when based on the kilovolt-ampere rating of the transformer and rated no-load voltages. (2) An impedance, in per cent on given kilovolt-ampere and voltage bases, can be expressed on new kilovolt-ampere and voltage bases if it is multiplied by the ratio of new to given base kilovolt-amperes and by the square of the ratio of given to new base voltage.

By inspection, the impedance to the current in the output circuit of figures 1 and 3, using the notation of the authors, is $(Z_{PS}\% + Z_{VW}\%)$ based on the voltage of the V winding and the kilovolt-ampere rating of the exciting and series transformers (assumed equal). Base current in winding V is base current of the output circuit; but base voltage of winding V is $\sqrt{3}n_1n_2E_n$, whereas base voltage of the output circuit is NE_n. The ratio of the kilovolt-amperes of the output circuit to the kilovolt-amperes of winding V is therefore the ratio of their base voltages. Expressed on the kilovolt-amperes and voltage of the output circuit, making use of principle (2) above, the impedance of the regulating transformer in per cent is

$$(Z_{PS}\% + Z_{VW}\%)\left(\frac{NE_n}{\sqrt{3}n_1n_2E_n}\right) \times \left(\frac{\sqrt{3}n_1n_2E_n}{NE_n}\right)^2 = (Z_{PS}\% + Z_{VW}\%) \times \frac{\sqrt{3}n_1n_2}{N}$$

This impedance has been determined for the output circuit, but it can be put in the input circuit as Z_1 per cent without change. When base voltages are proportional to N as in figure 4b, the same per-unit current flows in both circuits, and therefore it is immaterial on which side of the ideal transformer the per-cent impedance is placed.

The impedances of all regulating transformers cannot, of course, be determined so readily by inspection as that of figures 1 and 3. Figure 7, for example, with its three-winding transformers is more complex, but by using a per-unit equivalent circuit for the three-winding transformer, impedances for figure 7 and any of the other cases considered, can be determined directly in per cent.

It appears, therefore, that the authors could have reduced the length of their paper, and at the same time increased its usefulness as a reference, if they had determined the impedances of the various types of regulating transformers they have considered directly in per cent, instead of first in ohms as in appendix I, and then in per cent as in appendix II.

When the positive- and negative-sequence impedances in the other circuits of the system under consideration are equal, solution on the a-c network can be obtained by using α, β, and 0 components, described in "Problems Solved by Modified Symmetrical Components," *General Electric Review*, November and December 1938, or by using x, y, z components described in "Two-Phase Co-ordinates of a Three-Phase Circuit" by E. W. Kimbark, AIEE TRANSACTIONS, volume 58, 1939, pages 894–904.

Since the α and β voltages in a balanced system are at right angles to each other, in-phase and quadrature components of voltage corresponding to those of the regulating transformer can be automatically introduced into both the α and β circuits by the use of ideal transformers, and no readjustment is required during fault conditions. The connections of the α, β, and 0 (or the x, y, z) networks for various types of faults are given in the references cited above.

W. A. Lewis: Mr. Garin points out that many of the properties of regulating transformers described or derived in the paper are not exclusive properties of regulating transformers or transformer combinations, but are frequently found in individual power transformers. The authors have recognized this, of course, but the characteristics of individual transformers and their equivalent circuits have been covered by several authors in the past, and the effort in this paper was to discuss that phase of the general problem of transformer representation which had not been adequately covered heretofore. The methods of representation and analysis presented are believed to be adequate for attacking problems of all types, and assume little previous knowledge except Kirchhoff's laws and the elementary principles of two- and three-winding transformers.

Both Mr. Garin and Miss Clarke have criticized the length of the derivations. In so doing they have both implied that the desired objective was to derive the equivalent impedance of the regulating transformer in per cent, and that the steps given were believed by the authors necessary for that purpose. As a matter of fact the authors believe, for reasons stated in the paper, that less difficulty is encountered, and less confusion results, in complex cases, if the system study is carried out in ohms and volts rather than in per cent or per unit. The equivalent circuits have therefore first been derived in ohms and then converted to per cent for the benefit of that group who prefer to carry out numerical work in that form. Obviously, if the sole objective had been to derive the final results in per cent or per unit, it would have been much shorter to start with all quantities in that form and carry out the work directly in those units. For those who wish to carry all work in per cent, the principles enunciated by Miss Clarke will prove useful.

Again, the length of any derivation depends upon the starting point and the assumed knowledge. Both Mr. Garin and Miss Clarke give shorter derivations for some of the results of the paper, but in so doing, they make use of derived principles which are assumed to be familiar to the reader. This paper was written primarily for engineers engaged in occasional system studies, and the experience of the authors has indicated that the principles so easily assumed by the discussers are not widely known. For example, Mr. Garin assumes, in discussing figure 7, that, since the currents in windings *S* and *T* are in time quadrature, the impedance between them will not appear in the formula. He further assumes that the remaining impedances, when converted to the corresponding kilovolt-amperes, may be simply added. It is not at all apparent to the authors, and, we believe, to many of our readers, how these principles may be assumed without proof. In considering figures 9 and 10, for example, it is not at all apparent, without adequate derivation, that a single impedance is used for the positive-sequence impedance of figure 9*c* and that a T circuit must be used for the zero sequence of figure 9*d*. If the proofs for the principles so glibly stated to suit the case are supplied, and the results are worked out both for those who wish to work in ohms and those who wish to work in per cent, the authors believe that the derivations will not be substantially shorter than those given. Following the methods given, an engineer, not an expert in this particular field, confronted with a particular case, can proceed directly from fundamental principles to obtain the required equivalent circuit.

Mr. Garin asks if the authors consider the circuit of figure 7 safe from the standpoint of the third harmonic. The examples selected for the paper were chosen primarily to illustrate the points discussed, rather than because they were widely used in practice. The circuit of figure 7, composed of single-phase units or three-phase shell-type units, would not be recommended unless it was permanently connected to a low-impedance return path for zero-sequence fundamental and third-harmonic currents. Such a path might consist of a generator or synchronous condenser, having a low impedance to zero-sequence currents and permanently connected to the transformer, so that the transformer was not in service unless the machine was also operating. The machine neutral would also have to be connected to the transformer neutral with the minimum possible neutral impedance intervening.

If the regulating transformer of figure 7*a* were composed of small-size three-phase core-type units, it would probably be safe from the standpoint of third harmonic currents, but the zero-sequence circuit of figure 7*d* would be modified by the zero-sequence properties of the core-type unit.

Both Miss Clarke and Professor Kimbark have pointed out the possibilities of using other components than the usual positive-, negative-, and zero-sequence components. The components suggested have both advantages and drawbacks. For their components to be used, it is necessary to assume that the positive- and negative-sequence system impedances are everywhere equal. Where main transmission circuits are of primary importance, the characteristics of the generators and other rotating machines are usually predominant and this assumption is not allowable. Furthermore, in stability studies, where generating sources must be maintained separate, the representation of each source in two networks instead of one often introduces more complication than it saves. On the other hand, in studying the distribution system, the characteristics of the generators assume less importance, and the advantages of the method suggested may outweigh its disadvantages.

An Automatic A-C Potentiometer and Its Application to the Nondestructive Testing of Insulating Equipment

GEORGE KEINATH
MEMBER AIEE

THE importance of the measurement of dielectric loss for determining the quality of high-voltage equipment has been well known for a long time by the high-voltage-cable industry and its measurement has been included in the international regulations for acceptance tests of cables. It was obvious that the same method that had been applied successfully to cables could be used also for testing other high-voltage devices, such as transformers, bushings, etc.

Shortcomings of the Present Method of High-Voltage Testing With System Frequency

The short-time test with about twice the phase-to-phase voltage, as it is usually applied in routine tests, does not give any indication of what happens in the dielectric during the test. Many times the voltage stress during the test is so high that the object is partly damaged without, however, breaking down completely. This kind of test corresponds to a mechanical test during which the maximum load is applied without observing the deformation of the material as the test progresses.

Disadvantages of the Thomas-Schering Bridge When Used for Insulation Testing

The manual adjustment of the bridge takes too long a time for the usual high-voltage test lasting only about two minutes, half of which is used for raising the voltage from a low value to test voltage. To obtain the characteristic diagram of power factor versus voltage and time, it would be necessary to take within these two minutes at least ten readings of capacitance and power factor, and half of them at different voltages. Since this is impossible one has to be satisfied with a few readings at various voltages, with the highest one usually falling short of the maximum test voltage, because of the difficulty encountered in reading the instrument at the maximum value where it becomes too unsteady.

Principle of the New Automatic A-C Potentiometer

Automatic d-c potentiometers, with very high sensitivity, have been known for a long time. Their original designs require 30 to 40 seconds for the pen to go from one end of the chart to the other because of the step-by-step adjustment by a mechanical contactor, which is very similar to the manual adjustment of a potentiometer. The latest automatic d-c potentiometers are much faster and require only about two seconds for full-scale deflection, but are more complicated and, therefore, more expensive to manufacture.

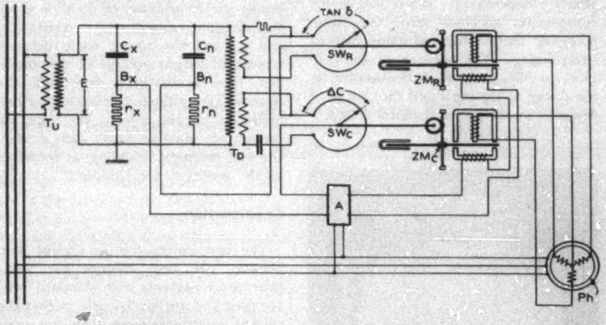


Figure 1. Schematic diagram of the automatic a-c potentiometer

The a-c potentiometer must be provided with *two* zero indicators, one for the active component of the current, and the other for the reactive component. It must also have two adjusting devices which may consist of variable resistors, inductors, or capacitors, whose settings indicate directly the measured value in a way similar to the slide-wire contact of the d-c potentiometer. Most of the recording d-c potentiometers in actual service have a galvanometer used as zero indicator and a continuously running motor and chopper-bar attachment which changes the position of the sliding contact when the value to be measured has changed. For considerable changes in value several steps are necessary for obtaining the final indication.

An a-c potentiometer developed by Geyger is equipped with an entirely new element, namely the *zero motor*, which serves both as zero indicator and as motor for adjusting the sliding contact. As there are two adjustments to be made, one for the active and one for the reactive component, we must have two zero motors, two sliding contacts, and two recorder charts.

Connection of the Potentiometer

Figure 1 shows schematically a simple arrangement for testing a high-voltage capacitor C_x by the voltage E from the testing transformer T_u. A standard capacitor C_n of negligible power factor is also connected to the testing transformer. In series with the capacitors are small resistors r_x and r_n, respectively. Furthermore, in parallel with C_n and C_x and their associated resistors there is a step-down transformer T_D with two secondary windings feeding two slide wires. One, SW_R, is in series with a resistor and the other, SW_C, is in series with a capacitor, resulting in currents and voltage drops which are in phase

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Paper number 39-73, recommended by the AIEE committee on instruments and measurements, and presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939. Manuscript submitted October 22, 1938; made available for preprinting December 27, 1938.

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and in quadrature, respectively, with the high voltage E. The bridge is balanced when the two points B_x, B_n have the same potential. If this condition is not fulfilled, we may change either resistance or capacitance until equilibrium is established or, what is actually done, we add or subtract voltages from both slide wires and read the values of power factor tan δ and change of capacitance ΔC immediately from the position of the slide-wire contacts. This adjustment of active and reactive voltages in the bridge is done automatically by the two zero motors ZM_R and ZM_C. The voltage on the diagonal of the bridge, between the points B_x and B_n, is impressed on an amplifier A with power supplied by the line voltage. The output voltage of this amplifier is connected to the current coils of the two zero motors, whose voltage coils are connected through a phase shifter Ph to the phase-to-phase voltage and phase-to-neutral voltage, respectively. In this way one of the motors ZM_R gives a torque only with the active component of the amplified current, the other with its reactive component. Any voltage existing between the points B_x and B_n is amplified and feeds the motors, which move the contacts on the resistors SW_R and SW_C so that the voltage between the points B_x and B_n is more and more nearly compensated. When one of the components becomes zero, the corresponding motor stops at once without overrunning. The position of the contacts on SW_R and SW_C indicates directly the power factor tan δ and the change of capacitance ΔC of the material tested.

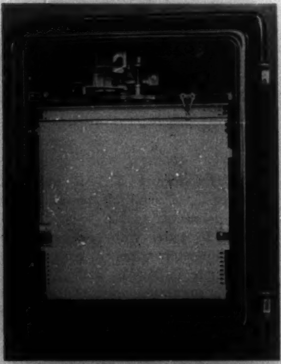


Figure 2. Recorder with built-in zero motor for moving the slide-wire contact and the pen

Actual connections of the a-c potentiometer are somewhat different from figure 1. Voltage for feeding the two slide-wire resistors SW_R and SW_C is not taken from a special step-down transformer but from a resistance in series with the standard capacitor C_n. The power required to supply the slide wires is so small that practically no additional error is involved. In some cases it may also be more convenient to replace the standard capacitor C_n by a voltage transformer of very high accuracy. The standard capacitor is then connected to the secondary of this transformer.

Properties of the Testing Equipment

Both motors run with variable speed. When the voltage difference between the points B_x and B_n is great, the speed is very high, about five revolutions per second, and becomes lower as balance is approached. Large deflections over the whole 250-millimeter (ten-inch) width of the paper are accomplished in not more than two seconds. The final setting of the contact and with it of the recorder pen is independent of line voltage and degree of amplification. Both factors influence only the speed but not the final results. The accuracy depends on the well-known elements of the a-c bridge, on the accuracy of the slide wires, the standard capacitor, the resistors r_x and r_n, and the phase angle between input and output voltage of the amplifier. The sensitivity is very high, the recorder giving the full deflection for two millivolts on the input; therefore, all precautions must be taken such as electric and magnetic shielding, as for other highly sensitive a-c bridges.

Construction

The new recorder is of utmost simplicity. The zero motors are watt-hour-meter elements with standard voltage coils and are special only in that the current coils have more than the usual number of ampere-turns. All other parts, including the slide wires, are the same as in other recorders of similar kind, but with the necessary shielding (figure 2). The sliding contact is connected with the pen and in this way continuous diagrams of power factor and capacitance are obtained from the recorders.

Sensitivity and Accuracy

The sensitivity may be the same as with any other a-c bridge, and is limited only by the degree of perfection obtainable in shielding the parts of the complete circuit, and by the accuracy of the standard resistors r_x and r_n and the capacitor C_n. For general testing work on bushings a range of five per cent for both power factor and capacitance is very useful, with changes of 0.1 per cent in power factor resulting in a deflection of 2.5 to 5 millimeters. For cable testing the range is adjusted to one per cent or even as low as 0.2 per cent, in order to detect sudden changes in capacitance or power factor in the order of 0.01 per cent or even lower. It must be understood, however, that these figures refer to sensitivity and not to accuracy. In this kind of test work it is not important to know whether the power factor is, say, 0.42 per cent or 0.43 per cent absolute, but it is important to know whether

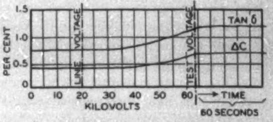


Figure 3. Change of capacitance and dielectric loss during the test of a bakelite-paper bushing for 20-kv rated voltage

Test voltage is 64 kv. Power factor is 0.75 per cent up to 32 kv and rises to 1.2 per cent at test voltage; a very good result. Change of capacitance is about 0.3 per cent between line voltage and test voltage. Capacitance increases at almost the same voltage as ionization begins

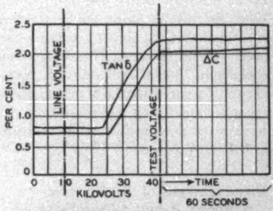


Figure 4. Same test on a bushing for 10-kv rated voltage and 42-kv test voltage

The initial value of power factor is almost the same, 0.8 per cent, but above 24 kv the ionization increases rapidly, the power factor at 43 kv being 2.25 per cent. The capacitance begins to increase at 26 kv; at 43 kv the increase is 1.2 per cent. Note slight, but definite, increase of both power factor and capacitance during the test there was a sudden change of 0.01 per cent during the test.

Advantage of a Continuous Record of Power Factor and Capacitance

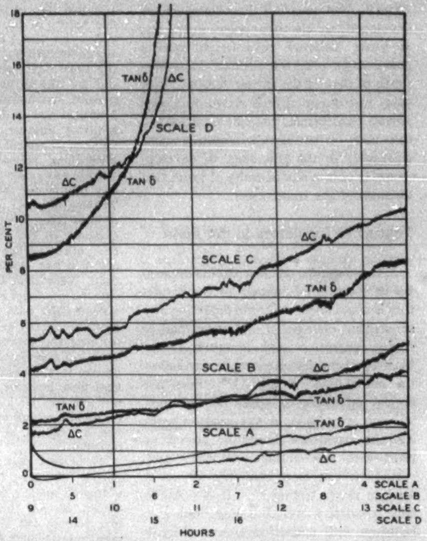
A continuous diagram gives much more information than spot readings plotted and connected to form a curve. It gives such details as small sudden changes in capacitance and power factor occurring when there is a partial breakdown in the dielectric. These changes might never be detected by spot readings.

Application of the A-C Potentiometer

A record is very useful, wherever dielectric losses are measured versus voltage or versus time with constant voltage. To record losses against voltage, we may move the paper not proportionally to time but proportionally to the voltage (which is a special feature of this form of an a-c potentiometer by moving the transport wheel of the paper instead of moving the recording pen) or one may raise the voltage of the testing transformer proportionally to time and move the paper chart in the usual way proportionally to time. The potentiometer is sensitive enough to give a reliable record with 20 per cent or even 10 per cent of the maximum voltage.

Figure 5. Testing of an old, compound-filled cable for $15\sqrt{3}$-kv rated voltage and 200 amperes rated current, with 50 kv and 300 amperes

(Copied from two separate diagrams for tan δ and ΔC, each 16 hours long)



Speed of paper chart 60 millimeters per hour. During the first half hour, power factor goes down from 1.2 per cent to 0.5 per cent due to temperature influences, capacitance changes only slightly, 0.1 or 0.2 per cent. After passing a minimum, both power factor and capacitance increase at the rate of about 0.5 per cent per hour. The diagram is first a thin line, but after more than one hour becomes thicker and rippled, showing deterioration of the cable during the test. With increasing losses the rise of tan δ becomes more rapid, the range has to be changed several times until final breakdown occurs with about 20 per cent power factor and increase of capacitance. Note the unsteady line in the last two-thirds of the test, a sure sign of the coming breakdown

Testing of High-Voltage Equipment, for Example Bushings, Transformers, Cables, Capacitors

The voltage is raised proportionally to time up to test voltage and kept constant for the required time, usually one minute, while recording is done. Figures 3 and 4 show the results of test on two bakelite-paper bushings. Figure 5 gives the results of testing a high-voltage cable for 20 hours with seven times rated voltage and 50 per cent overcurrent. In the beginning of the curve, during the first three hours, the line is thin and even. It shows the variations of capacitance and dielectric loss with temperature. Changes in capacitance, not observed at all by other methods, may sometimes be even more significant than changes in power factor. After a few hours the line becomes thicker and rippled, indicating that the setting of the zero motor is no longer stable, which in turn shows that deterioration of the insulation has begun and the destruction of the cable has been started. The initial range was 0–6 per cent, but had to be changed to 6–12 per cent and later to 12–18 per cent. The increases of both power factor and capacitance are almost the same and rise rapidly until complete breakdown occurs after about 20 hours, without any damage to the instruments. Diagrams of this kind would provide excellent supporting data for any high-voltage test on cables as they give complete records of what is going on during the test.

Value of a Continuous Record in Comparison With Spot Readings

The continuous record, visibly written with ink on a paper chart, enables us to observe the whole test to see whether anything abnormal is going on. If the power factor of a bushing or of a transformer rises much faster than usual, the test may be stopped before the complete breakdown occurs, to give the device a better vacuum treatment for the next test.

The entire diagram gives a picture of the relative strength of the object under continuous high-voltage stress. If the ionization point is below the voltage between the lines, the device tested cannot be considered as good for continuous service. But if at test voltage the power loss is unusually small and not much greater than at line voltage, indicating that test voltage could be maintained indefinitely, the insulation is better than required. In this case either the insulation may be reduced or the same device may be used for the next rated standard voltage. By intensive study of current and voltage transformers in this manner it was possible to reduce their weight to such an extent that a complete set of three current transformers and three voltage transformers for 220-kv line voltage and 460-kv test voltage weighs only 12 tons, resulting in considerable reduction of the price and in possibilities of much larger applications than with the much more expensive earlier designs. The recording and measuring of power factor was the only means of insuring in the laboratory the absolute safety of operation which was confirmed later in many years of actual service. Not one of all these transformers developed any electrical defect in the last 12 years, although their weight was in many cases only one-fifth or even one-tenth of that of the earlier designs. Not only instrument transformers but also power transformers may be improved in this way, by testing all insulating parts separately in the first stage of development, to obtain a uniformity of insulation throughout the transformer.

Testing of Insulators in the Field

A test diagram taken with this apparatus at the time of delivery of the insulators is an excellent means of detecting any subsequent changes in insulating quality during operation and is much better than spot checks often made far below the actual line voltage. If it does not appear advisable to apply the full test voltage after three or five years of service, the insulators should be tested at least with line voltage. With a test voltage of not more than ten kv, as is used in many testing sets, it is possible only to detect wet bushings, but not bushings with ionization at some higher voltage. It is not possible within the scope of this paper to give a complete description of portable measuring sets, its purpose is only to attract attention to the much better information obtained from comparing diagrams for the insulator when new and after a period of service than from comparing figures. Even some temperature correction could be made automatically during the test, using temperature-sensitive standards for comparison.

It appears impossible to predict all the possible applications of the automatic a-c potentiometer. A number of European manufacturers are using it already in their test rooms, and some utility companies also are using it to test with it the high-voltage equipment prior to installation. In the opinion of the author it is in the interest of both manufacturers and users to complete the present method of test with a record which gives a clear picture of the uniformity and quality of the product. Potentiometer tests made as described herein give a very good indication of the subsequent behavior of the equipment in service with the possibility of checking changes of quality at any time later.

Discussion

George Keinath (in presenting the paper for discussion): In addition to my report, I should like to tell you something about the history of this development. Some 15 years ago the VDE (Verband Deutscher Elektrotechniker) raised the test voltage for 12-kv equipment from 30 to 42 kv. I did not like to have bigger and more expensive instrument transformers designed, I wanted just to improve the insulation by applying another, in my opinion better, vacuum treatment of the high-voltage coils. Everything seemed to go well, the test voltage was raised from 30 to 42 kv without any trouble for the short time of the test. Trouble, however, started plenty about two months later, when the same transformers exploded in the power stations, one after the other. They were compound-filled and it is not necessary to describe the picture. Very soon we found that the reason was the imperfect impregnation of the high-voltage coils, with the formation of nitrous gases inside the windings. It was easy to improve the process of impregnation, but with this experience I had lost all my confidence in the usual high-voltage test. At that time, however, 15 years ago, no better method for testing current and voltage transformers was existing, for testing with the a-c bridge at test voltage was impossible.

To be sure that the transformers would not be damaged by running with rated voltage, I made a type test with every new design, testing the transformer with the test voltage (42 kv for the 10-kv instrument transformer) not just one minute, but *eight hours*. This is certainly a very severe test, but I had no difficulty with any oil-insulated transformer and it was never necessary to increase the dimensions. All new current and voltage transformers were tested in this way, for 110-kv line voltage with 240 kv, for 240 kv with 460 kv, during eight hours. The result was the entire elimination of all failures in actual service—the bad thing, however, was that some of the customers wanted this test made for all transformers ordered by them.

Such an eight-hour test is very expensive and it will certainly not improve the insulating quality of the object under test. So I had to look out for a new method for suitable routine testing of transformers. This could be only a method which gave a record of the test—a diagram of the power factor and capacity change during the test. First we used a very sensitive wattmeter and later a vibrating mechanical rectifier and a d-c mirror galvanometer, and got a record of the active component of the capacity current versus voltage and versus time on photographic paper. Diagrams of this kind already had proved very successful for the development of new transformer designs, but the method was not good for routine testing of high-voltage equipment because the diagram had to be developed in the darkroom. So I decided six years ago to find a method to have the diagram written with ink on a paper chart during the short time of the ordinary voltage test, so that every change of capacity or power factor could be seen immediately and that the test could be stopped before the eventual breakdown was complete.

This recorder is described in my paper. It is for the greater part the work of my former colleague and collaborator Wilhelm Geyger, who during almost all his engineering time studied d-c and a-c potentiometers and their application for the most different purposes.

By using this recorder for testing all instrument transformers, and testing all their parts separately, I was able to reduce their dimensions more than anybody else tried to do before and afterward. I never did care for any "standards" regarding the dielectric stress as long as the power-factor record at test voltage was low, usually not more than two or three times the value at line voltage. Many designs originally intended for a certain line voltage have finally been used for 25 or 50 per cent higher rated voltage (of course with corresponding increase of flashover distance), after it turned out that the test voltage could be raised so far without any trouble. The total weight of a 110-kv current transformer which can be tested with 240 kv during eight hours and which will finally break down at 350 kv after this test was reduced by-and-by to less than 1,000 pounds.

The 220-kv current transformer was finally built with a weight of only 3,500 pounds; it was tested with 460 kv during eight hours and broke down with 650 kv after this test. Far more than 1,000 current transformers of this design are in actual service in Europe and there has never been a single electrical breakdown on them in the field, after they had been developed to have low power factor not only at the rated voltage, but also at test voltage. It seems to me that this proves well enough that the new method of testing corresponds with the practical experience. This method has been adopted also by other companies. The Allgemeine Elektrizitaets Gesellschaft uses a diagram drawn with the present recorder for advertising their voltage transformers and their good insulating qualities.

Eric A. Walker (Tufts College, Medford, Mass.): In the paper entitled "An Automatic A-C Potentiometer and Its Application to the Nondestructive Testing of Insulating Equipment," Doctor Keinath has made a very definite contribution to the power-factor testing of high-voltage insulation.

Engineers who must make power-factor tests have long realized the handicap imposed by the time required to balance a bridge circuit, especially if several guard or shield circuits must be balanced in addition to the principle bridge. Very often when testing used insulation, such as oil from circuit breakers or cellulose insulation which has been contaminated by moisture, the characteristics are not the same at the beginning of the test as when the test has been completed. For this very reason, wattmeter sets, with adequate shielding and correction factors to account for I^2R losses in the circuit, have considerable merit. By means of such a device a curve can be plotted point by point. In this paper, however, we are presented with a device which not only shortens the time required to make an observation but even automatically records that observation.

There are several points which the author might explain in more detail. First of all, must the standard capacitor C_n of the author's figure 1 have approximately the same capacitance as the object being tested? If not, what preliminary adjustments must be made before the potentiometer is ready to start a recording?

In a later paragraph the author speaks of an application to the testing of insulation in the field. We all know that most devices in the field which may be power-factor tested have one electrode grounded, such as the flange of a bushing or the tank of a transformer. This fact would require some changes in the wiring diagram of figure 1. Not only must the position of the ground shown in this figure be changed but the system of shielding must be modified. Moreover, unless a preliminary balance is made, which is what the author is trying to avoid, the shielding must be such that the capacitance which is then connected in parallel with the measuring means does not change as the physical position of the transformer, the high-voltage conductor, and other parts of the circuit are changed. This is one of the factors which makes the testing of grounded equipment rather difficult.

I would also like to ask the author what the response of the instrument will be if one attempts to make measurements in a strong electrostatic field? It is perhaps well-known, that when making dielectric loss measurements in the field, the currents induced in the circuit by electrostatic coupling between the object being tested and nearby energized wires are quite large. Although such currents may not seriously affect the capacitance balance of the bridge, their phase angle may be such that the power factor indicated is several times the true power factor. Indeed, negative power-factor indications are quite possible. Such erroneous values can hardly be avoided by shielding because it is highly impractical to attempt to shield a power transformer or circuit breaker in a congested substation.

In field testing, magnetic interference cannot be entirely neglected for one might be called upon to test a device which is near conductors carrying large currents.

One solution, of course, is to use a test voltage of different frequency from that of the interfering voltages or currents. Such a scheme is impractical for large devices because of the cost of generating equipment.

It is quite possible that further development is required before the device is adapted to the field testing of insulation, but there is no doubt about its value as a new laboratory tool.

P. L. Betz (Consolidated Gas, Electric Light and Power Company of Baltimore, Baltimore, Md.): The paper that has just been presented is of considerable interest to those engaged in dielectric testing. There are, however, several suggestions as to content that I would like to make. I have in mind a group of papers on dielectric testing which at first glance appears to give a complete story, yet, on closer consideration, one realizes that no paper in the group has given a complete circuit diagram which includes the shielding arrangements. I should like the author of this paper to include in his printed closing discussion a circuit diagram of his bridge that shows the actual shielding circuits, and the actual circuit means for supplying voltage for the two slide-wire resistors SW_R and SW_C. In the case of the shield circuits, what preliminary balancing is necessary and does this balance, made at a given voltage, apply for all of the other values of test voltage?

I would also like to ask the author for a comparison of the power factor measurements obtained with his bridge, and those obtained by means of a Schering bridge.

L. J. Berberich (Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Doctor Keinath has described a very ingenious device for continuously recording power factor and capacitance change. In some insulation-testing applications it certainly has some advantage over the spot-reading procedures that must be used with the conventional bridges. The extent of the application of the device will, however, be strongly influenced by the cost of the equipment. We would appreciate it, therefore, if Doctor Keinath would give us some idea of the cost of the equipment necessary in order to transform a conventional power-factor bridge into one that records automatically.

In some of his earlier papers, Doctor Keinath suggested the use of automatic power-factor recording devices for testing the insulation on the stator windings of high-voltage generators. We believe that no power-factor method is satisfactory for testing the older machines, in which the contact between stator core and the outer surface of the coil insulation may not be definite. In the later machines, steps have been taken to make this contact definite and suppress all corona discharge that may form between the coil surface and stator slot and in these cases a test showing the power-factor behavior with voltage may have some value. In the older machines, however, a sharply rising power factor with voltage may be caused by either internal ionization or ionization between outer coil surface and slot. There is no way to distinguish between these and this is one of the problems that makes nondestructive testing of generator insulation difficult.

H. R. Richardson (Consolidated Edison Company of New York, Inc., New York, N. Y.): The recording a-c potentiometer described in Doctor Keinath's paper is of interest to the user of electrical insulating equipment in that continuous curves showing the trend and variations of power factor and capacitance with voltage or temperature against time are easily obtained. The record of momentary fluctuation of these quantities is of interest but I wonder if it is significant, especially in the case of oil- or compound-filled cables or bushings where small localized trouble spots are self-healing.

As this circuit is similar to the Dawes-Hoover high-voltage bridge with the exception of using a capacitor in the slide-wire circuit in place of a mutual inductor to obtain the quadrature balancing voltage, I presume that the shield and guard potentials must be adjusted in the same manner for this bridge as for the Dawes-Hoover bridge.

I would be interested in more detail as to the method of changing the range of this bridge. In particular, which resistor is changed and can this be done while the circuit is alive and the recorder in operation? Where the power factor is in the order of 20 per cent, as shown in figure 5 of the paper, would the accuracy of the bridge be seriously affected by this method of increasing the range?

Also, is a fixed phase shifter used to supply the fields of the zero motors? If so, what provision is made for a preliminary manual balance to assure the user that these fields are in the correct phase relation to the components of the unbalanced bridge current, especially as regards possible phase defect between line voltage and bridge detector voltage?

W. F. Davidson (Consolidated Edison Company of New York, Inc., New York, N. Y.): It was my good fortune during the past summer to have an opportunity to see in operation some of the recorders described in this paper. Of particular interest was a demonstration of the precision and speed of response. This was made by arranging a test sample so that small resistances might be inserted in series to increase the loss in power factor by a predetermined amount or so that small capacitances could be connected in parallel to cause an abrupt but definite change in the capacitances. For one set of tests, the full-scale power factor was two per cent while the scale in the capacitance element was such that a two per cent change represented the full range of the record. In both cases, the response was so rapid that it was impossible without the aid of special instruments to detect any time lag and at the same time there was no overtravel. Also, the power-factor record was entirely independent of the capacitance record and conversely the capacitance was influenced by changes in power factor.

My own experience during a good many years of testing insulation, particularly in high-voltage cables, has convinced me that a continuous record would reveal much information of decided value. This would be doubly true if the time scale could be sufficiently expanded so as to reveal rapid changes which escape detection when manually operated bridges must be resorted to.

A somewhat disturbing feature at the moment is the high cost of the equipment. Recently, I was given quotations on equipment to be imported from Germany and the figure was so high as to make it almost prohibitive, but this does not appear to be an unsurmountable obstacle.

A. J. Williams, Jr. (Leeds and Northrup Company, Philadelphia, Pa.): Doctor Keinath's solution to his problem is most interesting. As I understand it, with only a limited amount of time to make tests on each piece of equipment, he arranged to collect the maximum amount of information in the time that was available. He did this by placing a recorder on watch to make a record of the two principal factors throughout the whole time. Before this was done, the question might have been raised whether the extra information was helpful, but the end result obtained, namely, the production of smaller and more reliable equipment, seems to answer this question.

Doctor Keinath mentioned definitely that the gain of the amplifier does not affect the accuracy. It appears also that any phase shift in the amplifier would not affect the accuracy since at balance nothing comes through the amplifier anyway. This independence of phase shift is not general for power-factor measurement, but is definitely restricted to null measurements.

In connection with errors from the amplifier, it is possible that two or more upper harmonics from the bridge might, as a result of modulation in the amplifier, produce some input into the zero motor at fundamental frequency. This would result in a false shift in the balance position of the recorder. Although this type of error is not often encountered, the recorder described would be somewhat susceptible because of the high power level to which the amplification is carried, hence the greater chance for modulation.

It is most interesting that Doctor Keinath has been able to arrange his circuits so that balance of both components can be obtained by the use of slide-wires (rather than decade switches), which lend themselves to simple recorder construction.

In closing this discussion, I would like to ask:

(*a*). What was the approximate size of the slide-wires?

(*b*). What gearing was used between the zero motors and the slide-wires?

(*c*). Are the power-factor pen and capacitance pen in the same or separate recorders?

Frank C. Doble (Doble Engineering Company, Medford Hillside, Mass.): The paper by Doctor Keinath makes an interesting contribution along a line of investigation that needs attention in factory practice and one that should prove to be valuable to the manufacturers of electrical insulated equipment.

As a matter of general interest, it may be worth while to call attention to a statement in his paper under the subheading "Testing of Insulators in the Field," as follows:

> With a test voltage of not more than ten kv, as is used in many testing sets, it is possible only to detect wet bushings, but not bushings with ionization at some higher voltage.

This statement will bear careful analysis because "in the field" brings to mind routine tests on equipment as installed in operating position, whereas, the test described in the paper is more properly a manufacturer's test on new insulation at the factory.

In this country, nine years' experience with 10 kv for hundreds of thousands of field tests on insulation of all voltage ratings from 11 kv to 220 kv, inclusive, indicates that a 10-kv test is entirely adequate to show practically all forms of deteriorations, including the effects of moisture and corona, before they become immediate failure hazards.

It appears that in foreign practice there is a feeling that ten kv can not find, in time to prevent a failure, deterioration due to ionization, present only at or near operating voltage which might be several times ten kv. Without doubt, this belief is due to the fact that outside of the United States a very limited number of field tests on electric power equipment have been made at any voltage. Inexperience with actual ten-kv field tests accounts for the opinion still held by a few United States engineers that field tests at operating voltages are indispensable.

In American practice, there are numerous examples of actual failures on all operating voltages that were predicted by a ten-kv test. The case history of a 220-kv bushing failure which was predicted by a 10-kv test is available, together with similar data for all other operating voltages.

Moreover, ten-kv tests, properly applied to parts of a bushing, will reveal certain types of serious faults that would not be found by an over-all applied voltage up to or even greater than the normal operating voltage of the equipment under test.

We have records of approximately 1,000 tests made on solid-type bushings at least ten years old at a test voltage equivalent to $1^1/_2$ times normal line to ground voltage. Very thorough investigation involving bushings dissected at the manufacturer's laboratory failed to disclose evidence of a serious failure hazard that would not be found by a ten-kv test in time to prevent failure in service.

A further point to consider regarding high-voltage testing in the field is that many extraneous losses inherent to high-voltage equipment but not causing deterioration might be present in a volume as great as a real defect that should be found. It appears that the disadvantage of these extra losses more than offset the apparent advantages of testing in the field at voltages higher than ten kv.

George Keinath: In answering Mr. Walker's question, I should like to say that the standard capacitor may have almost any capacity. Actually the following are used:

between
100 and 2,000 volts—50,000 micromicrofarads
1,000 and 20,000 volts— 2,000 micromicrofarads
5,000 and 250,000 volts— 100 micromicrofarads
20,000 and 500,000 volts— 50 micromicrofarads

There has to be a preliminary adjustment to bring the pen into the chosen range of the recorder, so that the width of the paper is utilized, as it should be, only for the expected *change* of capacity which is usually approximately the same as the power factor.

Of course the wiring diagram has to be different if the capacitor to be tested is grounded with one electrode.

The influence of electrostatic and electromagnetic fields on the bridge is the same as in any other bridge and for high sensitivity shielding has to be very effective. I was describing only a new device for an automatic a-c potentiometer recorder and all this could not help in the least to avoid difficulties as they arise with any a-c bridge.

Mr. Richardson is absolutely right if he says that with the measurement of power factor on a large object one cannot easily detect localized trouble spots. Even when the loss on this spot increases perhaps 100 per cent or more in comparison to the loss in the same area in the good parts of the object, this may be less than 0.1 per cent of the total loss in the capacitor.

Just as the shielding, this has nothing to do in principle with the automatic a-c potentiometer, but refers to the value of power-factor measurements in general. Actual experience, however, has shown that with a continuous record one can detect small partial breakdowns in the dielectric much better than by making spot tests. According to the sensitivity you have chosen, and which for a cable test well might be five inches for one per cent power-factor change, you can easily see a change of 0.01 per cent power factor by a sudden break of the line, at the same time mostly also on the capacity curve. On a hand-set bridge, however, you cannot rely on such small changes generally and you do not know whether you have a sudden break, or a slow rise which might be caused by increase of temperature. Naturally, if the trouble spot is too small in comparison with the whole capacitor area, you will not find it, even with the most sensitive recorder. On smaller objects, on bushings or transformers, partial breakdown due to defective spots is easily detected by a sudden break of the line. Some time ago, I published a number of diagrams taken with the photographic recorder which I already have mentioned.

The range of the recorder is changed by adjusting the tappings on the resistors on the grounded side. This is done while the circuit is alive and the recorder in operation. When the power factor is as high as 20 per cent, there is always not much accuracy left because the capacitor is very near breakdown. In these last minutes or perhaps hours of capacitor life you have a large amount of higher harmonics which take part in the development of heat. I cannot give data for the accuracy under such circumstances, but I think it would not be important if the actual value were 18 per cent instead of 20 per cent. It seems to me that the absolute value of power factor and capacity are always of much less importance than the recording of the smallest changes of same, hitherto not observed with spot readings.

In itself, the change of the range for power factor or capacity by adjusting the resistances, is as accurate as it should be, depending only on the accuracy of the resistors.

The phase shifter which supplies the current for the zero motors is set before the test. It is not necessary to readjust it during the test, even a considerable phase shift between the voltages in the three-phase supply, several degrees, could not affect the bridge.

I am much obliged to Doctor Davidson to give a report on his personal impression when he saw the recorder in my former laboratories in Berlin.

Replying to Mr. Berberich, I would say that a number of the recorders actually in use have been designed and delivered as a supplement to old a-c bridges with vibration galvanometers so that the recorder always may be checked with the old bridge wiring. For high voltage, for more than 200 kilovolts, the standard capacitor is the most expensive part of the whole equipment. The new parts comprise the recorders, amplifiers, and some resistors, the cost of same may be between $2,000 and $3,000, probably less, if they would be manufactured in this country.

I admit that some time ago I hoped to be able to get good information by the power-factor testing of generator coils. However, as I said in my paper, the power factor itself can never give by its numerical value any reliable indication of the quality of the insulation. Five per cent may be very good for a generator, but it is very bad for an oil-insulated coil. But, if you have either records of several generators of the same kind or of the same generator under different conditions, for example, after several years of service, you may be able to deduct important information on differences and changes of the power factor-voltage and capacity-voltage curves at approximately the same temperature. I have tried to develop a recorder which gives a continuous curve of the dielectric losses in a generator. I could not finish this work, because too many considerations had to be taken before a suitable generator in one of the big power stations of Berlin could be selected for this test, but I still think that this would give a very good picture of the temperature inside the windings of the generator, so that it could be run not to a maximum temperature, but to a maximum dielectric loss. Excessive dielectric loss destroys the insulation and brings down the breakdown voltage. The fact that it rises very fast with the temperature makes the measurement more sensitive than the measurement of temperature with thermocouples.

To answer Mr. Betz, the limits of accuracy in general are given by the a-c bridge; for the recorder they are given by the accuracy of the slide-wire calibration and by the accuracy of the mechanical parts used for the transmission. Perfect shielding is just as necessary as with any other a-c bridge and the necessary shielding is not typical for the automatic potentiometer, therefore I did not give details about the shielding. The voltage for the slide-wire is actually supplied from the standard capacitor, so that it is not necessary to have a step-down transformer for the test voltage.

Mr. Doble has objected to my statement that for high-voltage testing of bushings a higher voltage than ten kv should be used. There is no doubt about the excellent work Mr. Doble has performed with his method. He can claim that he has reduced to a great extent the number of failures due to bad bushings, but it seems to me, that only in exceptional cases he has tested the same bushings with a higher voltage too. I admit that I have no practical personal experience with tests in the field, but I have tested a number of bushings in the laboratory and we have found that some of them were perfect at ten kv, but had considerable ionization at line voltage. It seems to me that such bushings should be eliminated at once and not put in service until the deterioration is so much advanced that also the test at ten kv shows up the trouble. I do not think that there is much difference in behavior, besides the fact that a part of the humidity evaporates after bringing the bushings into the warm laboratory.

It seems to me that a diagram with varying voltage gives much more information than a single point, especially when it is compared with the diagram at the time of delivery. I think that Mr. Doble's method could be very much improved in this way. If it should come to the weight of the testing equipment, I should say, that I had developed on the other side testing transformers with a very small weight, so that even an equipment for 150- or 200-kv test voltage could easily be transported with a light truck. I am sorry not to agree with Mr. Doble when he states that the dissecting of bushings never disclosed failures that would not also be found by a ten-kv test. We found corona traces on 60-kv bushings with a perfect 10-kv record.

I am much obliged to Mr. Williams to tell me that even a phase shift in the amplifier does not affect the accuracy of the measurement. The zero motor is practically tuned to the fundamental wave, because the current in the voltage coil has an almost pure sine shape and does therefore not respond to the higher harmonics eventually produced by the amplifier. We have made special tests and found that even 100 per cent third and fifth harmonic did not change the indication.

To answer Mr. William's questions:

(*a*). The slide-wire is of the toroid type and about six inches long.

(*b*). The transmission between the slide-wire and the motor is done by a metallic ribbon.

(*c*). The two zero motors and the two pens may be either in one or in two recorders, depending on the width of the recording diagram desired. Actually both types of recorders are used.

Two-Phase Co-ordinates of a Three-Phase Circuit

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MEMBER AIEE

IN RECENT years there has been an increasing interest in the use of substituted variables in the analysis of unbalanced three-phase circuits and machines. The best known and most widely used of the substituted variables are symmetrical components, introduced by Fortescue[1] in 1918. In the method of symmetrical components the actual currents and voltages of the three phases of a three-phase circuit are replaced by the zero-sequence, positive-sequence, and negative-sequence currents and voltages, and, furthermore, the three-phase circuit itself is replaced for purposes of analysis by three fictitious single-phase circuits known as the sequence networks. This method has achieved extensive use in the analysis of polyphase machinery and in the making of fault studies on three-phase power systems. Another example of substituted variables is furnished by the direct-axis and quadrature-axis quantities as defined by Park[2] in 1929, which have greatly facilitated the mathematical analysis of salient-pole synchronous machines.

This paper deals with a new set of substituted variables, which will be found simpler to use than symmetrical components in the analysis of certain types of unbalanced three-phase circuits. The new variables are related[3] both to symmetrical components and to the direct- and quadrature-axis quantities, but are different from either. They were used by Park and Skeats,[4] by Clarke, Weygandt, and Concordia,[5] and by Stanley[6] in the analysis of transient problems; and have been developed by Miss Clarke[7,8] for use in steady-state problems. They have been called "modified symmetrical components" or "α-β components"; but the author believes that they are of sufficient importance to merit a more distinctive name, and proposes the name "two-phase co-ordinates (or components)" as an appropriate one, because the three-phase circuit to be analyzed is replaced by an equivalent two-phase circuit and a single-phase circuit similar to the zero-sequence network.

The purpose of this paper is to provide a comprehensive and systematic derivation of the equations and equivalent circuits in two-phase co-ordinates of all the usual three-phase circuit elements such as series and shunt impedances, open circuits and short circuits, transformer banks, and transmission lines. This material, which will be found in the appendix, is preceded by a short discussion of the properties of two-phase co-ordinates and by several examples of their application.

Definition of Two-Phase Components

The two-phase components of current (I_x, I_y, I_z) and of voltage (V_x, V_y, V_z) are defined as follows in terms of the phase currents (I_a, I_b, I_c) and the phase voltages (V_a, V_b, V_c):

$$\left.\begin{aligned} I_x &= (2I_a - I_b - I_c)/3 \\ I_y &= (I_c - I_b)/\sqrt{3} \\ I_z &= (I_a + I_b + I_c)2/3 \end{aligned}\right\} \quad (1)$$

$$\left.\begin{aligned} V_x &= (2V_a - V_b - V_c)/3 \\ V_y &= (V_c - V_b)/\sqrt{3} \\ V_z &= (V_a + V_b + V_c)/3 \end{aligned}\right\} \quad (2)$$

Note that the expression for I_z differs from that for V_z by a factor 2.

If equations 1 and 2 are solved for the phase quantities in terms of their two-phase components, the following expressions are obtained:

$$\left.\begin{aligned} I_a &= I_x + {}^1/_2 I_z \\ I_b &= -{}^1/_2 I_x - (\sqrt{3}/2)I_y + {}^1/_2 I_z \\ I_c &= -{}^1/_2 I_x + (\sqrt{3}/2)I_y + {}^1/_2 I_z \end{aligned}\right\} \quad (3)$$

$$\left.\begin{aligned} V_a &= V_x + V_z \\ V_b &= -{}^1/_2 V_x - (\sqrt{3}/2)V_y + V_z \\ V_c &= -{}^1/_2 V_x + (\sqrt{3}/2)V_y + V_z \end{aligned}\right\} \quad (4)$$

The current and voltage symbols in equations 1 to 4 may represent either instantaneous values (real numbers) or steady-state values (complex numbers or vectors); however, the use of steady-state values only is considered in this paper.

Relation of Two-Phase Components to Symmetrical Components

By substituting into equations 1 and 2 the usual expressions for phase currents and voltages in terms of their symmetrical components, we obtain the following expressions for two-phase components in terms of symmetrical components:

$$\left.\begin{aligned} I_x &= I_1 + I_2 \\ I_y &= j(I_1 - I_2) \\ I_z &= 2I_0 \end{aligned}\right\} \quad (5)$$

$$\left.\begin{aligned} V_x &= V_1 + V_2 \\ V_y &= j(V_1 - V_2) \\ V_z &= V_0 \end{aligned}\right\} \quad (6)$$

where subscripts 0, 1, 2 denote zero- positive-, and negative-sequence components, respectively. The x current and voltage are equal to the sum of the positive- and negative-sequence components, and the y current and voltage are equal to the difference of the positive- and negative-sequence components, multiplied by j. The z voltage is the zero-sequence voltage, but the z current is twice the zero-sequence current.

Imagine a balanced set of three-phase currents of positive sequence. Their negative-sequence component, I_2, is zero. Their two-phase components are given by equations 5 as $I_x = I_1$ and $I_y = jI_1$, which denote balanced two-phase currents of phase order y, x. (The phase angle between I_x and I_y is the same as that between the conventional positive directions of the x and y axes of a rectangular co-ordinate system.) If, on the other hand, the three-phase currents are of negative sequence, their two-phase components are given by equations 5 as $I_x = I_2$ and $I_y = -jI_2$, which denote balanced two-phase currents of phase order x, y.

The symmetrical components in terms of two-phase components are:

$$\left.\begin{aligned} I_0 &= I_z/2 \\ I_1 &= (I_x - jI_y)/2 \\ I_2 &= (I_x + jI_y)/2 \end{aligned}\right\} \quad (7)$$

$$\left.\begin{aligned} V_0 &= V_z \\ V_1 &= (V_x - jV_y)/2 \\ V_2 &= (V_x + jV_y)/2 \end{aligned}\right\} \quad (8)$$

Relation of Two-Phase Components to Direct- and Quadrature-Axis Quantities

The direct-axis current, i_d, and the quadrature-axis current, i_q, are defined as follows in terms of the instantaneous

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Paper number 39-96, recommended by the AIEE committee on basic sciences, and presented at the AIEE North Eastern District meeting, Springfield, Mass., May 3-5, 1939. Manuscript submitted July 26, 1938; made available for preprinting March 28, 1939.

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The author acknowledges his indebtedness to Edith Clarke and Charles Concordia, of the central-station department, General Electric Company, Schenectady, N. Y., for important material.

1. For all numbered references, see list at end of paper.

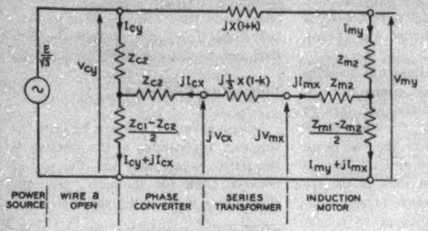


Figure 1. Equivalent circuit in two-phase co-ordinates of a phase converter taking single-phase power and supplying three-phase power through a series transformer in wires b and c to a three-phase induction motor (problem 3)

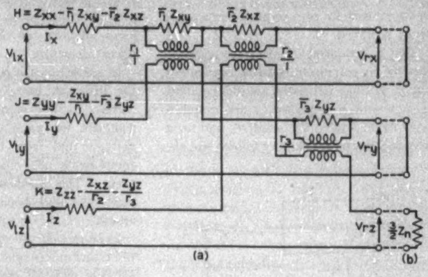

H = Zxx - r̄1 Zxy - r̄2 Zxz
r̄1 Zxy
r̄2 Zxz
Ix
V1x
r1/1
r2/1
Vrx
J = Zyy - Zxy/r1 - r̄3 Zyz
r̄3 Zyz
Iy
V1y
r3/1
Vry
K = Zzz - Zxz/r2 - Zyz/r3
Iz
V1z
Vrz
3/2 Zn
(a)
(b)

Figure 2. Equivalent circuit for series impedances (a), and, when joined to (b), for wye-connected shunt impedances

armature phase currents (i_a, i_b, i_c) of a synchronous machine:

$$\left.\begin{aligned} i_d &= (2/3)[i_a \cos\theta + i_b \times \\ &\quad \cos(\theta - 120°) + i_c \cos(\theta + 120°)] \\ -i_q &= (2/3)[i_a \sin\theta + i_b \\ &\quad \sin(\theta - 120°) + i_c \sin(\theta + 120°)] \end{aligned}\right\} \quad (9)$$

where θ = electrical angle between the magnetic axis of phase a and the direct field axis, varying with time. If we put $\theta = 0$ in equations 9, the expressions for i_d and $-i_q$ become equal respectively to those for I_x and I_y in equations 1. A like relation holds for voltages.

The direct- and quadrature-axis currents, i_d and i_q, give magnetomotive forces on two axes in space quadrature and fixed with respect to the field structure, while the two-phase components of current, I_x and I_y, give magnetomotive forces along axes in space quadrature and fixed with respect to the armature. If the two-phase components, I_x and I_y, of given three-phase currents, I_a, I_b, and I_c, flow in the windings of a symmetrical two-phase machine, they produce a space-fundamental magnetomotive force proportional to that produced by the given three-phase currents flowing in the windings of an equivalent three-phase machine. Furthermore, the electromotive forces induced in the windings of a two-phase machine by a given space-fundamental magnetic flux density are proportional to the two-phase components, E_x and E_y, of the electromotive forces, E_a, E_b, and E_c, induced in the windings of the equivalent three-phase machine by the same flux density.

Physical Interpretation of Two-Phase Components

Examination of equations 3 brings out the physical meaning of the two-phase components of current. Suppose that current component I_x is present alone, I_y and I_z being absent. The actual currents are then found by setting I_y and I_z equal to zero, which gives:

$$I_a = I_x \qquad I_b = I_c = -I_x/2 \quad (10)$$

That is, I_x is a current which flows out on conductor a and returns equally divided between conductors b and c.

Again, suppose that I_y is present alone. Putting $I_x = I_z = 0$ in equations 3 gives:

$$I_a = 0 \qquad I_c = -I_b = (\sqrt{3}/2)I_y \quad (11)$$

That is, I_y is a current flowing out on conductor c and back on conductor b. It does not flow in a.

I_z, of course, has the well-known characteristics of zero-sequence current.

Measurement of Two-Phase Components

The two-phase components of current and voltage in a three-phase circuit may be measured by ammeters and voltmeters connected to the three-phase circuit through groups of instrument transformers, the connections and ratios of which may be determined from inspection of the coefficients in equations 1 and 2. Since the coefficients are real, no impedances need be employed, as in the measurement of positive- and negative-sequence quantities, to shift phase.

If I_z is known to be absent, I_x is equal to I_a and may be read on an ammeter in line a. V_x is proportional to the reading of a voltmeter connected from line a to the mid-point of an autotransformer or drop wire connected between lines b and c. V_y is proportional to the voltage between lines b and c.

Equations 3 and 4 indicate how the phase currents and voltages can be read directly by combining electrically the two-phase components existing in networks set up on a calculating board.

Expression for Power in Terms of Two-Phase Components

The total complex power (usually called "vector power") of a three-phase circuit is given by

$$P + jQ = I_a \operatorname{conj} V_a + I_b \operatorname{conj} V_b + I_c \operatorname{conj} V_c \quad (12)$$

On substituting equations 3 and 4 into 12, the expression for complex power of a three-phase circuit is obtained in terms of two-phase components:

$$P + jQ = (3/2)(I_x \operatorname{conj} V_x + I_y \operatorname{conj} V_y + I_z \operatorname{conj} V_z) \quad (13)$$

The power is seen to be invariant except for the presence of the constant factor 3/2. This degree of invariancy is necessary for the correct representation of short circuits or open circuits on a three-phase network by connections between the substitute networks.

Substitute Networks

The substitution of new currents and voltages for the three-phase currents and voltages in every branch of a three-phase network in accordance with equations 3 and 4 may be extended to embrace the substitution of three new single-phase networks for the original three-phase network. The new networks are the x network, wherein I_x and V_x exist, the y network, wherein I_y and V_y exist, and the z network, wherein I_z and V_z exist. They are analogous to the positive-, negative-, and zero-sequence networks used in the method of symmetrical components. They may be set up to scale on a calculating board, or used merely as diagrams for paper calculation.

The x and y networks may be regarded as jointly constituting a two-phase network equivalent to the given three-phase network with its zero-sequence or z quantities removed.

For a balanced three-phase network having no polyphase rotating machinery, the x, y, and z networks are independent. The x and y impedances are equal to each other and to the positive- and negative-sequence impedances (which are also equal to each other in this case). The z impedances are one-half the corresponding zero-sequence impedances. Even in the case of polyphase rotating machinery it is sometimes regarded as sufficiently accurate to assume the positive-sequence impedance equal to the negative-sequence impedance, and in such cases it is permissible to assume that the x and y networks are independent. To be more accurate, however, an inequality in the positive- and negative-sequence impedances is equivalent to a coupling between the x and y networks, in the nature of a nonreciprocal mutual impedance, $Z_{xy} = -Z_{yx}$, which is difficult to represent on a calculating board.

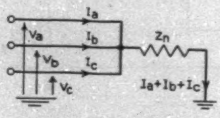


Figure 3. Wye connection

If the three-phase network contains balanced three-phase electromotive forces of positive phase sequence, then the x and y networks contain electromotive forces of equal magnitude, those in the y network leading those in the x network by 90 degrees. Ordinarily the z network contains no generated electromotive forces.

The self- and mutual impedances of the substitute networks and the connections between the networks corresponding to any given impedances and connections of the three-phase network or a portion thereof may be found by the following process: (1) Draw a diagram of the three-phase circuit element. (2) Write equations expressing the relations between the phase voltages, currents, and impedances. (3) By substituting equations 3 and 4 into the equations mentioned in step 2, obtain an equal number of equations giving the relations between the two-phase components of current and voltage and the impedances. (4) Find an equivalent circuit satisfying the equations obtained in step 3. Such circuits can always be found if the use therein of ideal transformers of real or complex ratio is allowed. In the appendix there are presented the results of this process for a large number of different three-phase circuit elements.

In some cases it is simpler to consider the different two-phase components of current to flow separately, and then to compute for each the resulting phase voltages and from them the two-phase components of voltage and the impedances. This method is used in the appendix to find transmission-line impedances.

In those cases in which the x and y networks are independent, the following simple rule may be used to find their self-impedances: Z_{xx} is two-thirds of the impedance between line a and lines b and c joined together. Z_{yy} is one-half of the impedance between lines b and c.

Comparison of Two-Phase Co-ordinates and Symmetrical Co-ordinates

The method of two-phase co-ordinates has two advantages over the method of symmetrical co-ordinates: First, as shown by inspection of the equivalent circuits developed in the appendix, the y network is independent of the x and z networks (though the latter two are usually coupled) in every case in which the represented three-phase circuit is static and is symmetrical with respect to phase a. Second, even if the static three-phase circuit is not symmetrical with respect to phase a, the substitute networks are coupled by reciprocal mutual impedances, which are readily represented on a calculating board, and never by nonreciprocal mutual impedances as is the case in the method of symmetrical co-ordinates.

The disadvantages of the method of two-phase co-ordinates are, first, that the x and y networks are coupled nonreciprocally in the case of balanced rotating machines having unequal positive- and negative-sequence impedances, whereas the positive- and negative-sequence networks of such machines are independent; and, second, that both the x and y networks contain generated electromotive forces, whereas in the method of symmetrical co-ordinates only the positive-sequence network contains them.

The method of two-phase co-ordinates is thus most useful in the analysis of unbalanced static three-phase circuits which are symmetrical with respect to one phase. In this category fall many transformer connections (such as delta-delta with one transformer different from the other two, vee-vee, vee-reversed vee, tee-tee, and Scott), open circuits, short circuits, single-phase loads, etc. The analysis is particularly simple if the connections are also such that zero-sequence current cannot flow.

Illustrative Problems

Several examples will now be given to illustrate applications of the method of two-phase co-ordinates and the use of material from the appendix. For applications to fault studies, see reference 7.

PROBLEM 1

Statement. A common device for indicating the phase rotation of a three-phase circuit consists of two lamps and a reactor connected in star. For one phase order one lamp is brighter than the other and for opposite phase order the second lamp is brighter than the first. Show which lamp is brighter when a positive-sequence voltage is applied to the device, and calculate the current in each lamp if its resistance is 100 ohms, the impedance of the reactor, $0 + j100$ ohms, and the applied voltage, 110 (line to line).

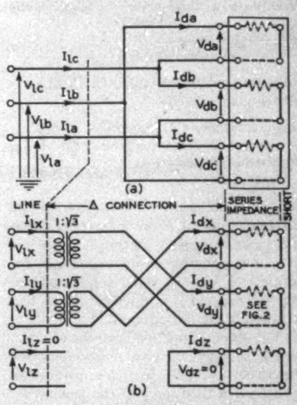


Figure 4. Delta connection of impedances (a) and its equivalent circuit (b)

Qualitative Analysis. It is advisable to denote the phases in such manner that the circuit is symmetrical with respect to phase a; hence the reactor will be called phase a, and the lamps, b and c.

The path of I_x consists of the reactor in series with the parallel combination of two lamps; the impedance of this path is predominantly inductive, and therefore I_x lags behind E_x by a large angle. The path of I_y, being through the two lamps in series, is resistive; hence I_y is in phase with E_y.

For balanced positive-sequence voltage, E_1, the two-phase components of voltage are equal in magnitude and are in time quadrature, E_y leading; that is, $E_y = jE_x = jE_1$. As a result of this phase relation between E_x and E_y and of those between E_x and I_x and between E_y and I_y, it follows that I_x and I_y are approximately in phase opposition.

The expressions for the lamp currents, I_b and I_c (using equations 3 and noting that $I_z = 0$) are proportional to $I_x + \sqrt{3}I_y$ and to $I_x - \sqrt{3}I_y$, respectively. Inasmuch as I_x and I_y are nearly in opposition, it is obvious that I_c is larger than I_b, and that lamp c is brighter than lamp b if the phase sequence is positive (abc).

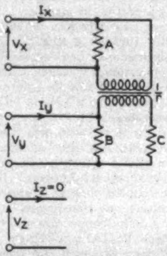


Figure 5. Equivalent circuit for shunt impedances connected in delta or in ungrounded wye

Calculations

$E_x = E_1 = E_a = 110/\sqrt{3} = 63.5$ volts,
$E_y = jE_x = j63.5$ volts,
$Z_a = j100$ ohms, $Z_b = Z_c = 100$ ohms.

Refer to cases 14 and 9 of appendix. From equations 9*b*,

$Z_x = (1/3)(2Z_a + Z_b) = (100 + j200)/3$ ohms, and $Z_y = Z_b = 100$ ohms
$I_x = E_x/Z_x = 63.5 \times 3/(100 + j200)$
$\quad = 0.38 - j0.76$ ampere
$I_y = E_y/Z_y = j63.5/100 = j0.64$ ampere
$I_z = 0$ because the neutral of the star is isolated
$I_b = {}^1/_2(-I_x - \sqrt{3}\, I_y + I_z)$
$\quad = {}^1/_2(-0.38 + j0.76 - j1.10)$
$\quad = -0.19 - j0.17$ ampere
$|I_b| = 0.26$ ampere
$I_c = {}^1/_2(-I_x + \sqrt{3}I_y + I_z)$
$\quad = {}^1/_2(-0.38 + j0.76 + j1.10)$
$\quad = -0.19 + j0.93$ ampere
$|I_c| = 0.95$ ampere

PROBLEM 2

Statement. A certain three-phase 60-cycle overhead line, 1.0 mile long, consists of three number 0000 seven-strand copper cables spaced two feet apart in a plane. There are no transpositions and no ground wires. What impedances should be connected in series with the wires to equalize the voltage drops of the three wires when the line currents are balanced?

Solution. Compute the line impedances Z_{xx} and Z_{yy} by substituting into equations 34*e* the following data:

$R_a = R_b = R_c = 0.303$ ohm per mile, $D_{aa} = D_{bb} = D_{cc} = 0.0158$ foot, $D_{ab} = D_{ac} = 2$ feet, $D_{bc} = 4$ feet, $\omega = 2\pi \times 60$ radians per second. This gives $Z_{xx} = 0.303 + j0.559$ and $Z_{yy} = 0.303 + j0.669$ ohm. In addition, $Z_{xy} = 0$.

In order for balanced line currents to give balanced voltage drops, the line impedances Z_{xx} and Z_{yy}, must be equal. To equalize them Z_{xx} must be increased by $j0.110$ ohms. From case 7 of the appendix we find that an impedance Z in wire a is equivalent to $2Z/3$ in network x, but has no effect on network y. Hence the line impedances may be equalized by adding an impedance $1.5 \times j0.110 = j0.165$ ohm in series with the center wire.

Since $Z_{zz} = j0.028$ ohms, a small V_z drop is produced by balanced line currents, tending to unbalance the line-to-ground, but not the line-to-line voltages.

PROBLEM 3

Statement. A three-phase induction motor, connected to a single-phase line (on terminals b and c) and running without shaft load, is used as a phase converter to supply three-phase power to a second induction motor, which runs loaded. Investigate the efficacy of a series transformer with negative mutual inductance, connected in wires b and c of the line between the phase converter and the motor, in reducing the voltage unbalance at the motor terminals.

Analysis. The single-phase line may be considered as a three-phase line, wire a of which is open. The three-phase circuit then consists of the following parts in the order named: (1) single-phase source of voltage E connected between wires b and c, (2) open circuit in wire a of line, (3) phase converter in shunt with line, (4) series transformer in phases b and c of line, (5) induction motor in shunt with line. The equivalent circuit for two-phase components is made up of the equivalent circuits for the several parts, connected in the same order. These circuits are found in the following sections of the appendix: phase converter and induction motor, cases 2 and 14; open circuit, case 11; series transformer, case 10. The single-phase source is represented by an electromotive force $E/\sqrt{3}$ in network y. The equivalent circuit resulting from the combination of the several parts may be simplified in two ways: (1) omission of network z, since I_z cannot flow; (2) omission of the two transformers of ratios $1:j$ through which networks x and y are coupled, with the understanding that the currents and voltages in the x network now represent jI_x and jV_x instead of I_x and V_x. The circuit thus simplified is shown in figure 1, with notation of the impedances as follows: Z_{c1} and Z_{c2} are the positive- and negative-sequence impedances of the phase converter; Z_{m1} and Z_{m2}, the same for the motor; X is the self-reactance of the transformer coils in wires b and c; and k is the coefficient of coupling, making the mutual reactance $-kX$.

Knowing numerical values of these impedances, it is now an elementary network problem to compute the motor terminal voltages jV_{mx} and V_{my}; and if $jV_{mx} = V_{my}$ the voltages are balanced. It should be noted that $(Z_{c1} - Z_{c2})/2$ is several times as large as Z_{c2} for an unloaded machine and that motor voltage jV_{mx} is lowered by the drop $2Z_{c2}jI_x$. It is now clear that the function of the series transformer is to add impedance to network y without adding an appreciable amount of it to network x, thus lowering V_{my} to the already lowered value of jV_{mx}. If, for simplicity, we assume $k = 1$, the impedances added to networks x and y are, respectively, 0 and $j2X$. To produce balanced motor voltage, jX should equal Z_{c2} as a first approximation.

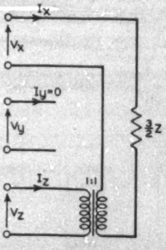


Figure 6. Equivalent circuit for shunt impedance Z connected line to neutral or ground

The equivalent circuit of figure 1, with suitable modifications, may be used to investigate the effect of other devices, such as a series capacitor in wire a, tapped autotransformers, etc., on the motor terminal voltages.

The solution of this problem by symmetrical components is given by Lyon.[10]

Conclusion

Though prognostication is uncertain, it seems probable that the chief application of the method of two-phase coordinates will be in the field of power distribution, with its single-phase loads, unsymmetrical transformer connections, untransposed lines, and mixtures of three-phase and two-phase circuits; whereas the chief application of the method of symmetrical co-ordinates has been in the fields of transmission and of polyphase machinery.

At any rate, a man who is confronted with a problem on unbalanced three-phase circuits should consider which method is best suited to his particular problem—the use of the actual three-phase currents and voltages, of symmetrical components, of two-phase components, or of some other substitution of variables devised to fit the specific problem.

Appendix. Equations and Equivalent Circuits for Two-Phase Components Corresponding to Various Three-Phase Circuit Elements

General Remarks

Notation

V_{la}, V_{lb}, V_{lc} = line-to-ground voltages of wires *a*, *b*, *c* at left-hand side of the circuit element, volts. The positive direction of voltage rise is from ground to line as indicated by the arrows in figure 3

V_{lx}, V_{ly}, V_{lz} = corresponding two-phase components

V_{ra}, V_{rb}, V_{rc} = line-to-ground voltages at right-hand side of circuit element, volts

V_{rx}, V_{ry}, V_{rz} = corresponding two-phase components

For shunt elements subscripts *l* and *r* are omitted

I_{la}, I_{lb}, and I_{lc} = line currents in wires *a*, *b*, *c*, entering left-hand side of the circuit element, amperes

I_{lx}, I_{ly}, I_{lz} = corresponding two-phase components

I_{ra}, I_{rb}, I_{rc} = line currents leaving right-hand side of circuit element

I_{rx}, I_{ry}, I_{rz} = corresponding two-phase components

For shunt elements or series elements subscripts *l* and *r* are omitted

For transformer banks subscripts *p* and *s* are used instead of *l* and *r*, and the positive direction of all currents is toward the transformers as indicated by the arrowheads in figures 9 to 12, inclusive

Z = impedance, ohms. Various subscripts are used and the significance in any particular case is explained in the text for that case or is apparent from the equations or figures thereof

r = transformer turns ratio

Other symbols are defined in the text for particular cases.

Units. It should be noted that all voltages, currents, and impedances, both in the equations and in the diagrams, are in volts, amperes, and ohms, respectively, and not in per unit. This usage was followed with the idea of keeping the change of variables under discussion, namely, the change from phase quantities to two-phase components, separate from any additional changes of variable, such as going to per-unit quantities or referring quantities on one side of a transformer to the other side.

Use of Transformers in the Equivalent Circuits. The equivalent circuits presented in this appendix consist of self-impedances and of ideal transformers having no leakage impedance nor exciting admittance. Such transformers offer no difficulty in computation; on the a-c calculating board they may be represented by actual transformers in which the leakage impedances and exciting admittances either are negligible or are compensated for by changes in other impedances of the circuit or by introducing adjustable sources of voltage. The transformer voltage ratios, generally one-to-one, are marked on the diagrams, and the polarity is understood to be subtractive; but a negative value of the ratio is equivalent to additive polarity. In one case the imaginary ratio $1{:}j$ is used. For a voltage ratio of $1{:}j$ the current ratio is also $1{:}j$; that is, both current and voltage are shifted 90 degrees in the same direction. (In general, the current ratio must be the conjugate of the reciprocal of the voltage ratio to make the complex power equal in the two windings.)

Simplification of Equivalent Circuits. In all the equivalent circuits the three substitute networks are shown as insulated from each other, constituting a six-wire circuit; and the lower or neutral side of each network is shown without impedance. It is permissible to combine the neutral sides of the three networks, thus forming a four-wire circuit. In some cases it is more convenient to join the neutral side of one network to the upper or line side of another. Such a case is the line-to-ground short circuit, figure 7*a*, in which the transformer may be eliminated if such a connection be made.

Wherever two networks or subnetworks are coupled through a single transformer, the transformer may be replaced by a conductive connection if voltages, currents, and impedances on one side be referred to the other side (no change if ratio is 1:1). If the networks are coupled through several transformers of the same ratio, one transformer may be replaced by a direct connection and the others by 1:1 transformers, or in some cases also by direct connections. If the networks are coupled through transformers of different ratios, one transformer may be replaced by a direct connection and the ratios of the others changed accordingly.

Another way in which the transformers may sometimes be eliminated can be illustrated by reference to figure 12. The 1:1 transformer in that figure may be omitted if impedance Z_{xy} be placed in the common neutral wire of networks *x* and *y*.

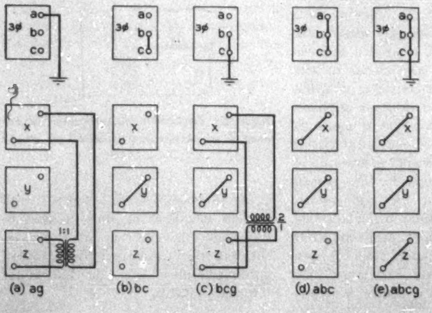
By elimination of transformers as suggested, the equivalent circuits may be considerably simplified. However, any such simplifications which would remove the insulation between the substitute networks have not been made in the circuits for three-phase circuit elements presented here because the best method of simplifying the equivalent circuit of any given three-phase network depends on what elements the complete network consists of.

Series Impedances

1. General Series Impedances With Nonreciprocal Mutual Impedances

The most general case of series impedances is the one in which the self-impedances of the three phases are unequal and in which there are mutual impedances between phases which are both unequal and nonreciprocal. (The mutual impedance between two circuits is nonreciprocal if the voltage produced in the first circuit by unit current in the second circuit is not equal to the voltage produced in the second circuit by unit current in the first.) Although this general case seldom occurs in practice, it is well to develop it first and then to derive various special cases of practical interest from it.

Let the impedances be designated by Z with two subscripts, the first of which refers to the voltage, the second to the current. The phase voltage drops may then be



(a) ag (b) bc (c) bcg (d) abc (e) abcg

Figure 7. Short circuits symmetrical with respect to phase a, and their equivalent circuits in two-phase co-ordinates

written as follows in terms of the phase currents and impedances:

$$\left.\begin{aligned} V_{la} - V_{ra} &= Z_{aa}I_a + Z_{ab}I_b + Z_{ac}I_c \\ V_{lb} - V_{rb} &= Z_{ba}I_a + Z_{bb}I_b + Z_{bc}I_c \\ V_{lc} - V_{rc} &= Z_{ca}I_a + Z_{cb}I_b + Z_{cc}I_c \end{aligned}\right\} \text{(1a)}$$

The corresponding expressions in symmetrical components are:

$$\left.\begin{aligned} V_{l0} - V_{r0} &= Z_{00}I_0 + Z_{01}I_1 + Z_{02}I_2 \\ V_{l1} - V_{r1} &= Z_{10}I_0 + Z_{11}I_1 + Z_{12}I_2 \\ V_{l2} - V_{r2} &= Z_{20}I_0 + Z_{21}I_1 + Z_{22}I_2 \end{aligned}\right\} \text{(1b)}$$

where each Z is a certain function of the Z's in equations 1a.[9]

The corresponding expressions in two-phase components are:

$$\left.\begin{aligned} V_{lx} - V_{rx} &= Z_{xx}I_x + Z_{xy}I_y + Z_{xz}I_z \\ V_{ly} - V_{ry} &= Z_{yx}I_x + Z_{yy}I_y + Z_{yz}I_z \\ V_{lz} - V_{rz} &= Z_{zx}I_x + Z_{zy}I_y + Z_{zz}I_z \end{aligned}\right\} \text{(1c)}$$

where

$$\left.\begin{aligned} Z_{xx} &= (2/3)Z_{aa} + (1/6)(Z_{bb} + Z_{cc} + Z_{bc} + Z_{cb}) - (1/3)(Z_{ca} + Z_{ac} + Z_{ab} + Z_{ba}) \\ &= {}^1\!/_2(Z_{11} + Z_{12} + Z_{21} + Z_{22}) \\ Z_{xy} &= 1/(2\sqrt{3})(Z_{bb} - Z_{cc} - Z_{bc} + Z_{cb} - 2Z_{ab} + 2Z_{ac}) \\ &= {}^1\!/_2j(-Z_{11} + Z_{12} - Z_{21} + Z_{22}) \\ Z_{xz} &= (1/3)(Z_{aa} + Z_{ab} + Z_{ac}) - (1/6)(Z_{bb} + Z_{cc} + Z_{bc} + Z_{ca} + Z_{cb} + Z_{ba}) \\ &= {}^1\!/_2(Z_{10} + Z_{20}) \\ Z_{yx} &= 1/(2\sqrt{3})(Z_{bb} - Z_{cc} + Z_{bc} - Z_{cb} + 2Z_{ca} - 2Z_{ba}) \\ &= {}^1\!/_2j(Z_{11} + Z_{12} - Z_{21} - Z_{22}) \\ Z_{yy} &= {}^1\!/_2(Z_{bb} + Z_{cc} - Z_{bc} - Z_{cb}) \\ &= {}^1\!/_2(Z_{11} - Z_{12} - Z_{21} + Z_{22}) \\ Z_{yz} &= 1/(2\sqrt{3})\ (-Z_{bb} + Z_{cc} - Z_{bc} + Z_{ca} + Z_{cb} - Z_{ba}) \\ &= {}^1\!/_2j\ (Z_{10} - Z_{20}) \\ Z_{zx} &= (1/3)(Z_{aa} + Z_{ca} + Z_{ba}) - (1/6)(Z_{bb} + Z_{cc} + Z_{bc} + Z_{ab} + Z_{cb} + Z_{ac}) \\ &= {}^1\!/_2(Z_{01} + Z_{02}) \\ Z_{zy} &= 1/(2\sqrt{3})(-Z_{bb} + Z_{cc} + Z_{bc} - Z_{ab} - Z_{cb} + Z_{ac}) \\ &= {}^1\!/_2j\ (-Z_{01} + Z_{02}) \\ Z_{zz} &= (1/6)(Z_{aa} + Z_{bb} + Z_{cc} + Z_{bc} + Z_{ca} + Z_{ab} + Z_{cb} + Z_{ac} + Z_{ba}) = {}^1\!/_2Z_{00} \end{aligned}\right\} \text{(1d)}$$

No equivalent circuit is offered for this general case on account of the difficulty of representing the nonreciprocal mutual impedances between substitute networks.

2. Balanced Series Impedances With Balanced Nonreciprocal Mutual Impedances (as in Three-Phase Rotating Machinery)

This is a special case of case 1, where

$$\left.\begin{aligned} Z_{aa} &= Z_{bb} = Z_{cc} \\ Z_{ab} &= Z_{bc} = Z_{ca} \\ Z_{ac} &= Z_{ba} = Z_{cb} \end{aligned}\right\} \text{(2a)}$$

Equations 1d become

$$\left.\begin{aligned} Z_{xx} &= Z_{yy} = Z_{aa} - {}^1\!/_2(Z_{bc} + Z_{cb}) \\ &= {}^1\!/_2(Z_{11} + Z_{22}) \\ Z_{xy} &= -Z_{yx} = (\sqrt{3}/2)(-Z_{bc} + Z_{cb}) \\ &= {}^1\!/_2j(-Z_{11} + Z_{22}) \\ Z_{zz} &= {}^1\!/_2(Z_{aa} + Z_{bc} + Z_{cb}) = {}^1\!/_2Z_{00} \\ Z_{xz} &= Z_{yz} = Z_{zx} = Z_{zy} = 0 \end{aligned}\right\} \text{(2b)}$$

The z network is independent of the others, but there is a nonreciprocal coupling between the x and y networks. For an equivalent circuit use figure 2, putting $r_1 = j$, $r_1Z_{xy} = {}^1\!/_2(Z_{11} - Z_{22})$, $Z_{xz} = Z_{yz} = 0$, $H = J = Z_{22}$, and $K = {}^1\!/_2Z_{00}$, and omitting transformers r_2 and r_3.

3. Unbalanced Series Impedances With Reciprocal Mutual Impedances

This is a special case of case 1, where

$$Z_{bc} = Z_{cb},\quad Z_{ac} = Z_{ca},\quad Z_{ab} = Z_{ba} \qquad \text{(3a)}$$

Equations 1d become:

$$\left.\begin{aligned} Z_{xx} &= (2/3)(Z_{aa} - Z_{ab} - Z_{ac}) + (1/3)Z_{bc} + (1/6)(Z_{bb} + Z_{cc}) \\ &= Z_{11} + {}^1\!/_2(Z_{12} + Z_{21}) \\ Z_{yy} &= {}^1\!/_2(Z_{bb} + Z_{cc}) - Z_{bc} \\ &= Z_{11} - {}^1\!/_2(Z_{12} + Z_{21}) \\ Z_{zz} &= (1/3)(Z_{bc} + Z_{ab} + Z_{ac}) + (1/6)(Z_{aa} + Z_{bb} + Z_{cc}) \\ &= {}^1\!/_2Z_{00} \\ Z_{xy} &= Z_{yx} = (1/\sqrt{3})(Z_{ac} - Z_{ab}) + 1/(2\sqrt{3})(Z_{bb} - Z_{cc}) \\ &= {}^1\!/_2j(Z_{12} - Z_{21}) \\ Z_{xz} &= Z_{zx} = (1/3)(Z_{aa} - Z_{bc}) + (1/6)(Z_{ab} + Z_{ac} - Z_{bb} - Z_{cc}) \\ &= {}^1\!/_2(Z_{01} + Z_{02}) \\ Z_{yz} &= Z_{zy} = 1/(2\sqrt{3})(Z_{cc} - Z_{bb} + Z_{ac} - Z_{ab}) = {}^1\!/_2j(Z_{02} - Z_{01}) \end{aligned}\right\} \text{(3b)}$$

There is reciprocal coupling between each pair of networks. An equivalent circuit is shown in figure 2. For simplicity the transformer ratios, r_1, r_2, and r_3, may be taken as unity, though in some cases a different ratio may be advisable.

4. Series Impedances and Reciprocal Mutual Impedances Symmetrical With Respect to Phase a

This is a special case of cases 1 and 3, where

$$Z_{bb} = Z_{cc},\quad Z_{bc} = Z_{cb},\quad Z_{ab} = Z_{ba} = Z_{ac} = Z_{ca} \qquad \text{(4a)}$$

Equations 1d become:

$$\left.\begin{aligned} Z_{xx} &= (2/3)Z_{aa} + (1/3)(Z_{bb} + Z_{bc}) - (4/3)Z_{ab} \\ Z_{yy} &= Z_{bb} - Z_{bc} \\ Z_{zz} &= (1/6)Z_{aa} + (1/3)(Z_{bb} + Z_{bc}) + (2/3)Z_{ab} \\ Z_{xz} &= Z_{zx} = (1/3)(Z_{aa} - Z_{bb} - Z_{bc} + Z_{ab}) \\ Z_{xy} &= Z_{yx} = Z_{yz} = Z_{zy} = 0 \end{aligned}\right\} \text{(4b)}$$

Network y is independent of the others, while x and z are coupled. For an equivalent circuit use figure 2, omitting transformers r_1 and r_3.

5. Balanced Series Impedances With Balanced Reciprocal Mutual Impedances

This is a special case of cases 1, 2, 3, and 4 where

$$\left.\begin{aligned} Z_{aa} &= Z_{bb} = Z_{cc} = Z_s \\ Z_{ab} &= Z_{bc} = Z_{ca} = Z_{ac} = Z_{ba} \\ &= Z_{cb} = Z_m \end{aligned}\right\} \text{(5a)}$$

Equations 1d become:

$$\left.\begin{aligned} Z_{xx} &= Z_{yy} = Z_s - Z_m = Z_{11} = Z_{22} \\ Z_{zz} &= {}^1\!/_2Z_s + Z_m = {}^1\!/_2Z_{00} \\ Z_{xy} &= Z_{xz} = Z_{yx} = Z_{yz} = Z_{zx} \\ &= Z_{zy} = 0 \end{aligned}\right\} \text{(5b)}$$

The substitute networks are independent. For an equivalent circuit use figure 2, omitting all transformers and putting $Z_{xy} = Z_{xz} = Z_{yz} = 0$.

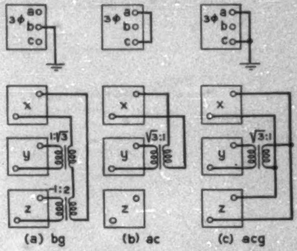


Figure 8. Short circuits symmetrical with respect to phase b, and their equivalent circuits in two-phase co-ordinates. For symmetry with respect to phase c, reverse network y

6. Unbalanced Series Impedances Without Mutual Impedance

This is a special case of cases 1 and 3, where

$$Z_{bc} = Z_{cb} = Z_{ac} = Z_{ca} = Z_{ab} = Z_{ba} = 0 \qquad \text{(6a)}$$

Equations 1d become:

$$\left.\begin{aligned} Z_{xx} &= (2/3)Z_{aa} + (1/6)(Z_{bb} + Z_{cc}) \\ Z_{yy} &= {}^1\!/_2(Z_{bb} + Z_{cc}) \\ Z_{zz} &= (1/6)(Z_{aa} + Z_{bb} + Z_{cc}) \\ Z_{xy} &= Z_{yx} = -Z_{yz} = -Z_{zy} \\ &= 1/(2\sqrt{3})(Z_{bb} - Z_{cc}) \\ Z_{xz} &= Z_{zx} \\ &= (1/3)Z_{aa} - (1/6)(Z_{bb} + Z_{cc}) \end{aligned}\right\} \text{(6b)}$$

For an equivalent circuit use figure 2.

7. Series Impedance Z in One Wire (a)

Although this is a special case of case 6, we may as well start from the relations between phase quantities:

$$\left.\begin{aligned} V_{la} - V_{ra} &= I_aZ \\ V_{lb} - V_{rb} &= 0 \\ V_{lc} - V_{rc} &= 0 \end{aligned}\right\} \text{(7a)}$$

The corresponding relations between two-phase components are:

$$\left.\begin{aligned} V_{lx} - V_{rx} &= 2(V_{lz} - V_{rz}) \\ &= (2/3)\ Z(I_x + {}^1\!/_2I_z) \\ V_{ly} - V_{ry} &= 0 \end{aligned}\right\} \text{(7b)}$$

For an equivalent circuit use figure 2, omitting transformers r_1 and r_3, and putting $r_2 = 2$, $r_2Z_{zz} = (2/3)Z$, $Z_{xy} = Z_{yz} = 0$, and $H = J = K = 0$.

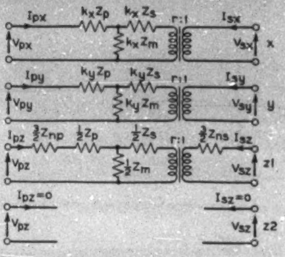


Figure 9. Equivalent circuits of wye-wye, delta-delta, and vee-vee transformer banks

For wye-wye, put $k_x = k_y = 1$, and use network z1

For delta-delta, put $k_x = k_y = 1/3$, and use network z2

For vee-vee, put $k_x = 1/3$, $k_y = 1$, and use network z2

The circuits shown are for zero phase displacement; for 180-degree displacement reverse the secondary terminals of each network

8. EQUAL SERIES IMPEDANCES Z IN TWO WIRES (b AND c)

Relations between phase quantities:

$$\left.\begin{aligned} V_{la} - V_{ra} &= 0 \\ V_{lb} - V_{rb} &= ZI_b \\ V_{lc} - V_{rc} &= ZI_c \end{aligned}\right\} \quad (8a)$$

Relations between two-phase components:

$$\left.\begin{aligned} V_{lx} - V_{rx} &= -(V_{lz} - V_{rz}) \\ &= (1/3)\,Z(I_x - I_z) \\ V_{ly} - V_{ry} &= ZI_y \end{aligned}\right\} \quad (8b)$$

For an equivalent circuit use figure 2, omitting transformers r_1 and r_3, and putting $r_2 = -1$, $r_2Z_{xz} = (^1/_3)Z$, $Z_{xy} = Z_{yz} = 0$, $H = K = 0$, and $J = Z$.

9. EQUAL SERIES IMPEDANCES IN TWO WIRES (b AND c) AND A DIFFERENT IMPEDANCE IN THE THIRD WIRE (a)

Relations between phase quantities:

$$\left.\begin{aligned} V_{la} - V_{ra} &= Z_aI_a \\ V_{lb} - V_{rb} &= Z_bI_b \\ V_{lc} - V_{rc} &= Z_bI_c \end{aligned}\right\} \quad (9a)$$

Relations between two-phase components:

$$\left.\begin{aligned} V_{lx} - V_{rx} &= (1/3)(2Z_a + Z_b)I_x + \\ &\qquad (1/3)(Z_a - Z_b)I_z \\ V_{ly} - V_{ry} &= Z_bI_y \\ V_{lz} - V_{rz} &= (1/3)(Z_a - Z_b)I_x + \\ &\qquad (1/6)(Z_z + 2Z_b)I_z \end{aligned}\right\} \quad (9b)$$

For an equivalent circuit use figure 2, omitting transformers r_1 and r_3, and putting $Z_{xy} = Z_{yz} = 0$ and $J = Z_b$. If $Z_b > Z_a$, put $r_2 = -1$, $r_2Z_{xz} = (^1/_3)\,(Z_b - Z_a)$, $H = Z_a$, and $K = {}^1/_2\, Z_a$. If $Z_b < Z_a$, put $r_2 = 2$, $r_2Z_{xz} = (^2/_3)\,(Z_a - Z_b)$, $H = Z_b$, and $K = {}^1/_2\, Z_b$.

10. EQUAL SERIES IMPEDANCES Z_s IN TWO WIRES (b AND c) AND MUTUAL IMPEDANCE Z_m BETWEEN THEM

Relations between phase quantities:

$$\left.\begin{aligned} V_{la} - V_{ra} &= 0 \\ V_{lb} - V_{rb} &= Z_sI_b + Z_mI_c \\ V_{lc} - V_{rc} &= Z_mI_b + Z_sI_c \end{aligned}\right\} \quad (10a)$$

Relations between two-phase components:

$$\left.\begin{aligned} V_{lx} - V_{rx} &= -(V_{lz} - V_{rz}) = \\ &\quad (1/3)(Z_s + Z_m)(I_x - I_z) \\ V_{ly} - V_{ry} &= (Z_s - Z_m)I_y \end{aligned}\right\} \quad (10b)$$

For an equivalent circuit use figure 2, omitting transformers r_1 and r_3, and putting $r_2 = -1$, $r_2Z_{xz} = (1/3)\,(Z_s + Z_m)$, $Z_{xy} = Z_{yz} = 0$, $H = K = 0$, and $J = Z_s - Z_m$.

Open Circuits

11. OPEN CIRCUIT IN ONE WIRE (a)

Relations between phase quantities:

$$\left.\begin{aligned} I_a &= 0 \\ V_{lb} - V_{rb} &= 0 \\ V_{lc} - V_{rc} &= 0 \end{aligned}\right\} \quad (11a)$$

Corresponding relations between two-phase components:

$$\left.\begin{aligned} I_x + {}^1/_2I_z &= 0 \\ V_{lx} - V_{rx} &= 2(V_{lz} - V_{rz}) \\ V_{ly} - V_{ry} &= 0 \end{aligned}\right\} \quad (11b)$$

For an equivalent circuit use figure 2, omitting transformers r_1 and r_3, and putting $r_2 = 2$, $Z_{xz} = \infty$, $Z_{xy} = Z_{yz} = 0$, and $H = J = K = 0$. The resulting circuit is like that portion of figure 10 between lines *AA* and *BB*, and consists of a coupling between the *x* and *z* networks through a 2:1 series transformer.

12. OPEN CIRCUIT IN TWO WIRES (b AND c)

Relations between phase quantities:

$$\left.\begin{aligned} V_{la} - V_{ra} &= 0 \\ I_b &= 0 \\ I_c &= 0 \end{aligned}\right\} \quad (12a)$$

Corresponding relations between two-phase components:

$$\left.\begin{aligned} (V_{lx} - V_{rx}) + (V_{lz} - V_{rz}) &= 0 \\ I_x &= I_z \\ I_y &= 0 \end{aligned}\right\} \quad (12b)$$

For an equivalent circuit use figure 2, omitting transformers r_1 and r_3, and putting $r_2 = -1$, $Z_{xz} = \infty$, $Z_{xy} = Z_{yz} = 0$, $H = K = 0$, and $J = \infty$ (that is, open network *y*).

13. OPEN CIRCUIT IN ALL THREE WIRES

Relations between phase quantities:

$$I_a = 0, \quad I_b = 0, \quad I_c = 0 \qquad (13a)$$

Corresponding relations between two-phase components:

$$I_x = 0, \quad I_y = 0, \quad I_z = 0 \qquad (13b)$$

This means that all three substitute networks are open at the point corresponding to the open circuit in the three-phase network.

Shunt Impedances

14. WYE-CONNECTED IMPEDANCES WITH NEUTRAL GROUNDED THROUGH IMPEDANCE Z_n

This combination of impedances may be considered as two circuit elements in series, first, a group of series impedances, with or without mutual impedances, such as have been considered in cases 1 to 10 inclusive, and, second, the element shown in figure 3. The equivalent circuit will then consist of a series combination of the elements representing the two portions. The circuits for the first portion have already been given; that for the second portion (figure 3) will now be developed. The relations between phase quantities for this portion are:

$$V_a = V_b = V_c = Z_n(I_a + I_b + I_c) \quad (14a)$$

The corresponding relations between two-phase components are:

$$\left.\begin{aligned} V_x &= V_y = 0 \\ V_z &= (3/2)Z_nI_z \end{aligned}\right\} \quad (14b)$$

The equivalent circuit, shown in figure 2*b*, consists of short circuits on the *x* and *y* networks and a shunt impedance $(3/2)Z_n$ in the *z* network. Z_n may have any value, from zero in case of solidly grounded neutral to infinity (open circuit) in case of isolated neutral. This equivalent circuit may be connected to the circuit of figure 2*a* as shown by the dotted lines.

15. DELTA-CONNECTED IMPEDANCES

Delta-connected impedances (figure 4*a*) may be considered as consisting of two circuit elements in cascade: first, a delta connection, and, second, series impedances. The equivalent circuit for the first element will be developed; that for the second element is shown in figure 2. The relations between line and delta quantities are as follows in terms of phase quantities. Let subscript *l* stand for line and *d* for delta, and let each delta phase be named from the opposite line phase:

$$\left.\begin{aligned} V_{da} &= V_{lb} - V_{lc} \\ V_{db} &= V_{lc} - V_{la} \\ V_{dc} &= V_{la} - V_{lb} \end{aligned}\right\} \quad (15a)$$

$$\left.\begin{aligned} I_{la} &= I_{dc} - I_{db} \\ I_{lb} &= I_{da} - I_{dc} \\ I_{lc} &= I_{db} - I_{da} \end{aligned}\right\} \quad (15b)$$

The corresponding relations in terms of two-phase components are:

$$\left.\begin{aligned} V_{dx} &= -\sqrt{3}V_{ly} \\ V_{dy} &= \sqrt{3}V_{lx} \\ V_{dz} &= 0 \end{aligned}\right\} \quad (15c)$$

$$\left.\begin{aligned} I_{lx} &= \sqrt{3}I_{dy} \\ I_{ly} &= -\sqrt{3}I_{dx} \\ I_{lz} &= 0 \end{aligned}\right\} \quad (15d)$$

Equations 15*c* and 15*d* are satisfied by the connections of the equivalent circuit in the left-hand part of figure 4*b*, to which may be connected on the right hand the equivalent circuit of figure 2.

If it is not desired to preserve the identity of the delta currents and voltages, the circuit can be reduced to a simpler form. The transformers of ratio $1 : \sqrt{3}$ may be omitted if all impedances in the right-hand part of the circuit are divided by 3. If there is no objection to a conductive connection between the neutral terminals of the x and y networks, the circuit can be reduced finally to three impedances connected in wye or delta. In the next case a delta equivalent circuit is derived directly.

16. DELTA-CONNECTED ADMITTANCES

Let the delta-connected admittances be Y_{ab}, Y_{bc}, and Y_{ca}. The relations between phase currents and voltages are:

$$\left.\begin{aligned} I_a &= (V_a - V_b)Y_{ab} - (V_c - V_a)Y_{ca} \\ I_b &= (V_b - V_c)Y_{bc} - (V_a - V_b)Y_{ab} \\ I_c &= (V_c - V_a)Y_{ca} - (V_b - V_c)Y_{bc} \end{aligned}\right\} \quad (16a)$$

The corresponding relations between two-phase components are:

$$\left.\begin{aligned} I_x &= (3/2)(Y_{ca} + Y_{ab})V_x + \\ &\qquad (\sqrt{3}/2)\ (Y_{ab} - Y_{ca})V_y \\ I_y &= (\sqrt{3}/2)(Y_{ab} - Y_{ca})V_x + \\ &\qquad (2Y_{bc} + {}^1/_2Y_{ca} + {}^1/_2Y_{ab})V_y \\ I_z &= 0 \end{aligned}\right\} \quad (16b)$$

An equivalent circuit is given in figure 5. A, B, and C are admittances whose values are:

$$\left.\begin{aligned} A &= Y_{ab}\,(3 - \sqrt{3}r)/2 + \\ &\qquad Y_{ca}\,(3 + \sqrt{3}r)/2 \\ B &= 2Y_{bc} + Y_{ab}(1 - \sqrt{3}/r)/2 + \\ &\qquad Y_{ca}(1 + \sqrt{3}/r)/2 \\ C &= (\sqrt{3}/2r)(Y_{ab} - Y_{ca}) \end{aligned}\right\} \quad (16c)$$

If $Y_{ab} > Y_{ca}$, put $r = 1$; if $Y_{ab} < Y_{ca}$, put $r = -1$.

Note that for symmetry with respect to phase a, $Y_{ab} = Y_{ca}$, and $C = 0$ (open circuit), which makes the x and y networks independent.

The results of this case may be applied to impedances connected in Y with isolated neutral, by first changing the Y to an equivalent delta.

17. SINGLE IMPEDANCE Z CONNECTED LINE TO LINE

Relations between phase quantities:

$$I_a = 0,\quad I_b + I_c = 0,\quad V_b - V_c = ZI_b \qquad (17a)$$

Corresponding relations between two-phase components:

$$I_x = 0,\quad I_z = 0,\quad V_y = {}^1/_2ZI_y \qquad (17b)$$

The equivalent circuit consists of impedance $Z/2$ in shunt with network y.

18. SINGLE IMPEDANCE Z CONNECTED LINE TO NEUTRAL

Relations between phase quantities:

$$V_a = ZI_a,\quad I_b = 0,\quad I_c = 0 \qquad (18a)$$

Corresponding relations between two-phase components:

$$V_x + V_z = (3/2)ZI_x,\quad I_x = I_z,\quad I_y = 0 \qquad (18b)$$

An equivalent circuit is shown in figure 6.

Short Circuits Symmetrical With Respect to Phase a

19. LINE-TO-GROUND SHORT CIRCUIT ON PHASE a

Relations between phase quantities:

$$V_a = 0,\quad I_b = 0,\quad I_c = 0 \qquad (19a)$$

Corresponding relations between two-phase components:

$$V_x + V_z = 0,\quad I_x = I_z,\quad I_y = 0 \qquad (19b)$$

Equivalent circuit, figure 7*a*.

20. LINE-TO-LINE SHORT CIRCUIT ON PHASES b AND c

Relations between phase quantities:

$$I_a = 0,\quad I_b + I_c = 0,\quad V_b = V_c \qquad (20a)$$

Corresponding relations between two-phase components:

$$I_z = 0,\quad I_x = 0,\quad V_y = 0 \qquad (20b)$$

Equivalent circuit, figure 7*b*.

21. TWO-LINE-TO-GROUND SHORT CIRCUIT ON PHASES b AND c

Relations between phase quantities:

$$I_a = 0,\quad V_b = 0,\quad V_c = 0 \qquad (21a)$$

Corresponding relations between two-phase components:

$$I_x + {}^1/_2I_z = 0,\quad V_x = 2V_z,\quad V_y = 0 \qquad (21b)$$

Equivalent circuit, figure 7*c*.

22. THREE-PHASE SHORT CIRCUIT NOT INVOLVING GROUND

Relations between phase quantities:

$$I_a + I_b + I_c = 0,\quad V_a = V_b = V_c \qquad (22a)$$

Corresponding relations between two-phase components:

$$I_z = 0,\quad V_x = 0,\quad V_y = 0 \qquad (22b)$$

Equivalent circuit, figure 7*d*.

23. THREE-PHASE SHORT CIRCUIT INVOLVING GROUND

Relations between phase quantities:

$$V_a = V_b = V_c = 0 \qquad (23a)$$

Corresponding relations between two-phase components:

$$V_x = V_y = V_z = 0 \qquad (23b)$$

Equivalent circuit, figure 7*e*.

Short Circuits Not Symmetrical With Respect to Phase a

These are included to show that two-phase components can be used in problems of simultaneous faults. Only faults symmetrical with respect to phase b are treated in detail. For faults symmetrical with respect to phase c, the equations in two-phase components differ only in changed algebraic sign of I_y and V_y; the equivalent circuits differ only in reversed polarity of the y network.

24. LINE-TO-GROUND SHORT CIRCUIT ON PHASE b

Relations between phase quantities:

$$I_a = 0,\quad I_c = 0,\quad V_b = 0 \qquad (24a)$$

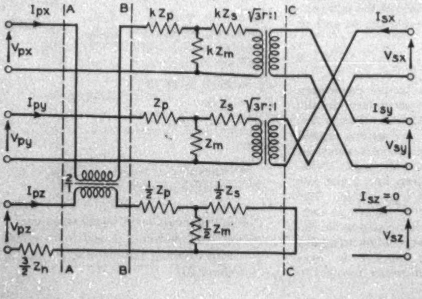


Figure 10. Equivalent circuit for wye-delta or for open wye-open delta (reversed vee-vee) transformer bank

For wye-delta omit portion between *AA* and *BB*, and put $k = 1$. For open wye-open delta omit that portion of network z between *BB* and *CC*, and put $k = 3$. Connections shown are for positive-sequence voltages on delta side 90 degrees behind those on wye side

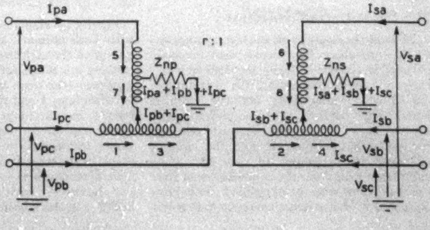


Figure 11. Tee-tee transformer bank with grounded neutrals on both windings

Corresponding relations between two-phase components:

$$\left.\begin{array}{l} I_x = I_y/\sqrt{3} = -{}^1/_2 I_z \\ V_x + \sqrt{3}V_y - 2V_z = 0 \end{array}\right\} \quad (24b)$$

Equivalent circuit, figure 8*a*.

25. Line-to-Line
Short Circuit on Phases a and c

Relations between phase quantities:

$$I_b = 0, \quad I_a + I_c = 0, \quad V_a = V_c \qquad (25a)$$

Corresponding relations between two-phase components:

$$I_z = 0, \quad I_x + \sqrt{3}I_y = 0, \quad V_x = V_y/\sqrt{3} \qquad (25b)$$

Equivalent circuit, figure 8*b*.

26. Two-Line-to-Ground Short Circuit on Phases a and c

Relations between phase quantities:

$$I_b = 0, \quad V_c = 0, \quad V_a = 0 \qquad (26a)$$

Corresponding relations between two-phase components:

$$\left.\begin{array}{l} I_x + \sqrt{3}I_y - I_z = 0 \\ V_x = V_y/\sqrt{3} = -V_z \end{array}\right\} \quad (26b)$$

Equivalent circuit, figure 8*c*.

Transformer Banks

27. Wye-Wye Connection

Each transformer, all three of which are assumed to be identical, is represented by an equivalent *T* circuit in cascade with an ideal transformer of the same ratio (r:1) as that of the actual transformer. The *T* circuit consists of the following impedances, all referred to the primary side: Z_p, the primary leakage impedance, Z_s, the secondary leakage impedance, and Z_m, the exciting impedance. Since the three transformers constitute a balanced circuit, their representation in the substitute networks is the same as the representation of a single transformer, except that in the *z* network the impedances have one-half their true values. The neutral impedances, Z_{np} and Z_{ns}, appear in the *z* network multiplied by 3/2, just as in case 14 (wye-connected impedances). The complete equivalent circuit for the bank and neutral resistors is given in figure 9 with $k_x = k_y = 1$ and the *z* network like that marked "*z*1." The ideal transformers, marked "r:1," may be omitted if secondary currents and voltages are referred to the primary side, and in many cases the shunt branches Z_m may be omitted also; then the equivalent circuit consists simply of a series impedance in each substitute network.

28. Delta-Delta Connection

Most of the remarks on the last case apply to this one also. The circuit may be considered to consist of three portions in cascade: (1) delta connection, (2) transformers, (3) delta connection; and the equivalent circuit consists of three corresponding portions. The two portions representing the delta connections are like the circuit of figure 4*b*, developed in case 15. The circuit resulting from the combination of the three portions may be simplified, with loss of identity of the delta currents and voltages, by replacing the 1:$\sqrt{3}$ transformers of figure 4 by direct connections and dividing the transformer impedances, expressed in ohms, by 3. (If expressed in per unit, they are not changed.) The equivalent circuit is then that of figure 9 with $k_x = k_y = 1/3$, and with the *z* network like that marked "*z*2."

29. Wye-Delta Connection

The equivalent circuit (figure 10) is built up of portions similar to those found in the last two cases. It is worthy of note that the *x* network on the primary side is connected to the *y* network on the secondary side, and the *y* network on the primary side is connected to the *x* network on the secondary side. Furthermore, a reversal of polarity occurs in one of the two connections (shown in the *py*-*sx* connection for transformer connections giving voltages of the delta side 90 degrees behind the voltages of the wye side).

30. Open Delta or Vee-Vee Connection

The open delta transformer bank may be considered to consist of the following portions in cascade: (1) delta connection, (2) open circuit in phase *a*, (3) transformers, (4) open circuit in phase *a*, and (5) delta connection. The equivalent circuit may be built up from corresponding portions, using figure 4 for the delta connections and that portion of figure 10 between lines *AA* and *BB* for the open circuits. The resulting circuit is then simplified to the circuit of figure 9 by two steps: (1) eliminate the 2:1 transformers representing the open circuits by multiplying the transformer impedances in the *z* network by 4 and adding them to the corresponding *x* impedances; (2) replace the $\sqrt{3}$:1 transformers by direct connections and divide the transformer impedances by 3. The equivalent circuit then becomes that of figure 9 with $k_x = 1/3$ and $k_y = 1$. Note that the effect of removing one transformer from a delta-delta bank is to increase the *y* impedances threefold.

31. Open Wye-Open Delta or Reversed Vee-Vee Connection

The equivalent circuit for this case (figure 10) may be derived from that for a wye-delta bank, just as that for an open delta bank was derived from that for a delta-delta bank, by inserting an open circuit in phase *a* on each side of the transformers. The effect of thus removing one transformer is to increase the impedance of the *px*-*sy* circuit threefold and to couple the *px* and *pz* networks.

32. Tee-Tee Connection

Figure 11 shows the tee-tee connection with both primary and secondary circuits grounded through impedance. Two transformers are used, each having two tapped windings—the main transformer, connected between lines *b* and *c*, and the teaser transformer, connected between line *a* and the center tap of the main transformer. The teaser transformer may be tapped at two-thirds of the turns from line *a* to provide the neutral connections. In the following analysis, however, exciting current is neglected, and the transformers are assumed to be ungrounded. The main transformer must be treated as a four-circuit transformer to find the impedance drops. Let the portions of the windings be numbered from 1 to 4 as shown in figure 11, the odd numbers being assigned to the primary and the even numbers to the secondary side. The impedance drop between any two windings, *i* and *j*, of an *n*-circuit transformer, neglecting exciting current, is given by

$$V_i - V_j = -{}^1/_2\sum_{k=1}^{n}(Z_{ik} - Z_{jk})I_k \qquad (32a)$$

where V_i and V_j are the terminal voltages of windings *i* and *j*, respectively, I_k the current in winding *k*, Z_{ik} the short-circuit or equivalent impedance of windings *i* and *k*, etc., all quantities being referred to the same winding. $Z_{ii} = Z_{jj} = 0$. Applying formula 32*a* to the main transformer, assuming for convenience that the two center taps are at the same potential, and not referring all quantities to the same side, one obtains:

$$V_{pb} - rV_{sb} = -{}^1/_2[-Z_{12}I_{pb} + Z_{12}I_{sb}/r + (Z_{13} - Z_{32})(-I_{pc}) + (Z_{14} - r^2Z_{24})(-I_{sc}/r)] \qquad (32b)$$

$$V_{pc} - rV_{sc} = -{}^1/_2[-Z_{34}I_{pc} + Z_{34}I_{sc}/r + (Z_{13} - Z_{14})(-I_{pb}) + (Z_{32} - r^2Z_{24})(-I_{sb}/r)] \qquad (32c)$$

Regarding the teaser transformer as a two-circuit transformer of impedance *T* (referred to primary),

$$V_{pa} - rV_{sa} = TI_{pa} \qquad (32d)$$

The following current equations are obtained by summing the ampere-turns on each core:

$$rI_{pb} + I_{sb} - rI_{pc} - I_{sc} = 0 \qquad (32e)$$

$$rI_{pa} + I_{sa} = 0 \qquad (32f)$$

and the following are obtained from Kirchhoff's current law:

$$I_{pa} + I_{pb} + I_{pc} = 0 \qquad (32g)$$

$$I_{sa} + I_{sb} + I_{sc} = 0 \qquad (32h)$$

The following equations in two-phase components are equivalent to equations 32*b* to 32*h* in phase quantities:

$$\left.\begin{array}{l} rI_{px} + I_{sx} = 0 \\ rI_{py} + I_{sy} = 0 \end{array}\right\} \quad (32i)$$

$$\left.\begin{array}{l} I_{pz} = 0 \\ I_{sz} = 0 \end{array}\right\} \quad (32j)$$

$$\left.\begin{array}{l} V_{px} - rV_{sx} = Z_xI_{px} + Z_{xy}I_{py} \\ V_{py} - rV_{sy} = Z_{xy}I_{px} + Z_yI_{py} \end{array}\right\} \quad (32k)$$

where

$Z_x = 2T/3 + M'/6$
$Z_y = M/2$
$Z_{xy} = (Z_{12} - Z_{34})/2\sqrt{3}$

where

T = impedance of teaser transformer, referred to primary side
$M = Z_{12} + Z_{34} + Z_{32} + Z_{14} - Z_{13} - r^2Z_{24}$
= impedance of main transformer measured on windings 1 and 3 in series with windings 2 and 4 in series short-circuited

$M' = Z_{12} + Z_{34} + Z_{13} + r^2Z_{24} - Z_{23} - Z_{14}$ = impedance of main transformer measured on winding 1 in series with 3 reversed, when winding 2 in series with winding 4 reversed is short-circuited

An equivalent circuit is given in figure 12. Note that with symmetrical construction of the main transformer, $Z_{xy} = 0$, resulting in simplification of the equivalent circuit.

If both neutrals are grounded, z current can flow to ground freely from either side. All four circuits (x, y, pz, and sz) are coupled together through different mutual impedances, making the equivalent circuit very complicated.

33. Scott Connection

The discussion is limited to the case of isolated neutrals. Let the currents and voltages on the two-phase (secondary) side be denoted by subscripts sx and sy; no substitutions need be made for them. Let the ratios of the main and teaser transformers be denoted by r_m and r_t, respectively; ordinarily $r_t = (\sqrt{3}/2)r_m$. Let the windings of the main transformer be denoted as follows: 1 and 2, the halves of the primary winding connected to lines c and b, respectively; 3, the secondary winding. Exciting current is neglected.

The equations in phase currents and voltages are:

$$\left.\begin{aligned}
&I_{pa} + I_{pb} + I_{pc} = 0\\
&r_tI_{pa} + I_{sx} = 0\\
&r_m(I_{pc} - I_{pb}) + 2I_{sy} = 0\\
&V_{pa} - r_tV_{sx} = TI_{pa}\\
&V_{pb} + V_{pc} = -{}^1/_2[-Z_{12}(I_{pb}+I_{pc}) +\\
&\qquad (Z_{13} - Z_{23})I_{sy}\,2/r_m]\\
&(V_{pc} - V_{pb}) - r_mV_{sy} = -{}^1/_2\times\\
&\quad[(Z_{12} - 2Z_{13})I_{pc} + (Z_{12} - 2Z_{23})\times\\
&\qquad(-I_{pb}) + (Z_{13} + Z_{23})I_{sy}\,2/r_m]
\end{aligned}\right\}\quad(33a)$$

The last two equations are obtained from 32*a*.

The corresponding equations in two-phase components are:

$$\left.\begin{aligned}
&I_{pz} = 0\\
&r_tI_{px} + I_{sx} = 0\\
&(\sqrt{3}/2)r_mI_{py} + I_{sy} = 0\\
&V_{px} - (2/3)r_tV_{sx} = Z_xI_{px} + Z_{xy}I_{py}\\
&V_{py} - (r_m/\sqrt{3})\,V_{sy} = Z_{xy}I_{px} + Z_yI_{py}
\end{aligned}\right\}\quad(33b)$$

where

$$\left.\begin{aligned}
Z_x &= 2T/3 + Z_{12}/6\\
Z_y &= M/2\\
Z_{xy} &= (Z_{23} - Z_{13})/2\sqrt{3}
\end{aligned}\right\}\quad(33c)$$

M = short-circuit impedance of main transformer measured from primary side (that is, measured on windings 1 and 2 in series with winding 3 short-circuited), ohms

T = short-circuit impedance of teaser transformer measured from primary side

Z_{23} = impedance measured on winding 2 with winding 3 short-circuited, ohms; etc.

An equivalent circuit is given in figure 12. Note that the secondary terminal voltages in this circuit are equal to two-thirds the actual secondary voltages. This is explainable by reference to equation 13 for power in terms of two-phase components.

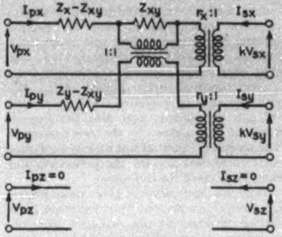


Figure 12. Equivalent circuit of ungrounded tee-tee or Scott transformer bank, exciting current neglected

For tee-tee, put $k = 1$, $r_x = r_y = r$. For Scott, put $k = 2/3$, $r_x = r_t$, $r_y = 0.866\,r_m$

Untransposed Lines

34. Line Without Ground Wires

Let

R_a, R_b, R_c = resistances of wires a, b, c per unit length, ohms per meter

D_{aa}, D_{bb}, D_{cc} = geometric mean radii of wires a, b, c (0.779 times actual radius for a round wire)

D_{ab}, D_{bc}, D_{ca} = geometric mean distances between wires (center-to-center distances for round wires). The D's may be in any units provided that all are in the same units

λ_a, λ_b, λ_c = magnetic flux linkages per unit length of wires a, b, c, webers per meter

μ_0 = permeability of air = 10^{-7} henry per meter (unrationalized meter-kilogram-second units)

f = frequency, cycles per second

$\omega = 2\pi f$

V = voltage rise per unit length along line, volts per meter

I = line current, amperes

Z = impedance per unit length of line, ohms per meter

Subscripts a, b, c, x, y, z are used with V, I, and Z as heretofore

ρ = earth resistivity, ohm-meters

The relations between phase currents and voltages which hold if $I_z = 0$ are:

$$\left.\begin{aligned}
V_a &= R_aI_a + j\omega\lambda_a\\
V_b &= R_bI_b + j\omega\lambda_b\\
V_c &= R_cI_c + j\omega\lambda_c
\end{aligned}\right\}\quad(34a)$$

where

$$\left.\begin{aligned}
\lambda_a &= -2\mu_0\,(I_a\log_e D_{aa} +\\
&\qquad I_b\log_e D_{ab} + I_c\log_e D_{ac})\\
\lambda_b &= -2\mu_0\,(I_a\log_e D_{ab} +\\
&\qquad I_b\log_e D_{bb} + I_c\log_e D_{bc})\\
\lambda_c &= -2\mu_0\,(I_a\log_e D_{ac} +\\
&\qquad I_b\log_e D_{bc} + I_c\log_e D_{cc})
\end{aligned}\right\}\quad(34b)$$

Now assume current component I_x to flow alone, that is, assume

$$I_a = I_x,\quad I_b = I_c = -{}^1/_2I_x\qquad(34c)$$

Substitute equations 34*b* into 34*a*, 34*c* into the resulting equations, and the latter into equations 2. The division of the resulting expressions for V_x, V_y, and V_z by I_x gives Z_{xx}, Z_{xy}, and Z_{xz}, respectively. Next assume current component I_y to flow alone, that is, assume

$$I_a = 0,\quad I_c = -I_b = (\sqrt{3}/2)I_y\qquad(34d)$$

and by a similar procedure obtain Z_{yy} and Z_{yz}. This leaves only Z_{zz}, which may be found from the knowledge that it is one-half the zero-sequence impedance, Z_{00}. The impedances thus found are:

$$\left.\begin{aligned}
Z_{xx} &= (2/3)R_a + (1/6)(R_b + R_c) +\\
&\quad j\omega(1/3)\mu_0\log_e\frac{D_{ab}{}^4D_{ac}{}^4}{D_{aa}{}^4D_{bb}D_{cc}D_{bc}{}^2}\\
Z_{xy} &= (1/2\sqrt{3})(R_b - R_c) +\\
&\quad j\omega\,(1/\sqrt{3})\mu_0\times\\
&\qquad\log_e(D_{ab}{}^2D_{cc}/D_{ac}{}^2D_{bb})\\
Z_{xz} &= (1/3)R_a - (1/6)(R_b + R_c) +\\
&\quad j\omega(1/3)\mu_0\times\\
&\qquad\log_e(D_{bb}D_{cc}D_{bc}{}^2/D_{aa}{}^2D_{ab}D_{ac})\\
Z_{yy} &= {}^1/_2(R_b + R_c) +\\
&\qquad j\omega\mu_0\log_e(D_{bc}{}^2/D_{bb}D_{cc})\\
Z_{yz} &= (1/2\sqrt{3})(R_c - R_b) +\\
&\quad j\omega\,(1/\sqrt{3})\mu_0\times\\
&\qquad\log_e(D_{ab}D_{bb}/D_{ac}D_{cc})\\
Z_{zz} &= (1/6)(R_a + R_b + R_c) +\\
&\quad(3/2)R_g + j\omega3\mu_0\log_e(H/A)
\end{aligned}\right\}\quad(34e)$$

where

$R_g = 0.99 \times 10^{-6}f$ ohms per meter

$H = 2{,}160\sqrt{\rho/f}$ feet

$A = (D_{aa}D_{bb}D_{cc}D_{ab}{}^2D_{bc}{}^2D_{ac}{}^2)^{1/9}$ feet

The impedances of equations 34*e* may be used in equations 1*c* and in the equivalent circuit of figure 2. Note that if the line is symmetrical with respect to wire a, that is, if spacing $D_{ab} = D_{ac}$ and if wires b and c are alike ($R_b = R_c$ and $D_{bb} = D_{cc}$), then $Z_{xy} = 0$ and $Z_{yz} = 0$, making the y network independent.

35. Symmetrical Line
With Two Ground Wires

The discussion is confined to the case in which both the line wires and the ground wires are symmetrical with respect to a plane through wire a. With such a configuration, both I_x and I_z, especially the latter, induce in the ground wires currents which are equal in the two wires and which return in the ground. I_y, on the other hand, induces a circulating current in the ground wires. The currents in the ground wires can be computed by established methods, after which the impedances can be calculated in the same general way as in the last case.

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Discussion

A. Boyajian (General Electric Company, Pittsfield, Mass.): I wish to make some comments as to title, method of derivation, and final results.

TITLE

The co-ordinates discussed by Professor Kimbark are not true two-phase quantities, and there is only one logical name for them: Clarke co-ordinates or Clarke components. Certain types of co-ordinates are called Cartesian co-ordinates after Descartes who first formulated them. I frequently call symmetrical co-ordinates, Fortescue co-ordinates. The system discussed by Professor Kimbark may have been used in some fragmentary form by others, but Miss Edith Clarke being the first person to systematize it, it is fitting that we name the system after her—Clarke co-ordinates.

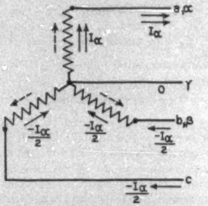

Figure 1. Illustrating the flow of I_α

Solid arrows represent the actual currents; dotted arrows the positive directions of the three-phase system

$$i_a' = I_\alpha$$
$$i_b' = -I_\alpha/2$$
$$i_c' = -I_\alpha/2$$

DERIVATION AND RESULTS

A transformation which replaces a given system of circuits by another, presumably more convenient, equivalent system of circuits, should be subjected to two tests: (*a*) physical interpretation, and (*b*) mathematical consistency. The first is essential because engineers are visual-minded, and they cannot use a system unless they have a clear physical understanding of it. We must be able to visualize the current and voltage components and also the impedances of the system to the new currents. The second is essential not merely to assure correctness, but also for the perfection and simplification of the technique.

Professor Kimbark has given some physical interpretations, but they have not been carried through to the voltages and impedances, so that neither he nor the average reader is likely to be aware of lack of fit at some points of the structure. The voltage transformation equations must have been obtained either by trial and error or by intuition, because they are not the inverse of the current transformation equations; and, as a consequence, his power equation 13 has to have an arbitrary correction factor of 3/2 applied to it.

Mathematical consistency demands for instance, that, while the transformation of the currents may be assumed arbitrarily, this having been done, the transformation of the voltages may not be assumed arbitrarily, but has to be the tensorial inverse of the first, for which we have a standardized routine.

I wish to develop the Clarke transformation for a triple purpose: (*a*) to obtain a consistent system, (*b*) to show clearly its physical significance, and (*c*) to utilize the present case to illustrate a typical procedure in developing any equivalent system and the many benefits of such procedure. I shall use Miss Clarke's notation: Greek letters for the new quantities, English letters for the customary three-phase quantities.

I_α

Figure 1 of this discussion illustrates positive I_α: as pointed out both by Miss Clarke and Professor Kimbark, this is a current which flows in phase *a* and returns equally through the other two. The algebraic signs are determined by assuming the positive directions in the three-phase system as from neutral outward. Thus, a positive I_α produces a positive component of current (i_a') in phase *a*, and negative half currents in the other two phases:

$$i_a' = I_\alpha, \quad i_b' = -I_\alpha/2, \quad i_c' = -I_\alpha/2 \quad (1)$$

Figure 1 defines also the impedance of the system to I_α.

I_β

Figure 2 illustrates a positive I_β: it is a current flowing between phases *b* and *c* only, in the positive sense of phase *b*. The three-phase currents corresponding to I_β are

$$i_a'' = 0, \quad i_b'' = I_\beta, \quad i_c'' = -I_\beta \quad (2)$$

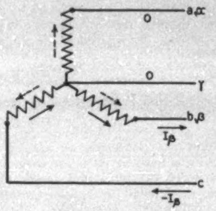

Figure 2. Illustrating the flow of I_β

Solid arrows represent the actual currents; dotted arrows the positive directions of the three-phase system

$$i_a'' = 0$$
$$i_b'' = I_\beta$$
$$i_c'' = -I_\beta$$

Figure 2 defines also the impedance of the system to I_β.

I_γ

Figure 3 illustrates a positive I_γ: it is the current in the neutral and provides equal positive currents of one-third value in all three lines. Thus, the three-phase currents corresponding to I_γ are

$$i_a''' = I_\gamma/3, \quad i_b''' = I_\gamma/3, \quad i_c''' = I_\gamma/3 \quad (3)$$

Table I. Transformation Matrices

	For Currents	For Voltages
Clarke	$A_m^\mu = \begin{array}{c\|c\|c} 2/3 & -1/3 & -1/3 \\ 0 & \frac{1}{\sqrt{3}} & \frac{-1}{\sqrt{3}} \\ 1/3 & 1/3 & 1/3 \end{array}$	$A_\mu^m = \begin{array}{c\|c\|c} 2/3 & -1/3 & -1/3 \\ 0 & \frac{1}{\sqrt{3}} & \frac{-1}{\sqrt{3}} \\ 1/3 & 1/3 & 1/3 \end{array}$
Kimbark	$A_m^\mu = \begin{array}{c\|c\|c} 2/3 & -1/3 & -1/3 \\ 0 & \frac{-1}{\sqrt{3}} & \frac{1}{\sqrt{3}} \\ 2/3 & 2/3 & 2/3 \end{array}$	$A_\mu^m = \begin{array}{c\|c\|c} 2/3 & -1/3 & -1/3 \\ 0 & \frac{-1}{\sqrt{3}} & \frac{1}{\sqrt{3}} \\ 1/3 & 1/3 & 1/3 \end{array}$
Boyajian	$A_m^\mu = \begin{array}{c\|c\|c} 2/3 & -1/3 & -1/3 \\ 0 & 1/2 & -1/2 \\ 1 & 1 & 1 \end{array}$	$A_\mu^m = \begin{array}{c\|c\|c} 1 & -1/2 & -1/2 \\ 0 & 1 & -1 \\ 1/3 & 1/3 & 1/3 \end{array}$

Figure 3 defines also the impedance of the system for I_γ.

Attention may be called to the fact that I_γ is the current in the *neutral;* and, therefore, the neutral wire is one of the circuits of the new equivalent system, not of the old three-phase system. In the three-phase system, the neutral lead is merely a return wire, not an independent circuit. To emphasize this point, let us note that each system has three independent circuits: the circuits of the three-phase system are a, b, c; while those of the equivalent Clarke system are α, β, γ. The terminals a and α coincide, b and β coincide, but c and γ are separate. Terminal c represents an independent circuit of the three-phase system, but only a return conductor of the Clarke system (of circuit β). It will be seen also that I_γ is not strictly the old zero-sequence current but one equal to three times the old zero-sequence current. If we were to define our I_γ as Fortescue's I_0, we would lose the identity—the nice physical picture—of circuit γ, besides having to define V_γ as $3V_0$ and thereby losing also the nice physical picture of V_γ.

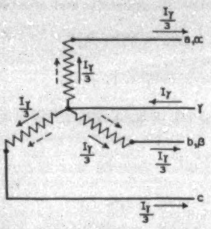

Figure 3. Illustrating the flow of I_γ

Solid arrows represent the actual currents; dotted arrows the positive directions of the three-phase system

$$i_a''' = I_\gamma/3$$
$$i_b''' = I_\gamma/3$$
$$i_c''' = I_\gamma/3$$

RELATIONSHIP OF RESULTANT CURRENTS

The total three-phase current, I_a, in phase a is of course the sum of all three of its Clarke components, i_a', i_a'', and i_a''', from equations 1, 2, and 3. The same comment applies to I_a and I_c; hence,

$$\left.\begin{aligned} I_a &= I_\alpha + 0I_\beta + (1/3)I_\gamma \\ I_b &= (-1/2)I_\alpha + I_\beta + (1/3)I_\gamma \\ I_c &= (-1/2)I_\alpha - I_\beta + (1/3)I_\gamma \end{aligned}\right\} \quad (4)$$

The matrix of the transformation from Clarke co-ordinates to three-phase co-ordinates may be copied from the coefficients of (4), and may be designated (in tensor notation) as A_μ^m:

$$A_\mu{}^m = \begin{array}{c|c|c|c} {}^{m}\diagdown{}_{\mu} & \alpha & \beta & \gamma \\ \hline a & 1 & 0 & 1/3 \\ \hline b & -1/2 & 1 & 1/3 \\ \hline c & -1/2 & -1 & 1/3 \end{array} \quad (5a)$$

CLARKE VOLTAGES IN TERMS OF THREE-PHASE VOLTAGES

Equations 1, 2, and 3 are arbitrary; but once they are accepted, every bit of the rest of the work will follow as a mathematical necessity. Those who are familiar with the tensor point of view, know that equation 4, hence matrix (5*a*) transposed, (that is, rows and columns interchanged)

$$A_\mu{}^m = \begin{array}{c|c|c|c} {}^{\mu}\diagdown{}^{m} & a & b & c \\ \hline \alpha & 1 & -1/2 & -1/2 \\ \hline \beta & 0 & 1 & -1 \\ \hline \gamma & 1/3 & 1/3 & 1/3 \end{array} \quad (5b)$$

defines the inverse transformation of the voltages, that is, from the three-phase system to the Clarke system; and thus we may write, out of mathematical necessity and hence with entire confidence,

$$\left.\begin{aligned} V_\alpha &= V_a - (1/2)V_b - (1/2)V_c \\ V_\beta &= 0 + V_b - V_c \\ V_\gamma &= (1/3)V_a + (1/3)V_b + (1/3)V_c \end{aligned}\right\} \quad (6)$$

PHYSICAL INTERPRETATION OF CLARKE VOLTAGES

The new voltage components, as defined by equation 6, are shown in figure 4.

V_β is simply the line voltage between b and c.

V_α is the three-phase system voltage parallel to I_α, that is, the vector from the vertex a to the midpoint of the line voltage b-c.

Finally, V_γ is what we have always known as the neutral shift, or, the zero-sequence voltage.

CLARKE CO-ORDINATES OF CURRENTS IN TERMS OF THREE-PHASE CO-ORDINATES OF CURRENTS

Equation 4 gave us the three-phase currents in terms of the Clarke currents; so, if we wish to obtain the Clarke currents in terms of the three-phase currents, we invert* matrix 5*b*, and obtain

$$A_\mu{}^m = \begin{array}{c|c|c|c} {}^{\mu}\diagdown{}^{m} & a & b & c \\ \hline \alpha & 2/3 & -1/3 & -1/3 \\ \hline \beta & 0 & 1/2 & -1/2 \\ \hline \gamma & 1 & 1 & 1 \end{array} \quad (7)$$

Matrix 7 means that the new current equations will be

$$\left.\begin{aligned} I_\alpha &= (2/3)I_a - (1/3)I_b - (1/3)I_c \\ I_\beta &= 0 + (1/2)I_b - (1/2)I_c \\ I_\gamma &= I_a + I_b + I_c \end{aligned}\right\} \quad (8)$$

COMPARISON OF RESULTS

The transformation matrices used by Miss Clarke, Professor Kimbark, and the writer are exhibited in table I of this discussion for easy comparison.

Some differences are to be seen easily. That however does not mean that one or another of these matrices must be wrong, but rather that in one or another the equivalence is not *complete,* even though, undoubtedly, the various applications that have been made are all valid.

The Clarke matrices have the distinction of being identical for currents and voltages. This is secured, however, at the expense of the vector power equation, because the current and voltage transformation matrices are not the tensorial inverse of each other. For the same reason, it may also be suspected that the Clarke impedances cannot be obtained by routine methods: that each problem has to be worked out from the ground up.

Kimbark matrices are very similar to the Clarke matrices except that the current and voltage transformation matrices now are not alike in the bottom row. They need not be alike, of course, even though it would be very nice if they turned out to be alike. Evidently, Professor Kimbark encountered some complication in using the Clarke matrices, and in his search for a way out arrived at his results, not stated how. But, even though the current and voltage transformation matrices have been rendered dissimilar, still they are not the inverse of each other, and all difficulty is not avoided: his power equation (13) needs a correction factor of 3/2, and the physical interpretations of the voltage and impedance components are lacking.

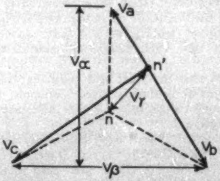

Figure 4. Illustrating V_α, V_β, V_γ

Three things may be said in favor of the modifications made by the writer:

(*a*). The voltages have as simple physical interpretations as the currents.

(*b*). The two matrices are the inverse of each other and therefore the vector power equation is what it ought to be, namely, the sum of the products of the currents with the conjugates of the corresponding voltages:

$$\begin{aligned} P+jQ &= I_\alpha \operatorname{conj} V_\alpha + I_\beta \operatorname{conj} V_\beta + I_\gamma \operatorname{conj} V_\gamma \\ &= I_a \operatorname{conj} V_a + I_b \operatorname{conj} V_b + I_c \operatorname{conj} V_c \end{aligned} \quad (9)$$

(*c*). All of the formulas developed for various problems in any system of components can be transformed to the present system with the help of a single appropriate transformation matrix and its inverse. For instance, all of the formulas of the Fortescue system, for the currents, voltages, and impedances of any problem, can be transformed to the present system with the help of the following single matrix (and its inverse), in which $m = 1, 2, 0$ refer to the Fortescue components; and $\mu = \alpha, \beta, \gamma$, to the Clarke components:

$$A_\mu{}^m = \begin{array}{c|c|c|c} {}^{\mu}\diagdown{}^{m} & 1 & 2 & 0 \\ \hline \alpha & 1 & 1 & 1 \\ \hline \beta & -\dfrac{1+\sqrt{3}j}{2} & \dfrac{1+\sqrt{3}j}{2} & 1 \\ \hline \gamma & 0 & 0 & 3 \end{array} \quad (10)$$

The statements by Miss Clarke and Professor Kimbark that the impedances of the new system involve mutuals, and in generators nonreciprocal mutuals, makes me wonder whether some further fundamental research in this system of co-ordinates may not be justified, to find a modification which will avoid that complication. Possible transformations of co-ordinates, that is, possible equivalent systems, are infinite in number; and we are justified in being optimistic. What we need is a clue as to the direction in which the kind of tool we want is to be found.

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* For the method of inversion of a matrix see Kron's new book, "Tensor Analysis of Networks."

Finally, in spite of some who are doubtful the electrical engineering profession will in time come to realize that *the method of reasoning called tensor analysis is a very valuable tool.*

Edith Clarke (General Electric Company, Schenectady, N. Y.): Professor Kimbark has given an excellent presentation of a method of solving three-phase unbalanced systems by means of three sets of components different from symmetrical components. He is to be congratulated on his systematic and logical treatment of this timely subject. Undoubtedly, the components discussed in this paper will be extensively used, since their use greatly simplifies the solution of many problems, not only those dealing with steady state conditions but with transient conditions as well.

With this in mind and realizing the advantages of a standardized notation, I am prompted to discuss the title and notation of the paper:

The title "Two-Phase Co-ordinates of a Three-Phase Circuit" is not descriptive of a circuit in which there are zero-sequence components. In reference 8 the title "Modified Symmetrical Components" was used. The objection to this title is that it does not specify how the symmetrical components are modified.

In references 3, 5, 6, and 8, the components are called α, β, and 0 components, where

α components are positive- plus negative-sequence components, and are therefore the same as x components.

β components are positive- minus negative-sequence components turned backward 90 degrees, and are therefore y components with reversed signs. β components conform to the notation in reference 2.

0 components are zero-sequence components. z components of voltage are zero-sequence voltages. z components of current are twice zero-sequence currents, z impedances are one-half zero-sequence impedances.

The advantages in retaining the well-established zero-sequence components and conforming to the notation of reference 2 appear to me to outweigh the advantages of the y and z components. My preference is for α, β, and 0 components as defined above with the title of "α, β, 0 components," but I should welcome a more descriptive title and a notation standardized by the Institute.

Troy D. Graybeal (Massachusetts Institute of Technology, Cambridge): Mr. Kimbark has given a lucid account of a mathematical transformation well adapted to solving certain types of unbalanced three-phase circuits. His physical interpretation of the transformed currents and voltages is highly commendable. A few words of this nature do more to clarify ideas for one unfamiliar with the subject than do pages of description and mathematical formulas.

The illustrative examples are pertinent in pointing out the type of problem that can be handled by this method. Undoubtedly, the equivalent circuits given in the appendix would be of great value to anyone applying these co-ordinates to a problem set up on a network analyzer. Yet in a method of this type there is often the disappointing lack of the "nicety" of symmetry. The factor $^3/_2$ coming into the expression for power and the factor 2 in the expression for I destroy the symmetry that is desirable in any set of working formulas.

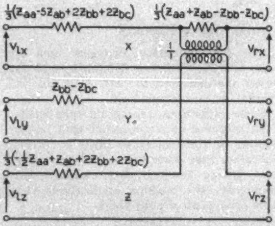
I rather disfavor the title "two-phase components." It gives the impression that the three-phase voltages (or currents) are expressible by two components which are two-phase. Actually there are three components, one of which is the well-known zero component divided by two. It does not seem proper to speak of a set of voltages as two-phase when a zero component is involved. In addition, the two remaining components V_x and V_y, do not represent a set of two-phase voltages in the ordinary sense unless $V^- = 0$ or $V^+ = 0$. This relation exists only for balanced impedances in all three phases. These conditions are contrary to the cases where this transformation would be applied.

It seems to me that Miss Clarke's method of representation where $V_\alpha = V^+ + V^-$ and $V_\beta = V^+ - V^-$ leads to somewhat simpler results than do the use of V_x and V_y. The introduction of the j in the y components appears to introduce complexities in the relations between the different voltages and currents so that transformer ratios like $1:j$ are needed in some of the equivalent circuits. Whereas transformer ratios of this kind may be treated easily mathematically, they are not easily represented on a network analyzer.

Edward Helwith (Gibbs and Hill, Inc., New York, N. Y.): This comment on Doctor Kimbark's paper "Two-Phase Co-ordinates of a Three-Phase Circuit" is a presentation of some of his conclusions in matrix form, illustrating an approach which is probably standard for cases of substituted variables.

Starting with the most general three-phase circuit

$$\left.\begin{aligned} V_a &= Z_{aa}I_a + Z_{ab}I_b + Z_{ac}I_c \\ V_b &= Z_{ba}I_a + Z_{bb}I_b + Z_{bc}I_c \\ V_c &= Z_{ca}I_a + Z_{cb}I_b + Z_{cc}I_c \end{aligned}\right\} \quad (1)$$



$\frac{1}{3}(Z_{aa}-5Z_{ab}+2Z_{bb}+2Z_{bc})$
$\frac{1}{3}(Z_{aa}+Z_{ab}-Z_{bb}-Z_{bc})$
V_{lx}
X
$\frac{1}{1}$
V_{rx}
$Z_{bb}-Z_{bc}$
V_{ly}
Y
V_{ry}
$\frac{1}{3}(-\frac{1}{2}Z_{aa}+Z_{ab}+2Z_{bb}+2Z_{bc})$
V_{lz}
Z
V_{rz}

Figure 5

We define

$$V = \begin{vmatrix} V_a \\ V_b \\ V_c \end{vmatrix} \quad I = \begin{vmatrix} I_a \\ I_b \\ I_c \end{vmatrix} \quad Z = \begin{vmatrix} Z_{aa}Z_{ab}Z_{ac} \\ Z_{ba}Z_{bb}Z_{bc} \\ Z_{ca}Z_{cb}Z_{cc} \end{vmatrix} \quad (2)$$

and write $V = Z{\cdot}I$.

We define

$$V' = \begin{vmatrix} V_x \\ V_y \\ V_z \end{vmatrix} \quad I' = \begin{vmatrix} I_x \\ I_y \\ I_z \end{vmatrix} \quad Z' = \begin{vmatrix} Z_{xx}Z_{xy}Z_{xz} \\ Z_{yx}Z_{yy}Z_{yz} \\ Z_{zx}Z_{zy}Z_{zz} \end{vmatrix} \quad (3)$$

To relate the systems to each other we put

$$\left.\begin{aligned} V_x &= \frac{1}{3}(2V_a - V_b - V_c) \\ V_y &= \frac{1}{\sqrt{3}}(0V_a - V_b + V_c) \\ V_z &= \frac{1}{3}(V_a + V_b + V_c) \end{aligned}\right\} \quad (4)$$

$$\left.\begin{aligned} I_x &= \frac{1}{3}(2I_a - I_b - I_c) \\ I_y &= \frac{1}{\sqrt{3}}(0I_a - I_b + I_c) \\ I_z &= \frac{2}{3}(I_a + I_b + I_c) \end{aligned}\right\} \quad (5)$$

which in matrix notation is

$$\begin{vmatrix} V_x \\ V_y \\ V_z \end{vmatrix} = \begin{vmatrix} 2y^2 & -g^2 & -g^2 \\ 0 & -g & g \\ g^2 & g^2 & g^2 \end{vmatrix} \cdot \begin{vmatrix} V_a \\ V_b \\ V_c \end{vmatrix} \quad (6)$$

(letting $1/\sqrt{3}$ be g for brevity) wherein the square matrix, called C_v, is an operator or transformation which through multiplication converts the last matrix into the set of equations (4). Equation 6 says $V' = C_v{\cdot}V$

Similarly for (5) we write $I' = C_I{\cdot}I$ where

$$C_I = \begin{vmatrix} 2g^2 & -g^2 & -g^2 \\ 0 & -g & g \\ 2g^2 & 2g^2 & 2g^2 \end{vmatrix}$$

differing from C_v in the last row.

We postulate that Ohm's law must hold for the two-phase co-ordinates ($V' = Z'I'$) thereby fixing the uniqueness of Z' in terms of Z. To find Z':

$$I' = C_I{\cdot}I$$
$$V' = C_v{\cdot}V$$

so

$$I = C_I^{-1}{\cdot}I'$$
$$V = C_v^{-1}{\cdot}V'$$

and substituting these in $V = Z{\cdot}I$ we get

$$C_v^{-1}{\cdot}V' = Z{\cdot}C_I^{-1}{\cdot}I'$$

or

$$C_v{\cdot}C_v^{-1}{\cdot}V' = V' = C_v{\cdot}Z{\cdot}C_I^{-1}{\cdot}I'$$

But by Ohm's law $V' = Z'I'$ so

$$Z' = C_v{\cdot}Z{\cdot}C_I^{-1} \quad (7)$$

where C_I^{-1} is the inverse of C_I.

$$C_I^{-1} = \begin{vmatrix} 1 & 0 & 1/2 \\ -1/2 & -1/2g & 1/2 \\ -1/2 & 1/2g & 1/2 \end{vmatrix}$$

(see Doctor Kimbark's equation 3).

Operating on the Z of (2) as indicated in (7) we get $Z' = C_V \cdot Z \cdot C_I^{-1} =$

$$\frac{1}{2}\begin{vmatrix} g^2\left\{\begin{matrix} 4Z_{aa} - 2Z_{ab} - 2Z_{ac} \\ -2Z_{ba} + Z_{bb} + Z_{bc} \\ -2Z_{ca} + Z_{cb} + Z_{cc} \end{matrix}\right\} & g\left\{\begin{matrix} 0 - 2Z_{ab} + 2Z_{ac} \\ +0 + Z_{bb} - Z_{bc} \\ +0 + Z_{cb} - Z_{cc} \end{matrix}\right\} & g^2\left\{\begin{matrix} 2Z_{aa} + 2Z_{ab} + 2Z_{ac} \\ -Z_{ba} - Z_{bb} - Z_{bc} \\ -Z_{ca} - Z_{cb} - Z_{cc} \end{matrix}\right\} \\ g\left\{\begin{matrix} 0 \quad +0 \quad +0 \\ -2Z_{ba} + Z_{bb} + Z_{bc} \\ +2Z_{ca} - Z_{cb} - Z_{cc} \end{matrix}\right\} & \left\{\begin{matrix} 0 + 0 \quad +0 \\ +0 + Z_{bb} - Z_{bc} \\ +0 - Z_{cb} + Z_{cc} \end{matrix}\right\} & g\left\{\begin{matrix} 0 \quad +0 \quad +0 \\ -Z_{ba} - Z_{bb} - Z_{bc} \\ +Z_{ca} + Z_{cb} + Z_{cc} \end{matrix}\right\} \\ g^2\left\{\begin{matrix} 2Z_{aa} - Z_{ab} - Z_{ac} \\ +2Z_{ba} - Z_{bb} - Z_{bc} \\ +2Z_{ca} - Z_{ab} - Z_{cc} \end{matrix}\right\} & g\left\{\begin{matrix} 0 - Z_{ab} + Z_{ac} \\ +0 - Z_{bb} + Z_{bc} \\ +0 - Z_{cb} + Z_{cc} \end{matrix}\right\} & g^2\left\{\begin{matrix} Z_{aa} + Z_{ab} + Z_{ac} \\ +Z_{ba} + Z_{bb} + Z_{bc} \\ +Z_{ca} + Z_{cb} + Z_{cc} \end{matrix}\right\} \end{vmatrix} \quad (8)$$

where the elements are respectively

$$\begin{vmatrix} Z_{xx}Z_{xy}Z_{xz} \\ Z_{yx}Z_{yy}Z_{yz} \\ Z_{zx}Z_{zy}Z_{zz} \end{vmatrix}$$

This is the most general case possible, in which all impedances are permitted to be unequal and nonreciprocal. The simpler forms used in most problems can be found from this by applying the simplifying equalities provided by the data of the problem. Thus, for static circuits all mutuals are reciprocal, while for balanced circuits various impedances are equal.

Take as an illustration an untransposed transmission line symmetrical about its center wire a

$$Z_{bb} = Z_{cc}, \; Z_{ab} = Z_{ba} = Z_{ac} = Z_{ca}, \; Z_{bc} = Z_{cb}$$

The impedance matrix (8) reduces to

$$Z' = \begin{vmatrix} g^2(2Z_{aa} - 4Z_{ab} + Z_{bb} + Z_{bc}) & 0 & g^2(Z_{aa} + Z_{ab} - Z_{bb} - Z_{bc}) \\ 0 & (Z_{bb} - Z_{bc}) & 0 \\ g^2(Z_{aa} + Z_{ab} - Z_{bb} - Z_{bc}) & 0 & g^2(^1/_2 Z_{aa} + 2Z_{ab} + Z_{bb} + Z_{bc} \end{vmatrix} \quad (9)$$

Note that circuit y is independent, while circuits x and z are coupled by a single mutual. The other mutuals have vanished (which was the reason for using the transformation). The equivalent circuit (figure 2 of Doctor Kimbark's paper) is suitable for solving by elementary methods or setting up on a calculating board (figure 5 of this discussion).

The demonstration that $Z' = C_V \cdot Z \cdot C_I^{-1}$ is of course general, and this equation will give the new impedances for any system of substituted variables.

R. D. Evans and **J. E. Hobson** (both of Westinghouse Electric and Manufacturing Company, East Pittsburgh, Pa.): Professor Kimbark's paper is of considerable interest in that he has given a competent discussion of a method which might be suggested as a substitute for the familiar method of symmetrical components. We believe the results of his investigation show that the new components may be useful for a small class of specialized problems as an auxiliary tool to supplement symmetrical components, but that the proposed system of components offers no fundamental advantage which will justify considering it as a replacement for symmetrical components in general. We agree with what we believe is Professor Kimbark's intent, that the method appears to be for limited rather than for general use, and of immediate value to the specialist in circuit theory rather than to the practical engineer handling system problems.

The solution of unbalanced circuit problems is now generally obtained by the method of symmetrical components using the sets of components designated as positive, negative, and zero sequence. The solutions are commonly carried out with the aid of the conception of the sequence network,[1] first introduced in 1925. The connection of these networks is rather simple for the ordinary unbalances to which a symmetrical system may be subjected. However, for the more complicated unbalances, such as may be described by the terms "unbalanced faults on unsymmetrical systems" and "multiple unbalances," the interconnections between networks become quite complicated. This problem was investigated in a previous paper by Professor Kimbark[2] who provided a solution for a number of these cases by introducing mutual coupling between the networks. An investigation revealed that the components, described in this paper as "two-phase co-ordinates," do simplify the coupling between networks for certain types of unbalances. The question naturally arises as to the practical value of the method. Stated in another way, does the method offer sufficient promise as to justify the practical engineer becoming as familiar with it as he has with the method of symmetrical components?

In our experience and opinion the vast majority of unbalanced-circuit problems deal with faults on a symmetrical system. For such problems the use of symmetrical components gives the simplest set of components because, (1) the positive-sequence network is the system network for the normal operating condition, (2) the negative-sequence network has an interpretation that is readily understood, (3) the use of "two-phase co-ordinates" requires sources of electromotive force associated with two rather than one set of components, and (4) the majority of problems involving machines may be considered on the basis of nonsalient-pole theory; for such cases the negative-sequence system is of particular advantage from the standpoint of the physical conception and from the ease of manipulation.

The question thus becomes that of whether or not the advantage of the method proposed here would justify its use for special problems. For double unbalances and many similar problems it is possible to extend the symmetrical components by analytical methods, as discussed by Edith Clarke,[3] Wagner and Evans,[4] and E. L. Harder.[5] Many times it is possible to resolve the problem into a symmetrical part and add it to the symmetrical system, as discussed by Wagner and Evans,[4] chapter 8. The use of this procedure would permit treating examples 6, 7, 8, and 9 of this paper as special cases for one type of unbalance, as in the manner employed by Harder.[5]

Professor Kimbark has indicated that unbalanced faults on an unsymmetrical distribution system provide the most frequently occurring practical problem for which the method would be applicable. Such problems as they occur practically, usually permit the use of a single source and also the use of the same impedance in the supply for both positive and negative sequence. Such a system may be readily solved with the a-c network calculator set up to represent the three-phase networks. Such a procedure has been found advantageous for the study of transients and has been used to a considerable extent.[6]

It should be pointed out that some of the special cases discussed by Professor Kimbark may be solved with less, or with no greater, complexity by the use of symmetrical components. The two-phase method appears to be advantageous for cases when the circuit (1) does not permit the flow of zero-sequence currents and (2) is symmetrical with respect to one phase. For example, the method is advantageous for systems involving transformers connected open delta/open delta but offers little advantage for the open delta/open star connection, for the open delta/open delta autotransformer connection, or for the open delta/open delta connection for the apexes of the two V's connected and common to both circuits. For either of the cases just mentioned the use of either system of co-ordinates will involve mutual coupling with the zero-sequence network.

The term "Two-Phase Co-ordinates of a Three-Phase Circuit" suggests that the method is simpler than symmetrical components with its three sets of components. We recognize the difficulty in finding suitable names for such methods, but we believe the name chosen is not altogether appropriate since there is included a third set of components not associated with a two-phase system, and since the voltages and currents of the two-phase system set up in this paper do not always bear 90-degree relationships. We believe it is more difficult to get a clear physical conception of the two-phase co-ordinate networks than for the conventional and accepted positive- and negative-sequence networks. Furthermore, it is very unfortunate that the co-ordinate system set up here has different equations for resolving voltages and currents into their zero-sequence components, and that the zero-sequence co-ordinate system is so nearly the same as the zero-sequence system of symmetrical components without being identical with it. The familiar zero-sequence system could have been retained by appropriate modification of the definitions for the other sequence systems.

In conclusion, we feel Professor Kimbark's investigation has been quite worth while, that his analysis is thorough and complete, and that he has presented the results quite clearly. However, we do not think the system of co-ordinates proposed will in any sense replace symmetrical components for general use, although specialists in system calculations may find occasional use for the system of "two-phase co-ordinates" as a supplemental tool to symmetrical components.

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3. SIMULTANEOUS FAULTS ON THREE-PHASE SYSTEMS, Edith Clarke. AIEE TRANSACTIONS, volume 50, 1931, pages 919–41.

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5. SEQUENCE NETWORK CONNECTIONS FOR UNBALANCED LOAD AND FAULT CONDITIONS, E. L. Harder. *Electric Journal*, volume 34, December 1937, pages 481–8.

6. SYSTEM RECOVERY VOLTAGE DETERMINATION BY ANALYTICAL AND A-C CALCULATING BOARD METHODS, R. D. Evans and A. C. Monteith. AIEE TRANSACTIONS, volume 56, June 1937, pages 695–73.

S. Austen Stigant (London, England): Accepting the stated definition of the 0, α, and β currents, an approach to their study, which is perhaps more direct than that given in the paper, is as follows. In a three-phase system of conductors the 0, α, and β currents are distributed as shown in figure 6 of this discussion from which,

$$\left.\begin{aligned} I_a &= I_0 + I_\alpha \\ I_b &= I_0 - 1/2 I_\alpha + I_\beta \\ I_c &= I_0 - 1/2 I_\alpha - I_\beta \end{aligned}\right\} \quad (1)$$

or in matrix notation $[I_n] = [M_1]\cdot[I_\gamma]$, that is,

$$\begin{bmatrix} I_a \\ I_b \\ I_c \end{bmatrix} = \begin{bmatrix} 1, & 1, & 0 \\ 1, & -1/2, & 1 \\ 1, & -1/2, & -1 \end{bmatrix} \cdot \begin{bmatrix} I_0 \\ I_\alpha \\ I_\beta \end{bmatrix} \quad (2)$$

Then, $[I_\gamma] = [M_1]^{-1}\cdot[I_n]$, that is,

$$\begin{bmatrix} I_0 \\ I_\alpha \\ I_\beta \end{bmatrix} = \begin{bmatrix} 1/3, & 1/3, & 1/3 \\ 2/3, & -1/3, & -1/3 \\ 0, & 1/2, & -1/2 \end{bmatrix} \cdot \begin{bmatrix} I_a \\ I_b \\ I_c \end{bmatrix} \quad (3)$$

from which,

$$\left.\begin{aligned} I_0 &= 1/3\ (I_a + I_b + I_c) \\ I_\alpha &= 1/3\ (2I_a - I_b - I_c) \\ I_\beta &= 1/2\ (I_b - I_c) \end{aligned}\right\} \quad (4)$$

The corresponding voltage equations on the basis of line-to-neutral voltages are identical with the foregoing, substituting E (or V) for I, so that,

$$[E_\gamma] = [M_1]^{-1}\cdot[E_m] \quad (5)$$

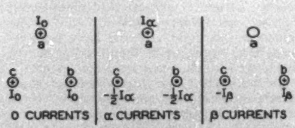

Figure 6

Now,

$$\left.\begin{aligned} [E_m] &= [Z_{mn}]\cdot[I_n] \\ &= [Z_{mn}]\cdot[M_1]\cdot[I_\gamma] \end{aligned}\right\} \quad (6)$$

and,

$$\left.\begin{aligned} &[Z_{mn}]\cdot[M_1] \\ &= \begin{bmatrix} Z_{aa}, & Z_{ab}, & Z_{ac} \\ Z_{ba}, & Z_{bb}, & Z_{bc} \\ Z_{ca}, & Z_{cb}, & Z_{cc} \end{bmatrix} \cdot \begin{bmatrix} 1, & 1, & 0 \\ 1, & -1/2, & 1 \\ 1, & -1/2, & -1 \end{bmatrix} \\ &= \begin{bmatrix} Z_{a0}, & Z_{a\alpha}, & Z_{a\beta} \\ Z_{b0}, & Z_{b\alpha}, & Z_{b\beta} \\ Z_{c0}, & Z_{c\alpha}, & Z_{c\beta} \end{bmatrix}, \text{ say.} \end{aligned}\right\} \quad (7)$$

Substituting for $[E_m]$ in (6),

$$\left.\begin{aligned} &[M_1]\cdot[E_\gamma] = [Z_{mn}]\cdot[M_1]\cdot[I_\gamma] \\ &\text{and,} \\ &[E_\gamma] = [M_1]^{-1}\cdot[Z_{mn}]\cdot[M_1]\cdot[I_\gamma] \end{aligned}\right\} \quad (8)$$

so that the new impedance matrix is,

$$\left.\begin{aligned} [Z_{m'n'}] &= [M_1]^{-1}\cdot[Z_{mn}]\cdot[M_1] \\ &= \begin{bmatrix} \frac{1}{3}, & \frac{1}{3}, & \frac{1}{3} \\ \frac{2}{3}, & -\frac{1}{3}, & -\frac{1}{3} \\ 0, & \frac{1}{2}, & -\frac{1}{2} \end{bmatrix} \cdot \begin{bmatrix} Z_{a0}, & Z_{a\alpha}, & Z_{a\beta} \\ Z_{b0}, & Z_{b\alpha}, & Z_{b\beta} \\ Z_{c0}, & Z_{c\alpha}, & Z_{c\beta} \end{bmatrix} \\ &= \frac{1}{3}\begin{bmatrix} (Z_{a0} + Z_{b0} + Z_{c0}), & (Z_{a\alpha} + Z_{b\alpha} + Z_{c\alpha}), & (Z_{a\beta} + Z_{b\beta} + Z_{c\beta}) \\ (2Z_{a0} - Z_{b0} - Z_{c0}), & (2Z_{a\alpha} - Z_{b\alpha} - Z_{c\alpha}), & (2Z_{a\beta} - Z_{b\beta} - Z_{c\beta}) \\ \frac{3}{2}(Z_{b0} - Z_{c0}) & , \frac{3}{2}(Z_{b\alpha} - Z_{c\alpha}) & , \frac{3}{2}(Z_{b\beta} - Z_{c\beta}) \end{bmatrix} \end{aligned}\right\} \quad (9)$$

In terms of symmetrical components,

$$\left.\begin{aligned} [E_s] &= [S]^{-1}\cdot[E_m] \\ &= [S]^{-1}\cdot[M_1]\cdot[E_\gamma] \\ &= [M_2]\cdot[E_\gamma] \end{aligned}\right\} \quad (10)$$

where

$$\left.\begin{aligned} [M_2] &= [S]^{-1}\cdot[M_1] \\ &= \frac{1}{3}\begin{bmatrix} 1, & 1, & 1 \\ 1, & h, & h^2 \\ 1, & h^2, & h \end{bmatrix} \cdot \begin{bmatrix} 1, & 1, & 0 \\ 1, & -\frac{1}{2}, & 1 \\ 1, & -\frac{1}{2}, & -1 \end{bmatrix} \\ &= \begin{bmatrix} 1, & 0, & 0 \\ 0, & \frac{1}{2}, & j\frac{1}{\sqrt{3}} \\ 0, & \frac{1}{2}, & -j\frac{1}{\sqrt{3}} \end{bmatrix} \end{aligned}\right\} \quad (11)$$

and

$$[M_2]^{-1} = \begin{bmatrix} 1, & 0, & 0 \\ 0, & 1, & 1 \\ 0, & -j\frac{\sqrt{3}}{2}, & j\frac{\sqrt{3}}{2} \end{bmatrix} \quad (12)$$

Also,

$$\left.\begin{aligned} &[E_s] = [Z_s]\cdot[I_s] \text{ where} \\ &[Z_s] = \begin{bmatrix} Z_{00}, & Z_{01}, & Z_{02} \\ Z_{10}, & Z_{11}, & Z_{12} \\ Z_{20}, & Z_{21}, & Z_{22} \end{bmatrix} \end{aligned}\right\} \quad (13)$$

so that,

$$[M_2]\cdot[E_\gamma] = [Z_s]\cdot[M_2]\cdot[I\gamma] \text{ and}$$

$$[E_\gamma] = [M_2]^{-1}\cdot[Z_s]\cdot[M_2]\cdot[I\gamma]$$

from which equation 14 is obtained.

The mutual impedances of the symmetrical component impedance matrix $[Z_s]$ ($= [S]^{-1}\cdot[Z_{mn}]\cdot[S]$) are, in general, nonreciprocal. However, when $Z_{11} = Z_{22}$ the mutual impedances of matrix 14 are reciprocal numerically if we multiply the first column of (14) by $^1/_2$ and the third column by $^3/_4$. We must then define I_0, I_α, and I_β as

$$\left.\begin{aligned} I_0 &= \tfrac{2}{3}\,(I_a + I_b + I_c) \\ I_\alpha &= \tfrac{1}{3}\,(2I_a - I_b - I_c) \\ I_\beta &= \tfrac{1}{2}\,(I_b - I_c) \end{aligned}\right\} \quad (15)$$

The fact that under these circumstances the mutual impedances of matrix 14 and hence (9) are reciprocal, that is, matrices (9) and (14) are virtually symmetrical, is of itself, of no use, since by the ordinary method of calculation $[Z_{mn}]$ is symmetrical in nearly all practical cases. But if, in addition, the network is symmetrical about phase a the equivalent impedance matrix (9) contains only one pair of reciprocal mutual impedances, all other mutual impedances being zero. Let

$$Z_{bb} = Z_{cc},\quad Z_{ab} = Z_{ac} \text{ and}$$
$$Z_{ab} = Z_{ba},\quad Z_{bc} = Z_{cb},\quad Z_{ca} = Z_{ac}$$

Then,

$$\left.\begin{aligned} Z_{a0} &= Z_{aa} + 2Z_{ab} \\ Z_{b0} &= Z_{bb} + Z_{ab} + Z_{bc} \\ Z_{c0} &= Z_{bb} + Z_{ab} + Z_{bc} = Z_{b0} \\ \\ Z_{a\alpha} &= Z_{aa} - Z_{ab} \\ Z_{b\alpha} &= Z_{ab} - \frac{1}{2}(Z_{bb} + Z_{bc}) \\ Z_{c\alpha} &= Z_{ab} - \frac{1}{2}(Z_{bb} + Z_{bc}) = Z_{b\alpha} \\ \\ Z_{a\beta} &= 0 \\ Z_{b\beta} &= Z_{bb} - Z_{bc} \\ Z_{c\beta} &= Z_{bc} - Z_{bb} = -Z_{b\beta} \end{aligned}\right\} \quad (16)$$

$$
\left.
\begin{aligned}
[Z_{m'n'}] &= [M_2]^{-1}\cdot[Z_s]\cdot[M_2] \\
&= \begin{bmatrix} 1, & 0\,, & 0 \\ 0, & 1\,, & 1 \\ 0, & -j\dfrac{\sqrt{3}}{2}, & j\dfrac{\sqrt{3}}{2} \end{bmatrix} \cdot \begin{bmatrix} Z_{00}, & Z_{01}, & Z_{02} \\ Z_{10}, & Z_{11}, & Z_{12} \\ Z_{20}, & Z_{21}, & Z_{22} \end{bmatrix} \cdot \begin{bmatrix} 1, & 0, & 0 \\ 0, & \dfrac{1}{2}, & j\dfrac{1}{\sqrt{3}} \\ 0, & \dfrac{1}{2}, & -j\dfrac{1}{\sqrt{3}} \end{bmatrix} \\
&= \begin{bmatrix} Z_{00} & ,\ \dfrac{1}{2}\,(Z_{01}+Z_{02}) & ,\ j\dfrac{1}{\sqrt{3}}\,(Z_{01}-Z_{02}) \\ Z_{10}+Z_{20} & ,\ \dfrac{1}{2}\,(Z_{11}+Z_{21}+Z_{12}+Z_{22}), & j\dfrac{1}{\sqrt{3}}(Z_{11}+Z_{21}-Z_{12}-Z_{22}) \\ j\dfrac{\sqrt{3}}{2}\,(Z_{20}-Z_{10}), & j\dfrac{\sqrt{3}}{4}\,(Z_{21}+Z_{22}-Z_{11}-Z_{12}), & \dfrac{1}{2}\,(Z_{11}+Z_{22}-Z_{12}-Z_{21}) \end{bmatrix}
\end{aligned}
\right\} \quad (14)
$$

Therefore from (9) the new impedance matrix becomes,

$$
[Z_{m'n'}] = \frac{1}{3}\begin{bmatrix} Z_{aa} + 2Z_{bb} + 4Z_{ab} + 2Z_{bc}, & Z_{aa} + Z_{ab} - (Z_{bb} + Z_{bc}), & 0 \\ 2\,\{Z_{aa} + Z_{ab} - (Z_{bb} + Z_{bc})\}, & 2(Z_{aa} - 2Z_{ab}) + Z_{bb} + Z_{bc}, & 0 \\ 0 & ,\ 0 & ,3(Z_{bb} - Z_{bc}) \end{bmatrix} \quad (17)
$$

Or dividing the first column by 2 and defining I_0 as in (15), and I_α and I_β as in (4), the new impedance matrix is,

$$
\frac{1}{3}\begin{bmatrix} \dfrac{Z_{aa}}{2} + Z_{bb} + 2Z_{ab} + Z_{bc}, & Z_{aa} + Z_{ab} - (Z_{bb} + Z_{bc}), & 0 \\ Z_{aa} + Z_{ab} - (Z_{bb} + Z_{bc}), & 2(Z_{aa} - 2Z_{ab}) + Z_{bb} + Z_{bc}, & 0 \\ 0 & ,\ 0 & ,\ 3(Z_{bb} - Z_{bc}) \end{bmatrix} \quad (18)
$$

where there is only one pair of mutual impedances and these are reciprocal.

W. A. Lewis (Cornell University, Ithaca, N. Y.): In the other discussions of this paper it is pointed out that the title "Two-Phase Co-ordinates" is not truly representative of the type of co-ordinates, and that a more suitable name should be applied. In view of the fact that these co-ordinates are not symmetrical, it would not be appropriate to call them a variety of symmetrical components even though they are conceived in terms of symmetrical component quantities. In view of the fact that the basic components are normally at right angles, I would like to suggest the name "quadric components."

It must be definitely borne in mind that to represent a three-phase system without restriction, there must be three components, each possessing both magnitude and direction, in order to comply with the necessary degrees of freedom, and it seems extremely important that the name used should not suggest there are less than three components. If an entirely new name is used, but one which is to some extent descriptive, it would seem that the principal objections are overcome.

Edward W. Kimbark: Most of the discussion can be grouped under two heads: first, that which concerns the field of application of the new co-ordinates; second, that which concerns their name, notation, and form.

With respect to the field of application, I agree with Evans and Hobson that the new method is not intended to supplant symmetrical components. On the other hand, I cannot agree with them that it is of such little practical value that the engineer who knows symmetrical components should not bother to become acquainted with it. Some problems are solved more easily by symmetrical co-ordinates, some by two-phase co-ordinates, and some with equal ease (or difficulty) by either method. Several practical problems, not given in the paper, to which two-phase co-ordinates are applied advantageously have come to my attention, and future experience with the method should widen its field of use. The new co-ordinates are not hard to learn, because familiarity with the method of symmetrical components has already accustomed engineers to thinking in terms of substitute currents and voltages and substitute networks. Becoming familiar with a second substitution is a smaller step than grasping the concept of using any substitution. Mr. Evans and his associates, by evolving a practical, everyday working tool from an ingenious but abstruse mathematical transformation, have already broken the hard ground.

The transformer connections which Evans and Hobson cite as ones in which two-phase co-ordinates offer little advantage over symmetrical co-ordinates are poorly chosen to prove their point. As they say, the use of either co-ordinates involves mutual coupling with the zero-sequence or z network. However, they fail to point out that in the equivalent circuits of these transformers in symmetrical co-ordinates all three sequence networks are coupled together, whereas in two-phase co-ordinates only the x and z networks are coupled, the y network being entirely independent. For this reason the equivalent circuits in question are much simpler in two-phase co-ordinates than in symmetrical co-ordinates.

A disadvantage of the method of two-phase co-ordinates, stated in the paper, is that both x and y networks contain sources of electromotive force. This disadvantage disappears, however, in the type of problems to which the method would be applied most frequently—problems in three-phase circuits symmetrical with respect to one phase—because in such cases the y network is independent. This fact enables us to make one network, set up on a calculating board, do double duty—first as the x network, then, with slight change, as the y network.

Let us turn now to the second aspect of the discussion, dealing with name, notation, and form. There seems to be general dissatisfaction with the name "two-phase co-ordinates." The objections to it given are: (1) that the name implies that three independent variables are replaced by two; and (2) that the two-phase co-ordinates of an *unbalanced* set of three-phase currents or voltages are not equal and in quadrature like a *balanced* set of two-phase currents or voltages. I feel that the name is appropriate because the x and y networks may be considered jointly to constitute a two-phase network equivalent to the original three-phase network after the zero-sequence quantities have been abstracted from it. The two-phase components of a balanced positive-sequence set of three-phase vectors form a balanced two-phase set of vectors of phase order y, x. Reversing the phase order of the three-phase vectors reverses the phase order of their two-phase components. The two-phase components of an unbalanced three-phase set of vectors form an unbalanced two-phase set.

But, as the discussion has shown amply that the name "two-phase co-ordinates" leads to misunderstanding, another name should be considered. The name "quadric components," proposed by Doctor Lewis, does not seem entirely appropriate, as the dictionary's definition of "quadric" is "(Math.) Of the second degree." The word "quadrature" more nearly fits the intended meaning, but is open to one of the expressed objections to the name "two-phase" even more than the latter is; namely, that the two-phase components of unbalanced three-phase vectors may not be in quadrature. In the absence of an acceptable descriptive name, a personal name could well be used, and the designation "Clarke components," proposed by Mr. Boyajian, is highly appropriate.

The notation to be used in denoting the three substitute networks is largely a matter of taste, and there is no disputing about tastes. I prefer roman letters (such as x, y, z) to Greek letters (such as α, β, γ) because the latter are not found on many typewriters. Besides, the Greek α is easily confused with the roman a, often used in the same equations to denote a phase. Letters x and y, by suggesting the axes of a rectangular co-ordinate system, help one to remember the positions of the vectors representing the two-phase components of balanced positive-sequence three-phase vectors; while z follows in alphabetical order and suggests zero sequence. Obviously, it would not be wise to use subscript 0 for components similar to, but not identical to, the zero-sequence components of symmetrical co-ordinates.

The use of matrix notation was avoided in the paper in the belief that it would be a hindrance rather than a help to the average reader. However, for readers interested in matrix and tensor methods, the discussions of Boyajian, Helwith, and Stigant form a valuable supplement to the paper.

In defining the new components there are many alternative forms differing by constant factors. That is, we may have in place of equations 2 of the paper:

$$
\begin{aligned}
I_x &= k_x(2I_a - I_b - I_c)\\
I_y &= k_y(I_b - I_c)\\
I_z &= k_z(I_a + I_b + I_c)
\end{aligned}
$$

$$
\begin{aligned}
V_x &= K_x(2V_a - V_b - V_c)\\
V_y &= K_y(V_b - V_c)\\
V_z &= K_z(V_a + V_b + V_c)
\end{aligned}
$$

where $k_x, k_y, k_z, K_x, K_y, K_z$ may be chosen in various ways. Mr. Boyajian has proposed a new form of the components which has considerable merit. The considerations bearing on the choice of the k's and K's may be listed as follows (not necessarily in order of importance):

1. (*a*) Complete or (*b*) partial invariance of power. By "complete invariance" of power, I mean that the vector power of a three-phase circuit is given in terms of substitute variables by $\Sigma(I_m \text{ conj } V_m)$ where $m = x, y, z$ or α, β, γ; and by "partial invariance" that it is given by $C\Sigma(Ie_m \text{ conj } V_m)$, where C is a constant. I consider partial invariance essential for the representation of unbalances by means of connections between the substitute networks. The additional step of complete invariance seems less necessary from a practical standpoint, though it is convenient in cases where a three-phase system is to be represented partially on a three-phase basis and partially in two-phase co-ordinates. The presence of the factor $C = 3$ in the power equation in symmetrical components has caused no particular difficulty; neither should a factor 3/2 in the corresponding equation for two-phase components cause any difficulty.

2. Same transformation for voltage as for current. This makes the formulas easier to remember.

3. Simple numerical coefficients (k's and K's) in the transformations. This facilitates both numerical calculation and visualization.

4. Simple transformer ratios (preferably one to one) in the connections between the substitute networks, especially the x and z networks, in the equivalent circuits of the commonest types of unbalance. This gives simple equivalent circuits, especially for calculating-board work.

5. Simple relation of the new components to symmetrical components. This takes advantage of the engineers' present familiarity with symmetrical components.

Unfortunately, the specifications given above are somewhat contradictory and cannot all be met by the same set of components. Therefore, one's choice of a particular set will depend upon the relative weight given to the several items in accord with one's opinion as to which contribute most to the mathematical consistency, nicety, or symmetry, and to the practical usability. With the aid of Mr. Boyajian's table of three forms of transformation matrix (Clarke, Kimbark, and Boyajian) let us see how well each form meets the specifications listed above. A fourth form, the "tensor form," given by Concordia,[3] will be considered also.

1. The Clarke form does not give invariance of power, the Kimbark form gives partial invariance, the Boyajian and Concordia forms give complete invariance.

2. The Clarke and Concordia forms have the voltage transformations identical to the current transformations; the Kimbark and Boyajian forms do not.

3. The Boyajian form has the simplest numerical coefficients and simplest physical interpretation, the Concordia form, the least simple.

4. The Concordia form leads to transformer ratios $1:\sqrt{2}$ in the coupling between substitute networks of the equivalent circuit of such simple unbalances as a line-to-ground short circuit or an open circuit in one wire. The Clarke form gives nonreciprocal coupling with the zero-sequence network. Ratios of transformers connecting the α and γ networks of the Boyajian form differ by a factor of 3/2 from those connecting the x and z networks of the Kimbark form.

5. The Clarke form is most closely related to symmetrical components, the Boyajian form least closely related.

It is true, as Mr. Boyajian says, that the currents, voltages, and impedances in his form of the co-ordinates have simple physical interpretations. As I do not believe that he has made the interpretation of these impedances clear in his discussion, I should like to amplify this point by the following statements:

$Y_{\alpha\alpha}$ is the admittance measured from terminal a to terminals b and c joined together.

$Y_{\beta\beta}$ is the admittance measured from terminal b to terminal c with terminal a open.

$Y_{\gamma\gamma}$ is the admittance measured from terminals a, b, c joined together to ground or neutral.

The corresponding statements for *impedance* are not true unless the substitute networks are independent, for otherwise the elements of the impedance matrix are not the reciprocals of the corresponding elements of the admittance matrix.

The self-admittances for the Kimbark form are equal to those of the Boyajian form multiplied by constants.

The Boyajian α and β impedances of a balanced static three-phase circuit differ from each other and from the positive-sequence impedance. The relations are $Z_{\alpha\alpha} = (3/2)Z_{11}$, $Z_{\beta\beta} = 2Z_{11}$, and $Z_{\gamma\gamma} = (1/3)Z_{00}$. Whether or not this disadvantage outweighs the advantage of simpler physical interpretation (simpler numerical coefficients) is hard to say.

After considering the discussion on name, notation, and form, my inclination is to recommend the following: Name—Clarke components, co-ordinates, or transformation. Notation—x, y, z. Form—Boyajian or Kimbark, pending investigation of equivalent circuits in the Boyajian system.

In concluding, I express the hope that further research in circuit transformations will show a general method of finding a transformation to eliminate or minimize the coupling between substitute networks in any given case. In such research, matrix or tensor methods will no doubt be useful and perhaps even essential.

Discussions

Of AIEE Papers—as Recommended for Publication by Technical Committees

ON THIS and the following three pages appear discussions submitted for publication, and approved by the technical committees, on papers presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938, and at the AIEE winter convention, New York, N. Y., January 23–27, 1939. These discussions were not available for correlation with their respective papers at the time the papers were published.

Low-Gas-Pressure Cable

Discussion and author's closure of a paper by G. B. Shanklin, presented at the AIEE winter convention, New York, N. Y., January 23–27, 1939, and published in AIEE TRANSACTIONS, 1939 (July section) pages 307–15. Other discussion was published on pages 315–18.

K. S. Wyatt (Enfield Cable Works, Ltd., Enfield, England): The theme of this highly interesting paper is in contrast to that of an earlier one[1] by the same author, for in the latter he showed the benefit of getting out and keeping out all gas spaces from the insulation, whereas here he shows the benefit of putting gas into the insulation. At about the same time that this paper, suggesting that solid cable is inadequate at 40 kv and below, was being delivered in New York, another paper[2] was being delivered in London, showing that solid cable was satisfactory at 60 kv for maximum temperatures of 75 degrees centigrade, and should even be considered for 132 kv.

For the last 40 years improvement in high-voltage cables has been achieved by getting more and more gas and vacuous spaces out of cable insulation, and more oil in: this is true for solid cable and for extra-high-voltage cable such as the oil-filled cable and the pressure cable. Now the author advocates reversing these principles and pushing oil out of the cable insulation and gas in, at least for cables up to 40 kv. Those who advocate gas in the insulation and the extinguishing of ionization by pressure on the gas, must show that on a long operating line, pressure can be maintained uniformly throughout the insulation wall both longitudinally and radially. Consider a line two or three miles in length, the contour of which will normally show a continued succession of rises and dips: if oil is not thoroughly drained from the cable, load-cycles will cause it to fill the dips and these will then act as oil seals against longitudinal transmission of gas pressure. As for radial maintenance of pressure, if there is not continuity of gas channel from conductor to sheath owing to the presence of oil which has not drained out, when gas pressure in the filler spaces drops, the gas, dissolved in the insulation under 10 to 40 pounds per square inch pressure, will come out of solution and tend to "bloat out" the insulation, increasing the radial dimensions of voids, and causing more severe ionization. It is imperative that gas flow through the insulation be such that pressure can be maintained at all points during the most rapid cooling cycle likely to be experienced in operation.

Migration of compound from the high points of a long line will leave the insulation with a very high thermal resistivity. It would be of interest if the author would give the actual values for this class of cable. In England it has been common practice to assume a thermal resistivity of 750 thermal ohms per centimeter cube, for cables up to six kv, in comparison with a value of 550 for high-voltage cables. Such a difference has been justified by practical tests and has been attributed to the less compact construction and the inferior impregnation attained in the lower-voltage class. It is difficult to believe that a cable blown out with nitrogen gas would achieve a lower thermal resistance than the former of the two classes mentioned above. This will reduce maximum current carrying capacity.

Since the author takes ionization for granted in this type of cable, it is certain that ionic bombardment of the oil will produce gases, chiefly hydrogen. The latter being admixed with the pressure gas will reduce its minimum ionization potential, even though original values of the pure pressure gas of special composition are quite high.

Experience with vertical ends of three-conductor 24-kv cable, in which compound has migrated downward and air at atmospheric pressure has replaced it, confirms that such cable will operate satisfactorily for years at maximum stresses of 66 volts per mil. Nevertheless, to propose for commercial operation a gas-filled cable design with advance knowledge that ionization takes place in it from the day it is placed in operation, appears to be a bold step. There is also the consideration of the impulse strength of a cable in which ionization is taking place in the gas films between layers.

The 30-degree-centigrade "solid" loss of the experimental cable exhibited by the author has increased in some cases severely. Unfortunately no radial power-factor curves which show up such deterioration so clearly, are given. There are good reasons for such increases. First, hydrogen generated by ionization from the compound, being admixed with the pressure gas, will react with it during discharge to form from carbon dioxide, water; from nitrogen, ammonia and amide compounds; and from special gases containing chlorine, hydrochloric acid. These products in the presence of moisture, which is always present in fibrous insulation in small amounts, undesirably increases dielectric loss. Second, 0.3 per cent O_2 impurity in the pressure gas should react at 2 atmospheres (absolute) at the same rate as 0.6 per cent O_2 at one atmosphere (absolute); and at 15 atmospheres as 4.5 per cent oxygen at one atmosphere. If the loss keeps on increasing year by year, will these cables be economical?

It is difficult to understand from the author's paper the economic advantage of such cable over solid cable, except that the lead sheath is perhaps not worked so much, thus perhaps cutting down the number of water failures. For a given duct size, however, a larger conductor can be used because of the thinner insulation wall.

Exception must be taken to the author's comments regarding the difficulty of reinforcing a cable for high gas pressure. Such reinforcement merely consists of a thin metal tape wound over the lead sheath together with some suitable bedding. If reinforcement has to be applied it is immaterial whether 40 or 200 pounds per square inch gas pressure is employed, somewhat thicker steel tapes of course being used for the higher pressures. The suggested tendency of such a cable to "whip" in manholes if it were present could be overcome quite simply and inexpensively by the inclusion of a metal tape applied with long lay in the reinforcement.

Conflicting nomenclature is employed by the author. In the title the cable is termed "low-gas-pressure cable" and in the conclusions, "gas-filled cable." Actually the term pressure cable was first used by Hochstadter and Bowden,[3] in a paper describing the use of fluid pressure, either gas or liquid, external to the lead sheath of an ordinary solid cable as a means of eliminating ionization and thus permitting operation at not less than double the stress of solid cable. The term appears to have become generally accepted in this sense. Since when oil is fed into an operating cable it is termed "oil filled" it seems reasonable that when compressed gas is fed into the insulation of an operating cable it should be termed "gas filled."

REFERENCES

1. PROGRESS IN HIGH-TENSION UNDERGROUND CABLE RESEARCH AND DEVELOPMENT, **G. B. Shanklin and G. M. J. Mackay**. AIEE TRANSACTIONS, volume 48, April 1929, page 338.

2. LONG-PERIOD AGING TESTS ON SOLID-TYPE CABLES, **T. R. Scott and R. C. Mildner**. IEE, January 1939.

3. *Journal* of the Royal Society of Arts, volume 80, December 11, 1931.

R. C. Mildner (Enfield Cable Works, Ltd., Enfield, England): The thorough and logical series of investigations on low-gas-pressure cables reported by Mr. Shanklin will be studied with keen interest by cable engineers, not only in America but also in many other countries. In all the major cable-manufacturing countries the realization that the satisfactory transmission of large blocks of power at high voltages goes beyond the basic requirement of adequate electrical performance has stimulated intense research activity, which has brought forward many new forms of high-voltage cable. It may well be that the construction sponsored by the author will find its niche in cable practice, and the Consolidated Edison Company deserves commendation for providing so early an opportunity for a trial installation in the field—a requirement which, for final judgment, must always supplement laboratory tests.

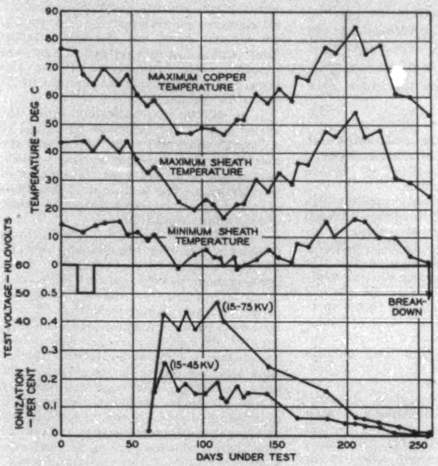

Figure 1. Life-test history—EC. 54/1a

In common with other advocates of the internal-gas-pressure cables, the author has seemed to adopt the viewpoint that the mere application of pressure is capable of defining to a very close degree the stress at which ionization will take place: as a consequence it is claimed that such cables are susceptible of exact engineering design so that it is possible thereby to prevent absolutely any deleterious ionization phenomena under working conditions. General experience and, indeed, the author's own results show that this is by no means the case. The incidence of ionization at any given pressure is both variable, depending upon the previous history of the cable, and indefinite in relation to the stress at which it is initiated. For the case of the low-gas-pressure cable at least, the situation in this respect is in no way dissimilar to that of solid-type cable. Figure 1 of this discussion shows the aging characteristics of a solid-type cable which was reported in a paper presented recently before the British Institution of Electrical Engineers ("Long-Period Aging Tests on Solid-Type Cables," T. R. Scott and R. C. Mildner, paper presented before the transmission section, IEE, January 1939), and it will be seen that it presents ~~quite~~ analogous phenomena of restrained ionization and a "cleaning up" process which operates during a major portion of the life under test. Furthermore, the cable shows a preliminary period during which ionization is entirely absent. In modern well-constructed cable cumulative ionization of the type envisaged by the author does not demonstrate itself at the stresses quoted for low-gas-pressure cable. In the absence, therefore, of quantitative tests it is unwise to assume that pressure-controlled cables of this type are immune from continuous deterioration when subjected to working electrical stresses at which ionization is relatively restrained.

In his discussion of ionization stability the author draws a distinction between positive and negative pressures. It is difficult to see how such a difference can in fact be other than one of degree since, as I assume, such pressures are relative to that of the external atmosphere. A dielectric composed of impregnated paper and gas pockets comprises under voltage a complex series-parallel network, in which the latter components will in general present saturated ionization phenomena. When, under severe conditions of test, secondary ionization sets in, the magnitude of this ionization will often be limited by the capacitive currents which the solid dielectric is capable of transmitting. It is therefore not difficult in solid-type cable to ensure that such ionization is not cumulative but decidedly limited in effect. In normal operation an advantage accrues to the solid-type cable from the fact that the energy associated with saturated ionization at low pressures is less than that bound up with saturated ionization at high pressures.

I believe that it is true to say that most cable engineers in England have accepted the thesis that, in judging test performance in which breakdown occurs by ionization phenomena, it is the value of maximum stress rather than that of average stress which is the limiting factor of operation. Judged on this basis the tests quoted by the author do not seem severe in comparison with those capable of being surmounted by solid cable. Of the cable type adopted by the author only one test to breakdown is mentioned, and this does not compare favorably, even when using the criterion of average stress, with the performance of the best types of modern solid-type cable. Thus in the previously-mentioned paper three distinct cable lengths with 445-mil insulation wall are quoted as having withstood without failure more than 260 days' testing at 134 volts per mil. Now while one cannot say at the moment whether it will prove possible to guarantee such characteristics for every production length of cable, these results do indicate that there would seem to be no obvious intrinsic limitation to the performance of solid-type cable, such as the author implies. It is therefore important to establish what advantages these gas-pressure cable types will have in service operation.

In conclusion I should like the author to express his opinion as to the reason for the variation in the solid loss of his cables at 80 degrees centigrade. An increase in loss may well be explained by the oxygen content of his nitrogen supply, a fact to which reference is made, but the decrease in loss observed, for instance, in lengths 5, 6, 7, and 8, if explained in terms of compound drainage, would suggest that such movements of compound may persist over a period as long as two years. It would appear that the high losses in these cables limit their application at the moment to medium voltages, moderate maximum temperatures, and uncongested cable systems.

G. B. Shanklin: In his discussion, Mr. Wyatt refers to an earlier paper by the same author and cites it as an example of inconsistency. While the author reserves the right to change his mind as the art progresses, this does not exactly apply in this case. If Mr. Wyatt will read the older paper closely, he will find a statement to the effect that gas under pressure in a solid-type cable might be beneficial if some way could be found to prevent this gas from concentrating and forming "dry spots" along the cable length. The present paper describes a design in which we believe this is accomplished.

Mr. Wyatt has raised practical questions regarding uniform maintenance of gas pressure along the cable line in service. Such questions can only be answered by practical field experience. No difficulty has yet been found in controlling pressure in the two installations now in service. We would prefer postponement of a final answer to this question until more field experience is obtained. Regarding maintenance of uniform pressure radially from conductor to sheath, I think Mr. Wyatt is overlooking several important factors. First, the cable is completely gas saturated at the required pressure and automatically tends to maintain this pressure at all points in the cross section. Second, with even the most rapid cooling cycle the drop in gas pressure in the feed channels is relatively small. We are not worried about the difficulties of maintaining uniform radial gas pressure so long as we can maintain uniform longitudinal pressure in the gas feed channels.

In regard to the thermal resistivity of the impregnated-paper insulation after drainage, it is stated in the paper that measurements show this to be from 10 to 15 per cent higher than the thermal resistivity of solid-type cable. It has been the practice in the United States for some years past to assign a value of 700 to solid-type cable after it is fully expanded by load cycles in service. The gas-pressure cable after drainage of compound accordingly has a thermal resistivity from 770 to 805.

I am afraid Mr. Wyatt has not read the present paper very closely or he would understand that we are not proposing continuous ionization discharge in the cable throughout its life and length. The paper merely presents test data tending to show that at those unfavorable points where incipient ionization might start in service it will be extinguished by self-healing action before real damage is done. This will allow much closer approach to critical ionization in service, than is the case with solid-type cable, without danger of cumulative action at the weaker points along the cable line.

Mr. Wyatt states that in some cases the 30-degree-centigrade "solid" loss increases severely, and goes on to demonstrate that this is due to deleterious by-products of ionization. If this were so, the 80-degree-centigrade "solid" loss would increase even more severely. A study of the test data in the paper will show that this occurred only in test length number 9 for reasons fully explained. In all other lengths tested there is no permanent increase in 80-degree centigrade "solid" loss, and the 30-degree-centigrade increase is actually small and unimportant.

I am sorry I cannot agree with Mr. Wyatt's claims relating to reinforcement of lead sheath for permanent operation at 200 pounds per square inch pressure. We know from a good deal of experience at pressures up to 50 pounds that the problem is more difficult than outlined by Mr. Wyatt.

I would like to thank Mr. Mildner for his interest in the low-pressure gas-filled cable and only wish that I could give him more complete answers to the questions he has raised. Unfortunately, I cannot because Mr. Mildner's whole discussion is based on the supposition that uniformly maintained gas pressure gives no more exact control of ionization than obtained in ordinary solid-type cable where the pressure fluctuates widely with temperature cycles. This is a question that can be fully answered only by operating experience. Laboratory tests and theory both indicate that uniformly maintained pressure does give more exact control of ionization. We all know that ionization discharge is materially influenced by pressure. The whole question is whether this theory will hold under practical operating conditions. Only time and a lot of cable in service will fully answer this question by giving a direct comparison with the service record of solid-type cable.

Mr. Mildner cites a test he has made and offers figure 1 to show that incipient ionization can also be controlled in solid-type cable. I do not doubt that this can be done when all conditions happen to be favorable. The question is can these results be reproduced with every length of solid cable tested? That there is some doubt of this in Mr. Mildner's mind is shown by his statement, "Now while one cannot say at the moment whether it will prove possible to guarantee such characteristics for every production length of cable, these results do indicate that there would seem to be no obvious intrinsic limitation to the performance of solid-type cable." If the low-gas-pressure cable proves to have any real value, it will be based on the hypothesis that uniformly maintained gas pressure will result in a more uniform performance of the insulation and sheath. If this proves so, then obviously we can safely increase the working voltage stress and the severity of other conditions as compared with solid-type cable.

Mr. Mildner is correct in stating that it is better to deal with maximum voltage stress than average voltage stress. Most of the cable lengths we tested had compact sector conductors with relatively sharp corners. For conductors of this shape it is difficult to express maximum voltage stress accurately, and it is for this reason that average voltage stress is generally used as a practical yardstick in the United States.

Referring to the last paragraph of Mr. Mildner's discussion, I offer the following tentative explanation for the variations in the 80-degree-centigrade solid loss of the gas-pressure-cable test lengths. The same characteristic has been noted in solid and oil-filled types of cable and is known to be directly related to the amount of impurities in the cable, particularly the amount of oxidation impurities. To identify this characteristic some engineers designate it as an "oxidation cycle." The theory is that during the initial active stage of oxidation the solid loss increases until finally all oxygen is consumed. After this the solid loss decreases as the final oxidation products become fixed or precipitated. These fixed oxidation products are not actively conducting and explain the decrease in loss.

Symposium on Operation of Boulder Dam Transmission Line—General Operation of Transmission Line

Wm. S. Peterson

Insulation and Lightning Protection

Bradley Cozzens

Discussion and authors' closures of two papers of a symposium presented at the AIEE Pacific Coast convention, Portland, Ore., August 9–12, 1938, and published in AIEE TRANSACTIONS, 1939 (April section) pages 131–56. Other discussion was published on pages 156–60.

C. M. Longfield (State Electricity Commission of Victoria, Melbourne, Australia): As an Australian who had the privilege of visiting the Boulder Dam project early in 1938, this group of papers is of very special interest to me. It is quite evident from even a casual contact with the designing staff that the principles upon which all components of this scheme were based were carefully checked before a final design was adopted. This aspect, and the courageous manner in which it was done, even more than the magnitude of the undertaking, are the things which impress one most, and the electrical industry is very much in the debt of the Bureau of Power and Light for making public both the design and operating results of this undertaking.

Mr. Peterson gives some very interesting data on page 134 of the results of several interruptions. In the case of the trouble experienced on March 29, 1938, he says that Boulder was carrying 230,000 kw. Did this result in a major shutdown in Los Angeles? As I understand the situation, Boulder carries a very large percentage of the total load in Los Angeles, and the erection of a third line will increase this percentage. What provision is being made for maintaining emergency supply in the event of major interruptions on the Boulder transmission line?

It appears that, so far, relay operations from one cause or another have always resulted in tripping off both circuits. This seems to have been due to:

(*a*). Both circuits being damaged by a tower wash out,

(*b*). Switching surges on parallelling circuits, or

(*c*). Faulty relay wiring.

In other words, from an operating point of view the reliability of the line has so far not been improved by having two circuits. Had these circuits been mounted on separate towers and paralleled at a lower-voltage bus, one circuit might have remained in service at least when the load being carried was within the transient stability limit of the remaining circuit.

The high efficiency of the line is a striking feature of the paper, and revival of the use of variable voltage at the generators is of particular interest. Have the losses in the synchronous condensers been included in calculating the efficiency of transmission listed in table I?

It is not generally realized how important the distribution of voltage along a transmission line can be as affecting the size of synchronous condensers. This is especially noticeable in those cases where intermediate condensers are used when the optimum set of conditions is highly critical.

In discussing lightning protection, Mr. Cozzens says that "ground resistance measured on stakes driven to approximate counterpoise depth showed resistance values as high as 3,000 ohms." Is this the resistance to ground of a single stake driven to this depth, or is it the soil resistivity as measured, say, by the Gish-Rooney method? The continuous counterpoise as used on the Boulder line is a very expensive construction, and one would naturally want to avoid the expense involved if a discontinuous counterpoise would do. For a line of the type described, but more favorably situated as regards ground resistivity, what would the author regard as a limiting resistance above which a continuous counterpoise becomes necessary? A very striking feature of the design of this line is the immunity from flashovers during the lightning storms so far experienced and which have left unmistakable evidence of their severity in the burning of the gaps. Is it safe to assume that the majority of discharges which cause burning of the gaps have been direct strokes to tower structures? Why is it that the magnetic links on the lightning arresters have shown no appreciable evidence of discharge? Is there then no evidence to show that the lightning arresters themselves have functioned? If not, why?

On the evidence so far presented, one could summarize Mr. Cozzens' paper by saying that the lightning protection provided has produced a line which, for all practical purposes, is trouble-free. It at least appears that the line is immune from interruptions due to lightning and that with periodical cleaning of the insulators in certain sections the line should be free from insulator failures due to foggy conditions. This conclusion, if valid, is an important one since, in that case, it would be possible to put reliance in a single-circuit line where it would otherwise be uneconomical to erect a duplicate circuit.

The influence of high-speed operation of relays and switchgear is also of great importance, as has been emphasized by E. H. Bancker in the discussion. Indeed the effect of rapid clearing of faults is very profound, especially in those cases where the transmission line delivers only a part of the total load into the network. Whether an additional circuit can be dispensed with will depend upon what provision has been made for taking up the load dropped by the faulty line and the transient stability characteristics of the rest of the system. This brings me back to the point raised in the second paragraph above, namely—what provision has been made in Los Angeles for supplying that part of the load dropped by the loss of one or both Boulder circuits?

W. S. Peterson: We greatly appreciate the interest that Mr. Longfield has shown in this project so remote from his immediate operations, and value greatly his contributions in discussion.

In reply to the question concerning the outage of March 29, 1938, the rolling reserve at the time of the trouble was sufficient to carry the entire system load, even if the Boulder plant were dropped. However, during the trouble and before the Boulder plants were disconnected, the frequency had fallen from 60 to 48 cycles. While the remaining generators were picking up the system load, the local load at station *B*, where Boulder power is received, was dropped, to facilitate the speed increase so synchronizing with Boulder could be accomplished. The amount of load dropped was approximately 50,000 kw. The remainder of the system load was carried without interruption.

The additional circuit from Boulder travels an entirely different route from Victorville switching station to Los Angeles. It is felt that this will give added protection to the Boulder power. Additional steam facilities will have to be provided in the near future to furnish load demands as well as stand-by service for the transmission systems. However, it is not the intent to provide 100 per cent Boulder system stand-by at the time of the annual peak load. Sufficient stand-by will be used to permit the leaving out of one section of circuit (90 miles) and be ready for a switching operation on another 90-mile section without losing synchronism. Due to the high standards of construction of the Boulder transmission system, it is believed that the service under such conditions will be superior and the investment less than would prevail if lower standards of line construction and 100 per cent steam stand-by were used.

Mr. Longfield's statements are true regarding the causes for outages, but it should not be misconstrued that the switching surges were of the type to cause flashover. They were, rather, current transients that operated on the relay circuits such as to indicate a faulted line and cause false tripping. Also, the major sources of trouble were the errors of human operation. It is true that to date the relays have failed to clear the faulted line without also dropping the parallel good section, but it is anticipated that this will be overcome in the very near future. The undesirable features of two circuits on one tower line were recognized, but right-of-way expense made the use of two tower lines prohibitive.

As stated on the bottom of page 133, the efficiency given in the table includes the losses through the autotransformers and the synchronous condensers, but does not include the loss in the transformation from 132 kv to the distributing voltage of 33 kv.

Bradley Cozzens: In reply to Mr. Longfield's question concerning the high values of ground resistance, these values were determined with the three-electrode Meggar method, using two of the electrodes as the current-carrying elements and a third stake to determine the potential of the ground as uninfluenced by the flow of current. This method is that outlined for use with the ground Meggar in Bulletin 1285 of the James G. Biddle Company.

In regard to the cost of continuous counterpoise as compared to the radial or other type, it is true that where the country is mountainous, rough, or of a solid rock formation, the continuous counterpoise is the more expensive. However, where the country is not extremely rough and will allow the use of the large counterpoise-laying plow, the cost of the continuous counterpoise is probably less than the actual cost of radial counterpoise with terminating ground rods, and is felt to be much more effective. We have not had sufficient experience to state at what ground resistance counterpoise should go from radial to continuous type. Future flashovers, if and when they occur, may give the answer to this question.

It is certain that practically all of the burns on the counterpoise gaps were caused by direct hits to either the tower structures or the ground wires. The few exceptions to this might be where the transmission line crosses lower-voltage circuits, and these circuits may have discharged into the counterpoise system and caused some of the smaller burns observed. The strokes to the towers have not as yet flashed gaps more than four towers from the tower contacted by the stroke.

With regard to the operation of the arresters at Boulder, we have had no direct hits within the first mile from the Boulder switch rack, so it is relatively certain that any of the more remote strokes would not have induced the 825 kv in the conductor which is necessary to cause operation of the arresters. The only indication of operation of the arresters is the slight burning of a series paper-film gap, but this has been attributed to the arcing of the disconnect switches connecting the arresters to the 287-kv bus.

Mr. Longfield's comment concerning immunity from lightning will have to have a slight prefacing, for on March 22, 1939, the Boulder north line was flashed over by lightning. This was the first flashover out of over 200 recorded direct hits. There is some consolation in the fact that it was a hit of the "unusual" type. The stroke apparently contacted the top arcing horn of a suspension string and with the heavy ionization accompanying such a condition bridged the string. This occurred with the ground wires located just $7^{1}/_{2}$ feet inside the vertical plane through the outer conductors and 30 feet above them at the tower. Such conditions, of course, cannot be predicted or guarded against. The arc formed directly between the horns and there were no burns on any of the insulators.

We, again, wish to thank Mr. Longfield for his interest and contributions to the papers.

Report of the Board of Directors

For the Fiscal Year Ending April 30, 1938

THE board of directors of the American Institute of Electrical Engineers presents herewith to the membership its 54th annual report, for the fiscal year ending April 30, 1938. A general balance sheet showing the condition of the Institute's finances on April 30, 1938, together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in ELECTRICAL ENGINEERING.

BOARD OF DIRECTORS' MEETINGS

During the year, the board of directors held five meetings, four in New York City, and one at Milwaukee, Wis. The executive committee meetings in December and March were held in place of regular meetings of the board. Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of ELECTRICAL ENGINEERING devoted to "News of Institute and Related Activities."

PRESIDENT'S VISITS

President Harrison attended the Pacific Coast and winter conventions and the Middle Eastern District meeting. He also visited many Sections and some Student Branches. During May and June, President Harrison will attend the North Eastern District meeting in Lenox, Mass., and the summer convention in Washington, D. C., and make a few other visits.

The places visited are listed below:

California
- Los Angeles Section
- San Francisco Section

Colorado
- Denver Section

Illinois
- Urbana (District conference on student activities)

Indiana
- Central Indiana Section, Lafayette
- Fort Wayne Section
- Purdue University, Lafayette

Iowa
- Iowa Section, Des Moines

Louisiana
- New Orleans Section

Michigan
- Detroit-Ann Arbor Section, Detroit

Minnesota
- Minnesota Section, Minneapolis

Missouri
- Kansas City Section
- St. Louis Section

Nebraska
- Nebraska Section, Omaha

New York
- New York Section
- Winter Convention
- Pratt Institute, Brooklyn

North Carolina
- North Carolina Section, Chapel Hill

Ohio
- Akron Section
- Cincinnati Section
- Columbus Section
- Ohio State University, Columbus

Oklahoma
- Oklahoma City Section

Pennsylvania
- Lehigh Valley Section, Wilkes-Barre
- Philadelphia Section
- Pittsburgh Section

Tennessee
- Memphis Section

Texas
- Dallas Section
- Houston Section

Washington
- Spokane (Pacific Coast Convention)

Wisconsin
- Milwaukee (Summer Convention)

Canada
- Toronto Section

NATIONAL CONVENTIONS

Three national conventions were held during the year, and a brief report on each follows:

Summer Convention. The 53d summer convention was held in Milwaukee, Wis., June 21–25, 1937. During 10 technical sessions, 35 technical papers, 5 committee reports, and 3 addresses were presented. Two technical conferences were held: one on field problems, and the other on electrical apparatus for three-phase arc furnaces. Convention events of unusual interest were a lecture by Doctor Vannevar Bush on Tuesday evening, an address by Doctor R. E. Flanders during the first part of the general session on Wednesday morning, and the subsequent introductory discussions by six members on various phases of Institute programs, which were followed by general discussion.

Other parts of the convention were the annual business meeting, conference of officers, delegates, and members, president's reception, followed by dinner and dancing, boat trip on Lake Michigan, buffet supper and smoker, golf and tennis tournaments, ladies' events, and farewell gathering. The registration was 1,067.

Annual Meeting. The annual business meeting of the Institute was held on Monday morning, June 21, as part of the opening session of the summer convention. The annual report of the board of directors for the fiscal year which ended April 30, 1937, was presented in abstract by the national secretary. A report on the finances of the Institute was presented by National Treasurer W. I. Slichter. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1937, was presented, and President-Elect Harrison responded to his introduction with a brief address.

Pacific Coast Convention. The 25th Pacific Coast convention was held in Spokane, Washington, August 31 to September 3, 1937, with a registration of 266. Six technical sessions, two student sessions, a joint conference on student activities, reception and dancing, a banquet, inspection trips, a golf tournament, and ladies' events constituted the principal features of the convention. One of the technical sessions was held jointly with the Institute of Radio Engineers.

Winter Convention. The 26th winter convention was held in New York City, January 24–28, 1938, with a technical program including 66 papers in 16 sessions, 4 technical conferences, and 3 addresses. During a general session on Wednesday morning, brief addresses were given by President Harrison, and T. F. Barton, chairman of the winter convention committee, the Alfred Noble Prize was presented to Doctor G. M. L. Sommerman, and Doctor H. G. Moulton, president of the Brookings Institution, gave an address on "Technological Development in Relation to Economics."

At an evening session, the Edison Medal was presented to Past President Gano Dunn; and Stephen F. Voorhees, senior partner of Voorhees, Gmelin and Walker, gave an illustrated lecture on the "New York World's Fair, 1939."

A smoker, numerous inspection trips, and ladies' events completed the program of the convention which had a registration of 1,438.

DISTRICT MEETINGS

Brief reports on the two District meetings held during the year are given below.

North Eastern District Meeting. This meeting was held in Buffalo, N. Y., May 5–7, 1937, with five sessions, including one for student papers, a dinner and a second evening meeting, each with a talk and demonstration, a District conference on student activities, inspection trips, and ladies' events. The registration was 352.

Middle Eastern District Meeting. The meeting of this District was held in Akron, Ohio, October 13–15, 1937, with seven technical sessions, including one for the presentation of student papers, a banquet, dinner and entertainment at Nela Park, inspection trips, conference on student activities, and ladies' events. The registration was 464.

SECTIONS

The Sections maintained unusually keen interest in their activities during the past year. The total number of meetings reported was slightly larger than the number for 1936–37, which far exceeded that for any previous year. Interest in technical groups and other forms of more specialized meetings continued strong, and special efforts were made in some Sections to determine the particular interests of their membership with reference to subjects.

As usual, a considerable number of Sections offered prizes for the best papers presented in competition, and the interest in encouraging student participation in Section programs produced several excellent joint meetings with Branches.

Three new Sections were organized during the year: Wichita in September, Tulsa in October, and Muscle Shoals in February, and each took its place among the more active Sections. These brought the total number to 65.

The name of the Atlanta Section was changed to Georgia Section, and its territory was extended to include the entire state.

Table I. Section and Branch Statistics

	For Fiscal Year Ending			
	April 30, 1932	April 30, 1934	April 30, 1936	April 30, 1938
Sections				
Number of Sections	60	61	61	65
Number of Section meetings held	497	472	540	624
Total attendance	105,325	73,271	85.501	110,148
Branches				
Number of Branches	109	113	118	120
Number of Branch meetings held	1,135	1,015	1,045	1,334
Total attendance	54,197	41,772	45,304	60,446

The Sections committee completed a year's study of the various Section activities in an endeavor to facilitate co-operation between Sections, and to enable the Sections to serve their communities more effectively. Self-analysis blanks were filled out by the Sections, and the results were studied by a subcommittee. The findings of this study were published in the March issue of ELECTRICAL ENGINEERING, pages 134–5.

The Sections committee also made a compilation of the various plans which the different Sections are using in dealing with the local membership problems. A detailed outline was prepared of the more successful plans, and a copy was sent to each Section. The object of this study was to aid the Sections in settling satisfactorily their individual problems concerning local membership.

More detailed information on these activities may be found in the annual report on Section and Branch activities in the June issue of ELECTRICAL ENGINEERING, pages 263–3.

STUDENT ACTIVITIES

A new Branch organized at Northwestern University brought the total number to 120. A large majority of the Branches carried on a substantial amount of activity, but two reported no meetings. The total number of meetings reported was below that for the preceding fiscal year, but larger than the total for any previous fiscal year.

The extensive participation by students in the technical programs of national conventions and District meetings was continued, student sessions having been held as follows: North Eastern District meeting in Buffalo, one, Pacific Coast convention in Spokane, two, and Middle Eastern District meeting in Akron, one.

With the endorsement of the chairman of the committee on Student Branches, the committee on safety sent a letter of October 30, 1937, to the counselors of all Student Branches suggesting that each Branch have presented "a paper dealing with some phase of the problem of the prevention of accidents or remedial measures after an electrical shock." Twenty-four Branches followed this suggestion.

The terms of enrollment of 1,474 Students expired on April 30, 1938, and 703, or about 48 per cent, applied for admission as Associates.

SECTION AND BRANCH STATISTICS

Data on the Sections and Branches are given in table I.

TECHNICAL PROGRAM COMMITTEE

The general review and study of the planning, scope, and procedures of the programs of Institute meetings, which had been carried out during the previous year, resulted in two changes which have been put into effect in the course of the current year and which are believed to be important forward steps.

Advance Copies of Technical Papers. A subcommittee of the technical program committee, under the chairmanship of R. N. Conwell, as a result of their study, recommended last July that "the Institute adopt as a policy the provision of preprints of technical articles submitted as program material." This proposal had been made by the publication committee, and was discussed and approved at the conference of officers, delegates, and members held at the summer convention in Milwaukee. The recommendation was approved by the new technical program committee appointed the first of August, by the publication committee, and, in October 1937, by the board of directors. As a corollary to this recommendation, ELECTRICAL ENGINEERING is relieved of the necessity of publishing all technical papers, but will include about two-thirds of them. The TRANSACTIONS will in the future include all of the formal technical program papers rather than consisting of a reprinting of ELECTRICAL ENGINEERING as in recent years.

The change to the new procedure has been carried out gradually during the year, and the summer convention in Washington will be the first meeting at which the new policy is completely effective. At the winter convention in January, advance copies were provided for 39 out of 65 papers, the others having previously been published in ELECTRICAL ENGINEERING. The advance copies are sold at a nominal price (in addition to a limited free distribution). The response of the membership has been good, and it is anticipated that the average distribution of 212 copies of the advance copies of winter convention papers (not including quantity orders, averaging 40 copies per paper) will be increased as the plan becomes fully effective and as the membership becomes accustomed to it.

General Meetings. The "special committee on Institute activities" arranged for a meeting of broad general interest at the summer convention in Milwaukee, to which a half day was devoted without parallel sessions. At this meeting, Doctor Ralph Flanders addressed the Institute on the subject "The Engineer in A Changing World." This was followed by a discussion by members of the Institute on the subject "How Can Institute Programs Be Made of Greatest Value to the Membership?" This discussion evoked so much interest that it was continued in the afternoon of the following day.

Based upon this experience, the special committee on Institute activities recommended to the board of directors that while "major emphasis should continue to be given to the technical programs substantially along the present lines," the programs of Institute meetings should frequently include "sessions at which subjects of general interest to all the members are discussed." This recommendation was approved by the board in August 1937, and the technical program committee has been guided by it in the programs for the current year.

At the winter convention Doctor Harold G. Moulton, president of the Brookings Institution, Washington, presented an address on "Technological Development in Relation to Economics." His penetrating analysis of this subject proved of great interest to the membership. At the approaching summer convention in Washington, arrangements have been made for a general session to which Wednesday morning will be devoted, at which the Institute will be addressed by Doctor W. R. Gregg, chief of the Weather Bureau, and Colonel J. M. Johnson, assistant secretary of the Department of Commerce on aeronautics. Also, at the business meeting on Tuesday morning, it is hoped that an address will be given on municipal planning in Washington, by an appropriate official.

Technical Programs. Certain information regarding the technical programs for the national conventions and District meetings for the current year in comparison with the previous year is given in table II.

These figures show a marked increase in the Institute's activity. The increase in registration at national conventions and District meetings is especially striking. There was one more convention this year than last (the Pacific Coast convention and summer convention having been combined in 1936), but, even after allowances for that fact, the registration has increased by 20 per cent.

Attention is called to the material decrease in the average length of paper which is, the committee believes, a worth-while achievement. Engineering papers tend to be too long for maximum effectiveness, and in many cases the approval of a paper is delayed by the process of having it shortened. It is hoped that authors will cooperate with the technical committees and the technical program committee in continuing this trend toward shorter papers.

Table II. Technical Programs, Last Two Years

	April 30, 1937 to April 30, 1938	April 30, 1936 to April 30, 1937
Number of sessions	43	37
Number of conferences	8	10
Number of papers presented	155	124
Registration at national conventions and District meetings	3,590	2,720
Average length of paper, pages	$6^{1}/_{2}$	$7^{1}/_{2}$

In comparison with a budget of 1,115 pages for the previous appropriation year, the technical program committee is operating this year on a budget of 1,200 pages of papers recommended for the TRANSACTIONS plus provision for duplicating in small quantities 300 pages of additional material for presentation only. Because of the change in the publication procedure associated with the provision of advance copies for technical meetings, a considerable part of the expense of publishing this year's technical papers will go over into next year's budget. In the approval of papers, however, the technical program committee has laid its plans on the basis of adhering to the allotted budget, since the carry-over of publication expense will, of course, affect next year's budget.

The satisfactory results of the year's work and the real progress which has been made have come about through the loyal co-operation and devoted efforts of the chairmen of the technical committees, the members of the technical program committee, and of other committees co-operating with them, and the untiring zeal of Mr. Rich and other members of the staff involved in the work of the committee.

PUBLICATION COMMITTEE

During the year, there has been put into effect the revised publication policy approved by the board of directors at its October 28, 1937, meeting, and described on page 1409 of the November 1937 issue of ELECTRICAL ENGINEERING. This revised policy has made it possible not only to meet more fully the diversified interests of the membership with reference to ELECTRICAL ENGINEERING and TRANSACTIONS, but also to make available in advance photolithographic copies of manuscripts of technical program papers, and to shorten the time required to make technical program papers available for distribution; all of this, of course, within the limitation of the publication budget. Also a real improvement has been made in the efficiency of operation of the publication office.

At the 1938 winter convention, over 3,600 separate advance copies of technical program papers were sold, and practically all of the comments received with reference to the arrangement as a whole and with reference to the form of the advance copies have been favorable.

One feature of the revised publication policy is that discussions are correlated with their respective papers, both in the TRANSACTIONS section of ELECTRICAL ENGINEERING and in the bound annual volume of TRANSACTIONS. While the new policy was made effective, in general, with the January 1938 issue of ELECTRICAL ENGINEERING, the full effect of the new policy will not be evident until the issuance of the September 1938 number. The change from prepublication of technical program papers in ELECTRICAL ENGINEERING to postpublication naturally could not be made completely in any one issue. Beginning with the April 1938 issue, the discussions are correlated with all the technical program papers published. However, until the September issue, there will appear separate discussions pertaining to technical program papers which were published before the discussions became available.

Table III. Membership Statistics for the Fiscal Year Ending April 30, 1938

	Honorary	Fellow	Member	Six-Year Associate	Associate	Total
Membership on April 30, 1937	10	709	4,118	5,791	4,680	15,308
Additions						
Transferred	1	25	151	614		
New members qualified		1	167	39	1,428	
Former members reinstated		2	14	27	19	
	11	737	4,450	6,471	6,127	17,796
Deductions						
Died	2	16	34	29	11	
Resigned		4	37	153	120	
Transferred		1	17	149	624	
Dropped		6	46	185	284	
Membership on April 30, 1938	9	710	4,316	5,955	5,088	16,078

The improved efficiency in the publication office is permitting more attention to be given to the publishing of a suitable proportion of general interest articles in ELECTRICAL ENGINEERING. This is a continuation of the policy outlined in last year's report.

Early in the year there was brought to a satisfactory conclusion the publication of the "Lightning Reference Book." The attempt to handle this matter on a self-supporting basis was entirely successful, and the form and appearance of this volume have received very favorable comment. As a result of the success of this venture, consideration is being given to the publication of other books on a similar self-supporting basis.

In recognition of the duties and responsibilities he has been carrying regularly for a number of years, F. A. Norris was appointed business manager of ELECTRICAL ENGINEERING, effective November 15, 1937. This involved no change in the salary account of the publication office.

MEMBERSHIP COMMITTEE

The membership committee has continued its activity along lines similar to those of the last few years. An active national committee of 21 members and Section committees or representatives in all of the Sections have continued their work throughout the entire year. Two meetings of the national committee were held in New York, one being at the time of the winter convention to permit a large percentage of the committee to be present.

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending	From Students	From All Others	Total
April 30, 1938	739	932	1,671
April 30, 1937	716	1,040	1,756
April 30, 1936	631	946	1,577
April 30, 1935	575	715	1,290
April 30, 1934	467	496	963

Table V. Number of Enrolled Students

April 30, 1938	5,037	(2,428)
April 30, 1937	4,503	(2,249)
April 30, 1936	4,049	(1,991)
April 30, 1935	3,806	(1,983)
April 30, 1934	3,186	(1,548)

Following the number of Students reported for April 30 of each year is indicated within parentheses the number of new applications received during that year; the difference between this number and the reported total, of course reflects the number of renewals of Student enrollment for the corresponding period.

Continuing the policy of last year in passing along increased responsibility to the District vice-chairmen of the committee, it was recommended, and the board of directors approved the proposal, that the vice-chairmen become members of their respective District executive committees. The vice-chairmen, supported by their respective Section membership committeemen, have worked in close contact with each other to produce a good record for the year.

The general business conditions now affecting a large portion of the industrial areas in the country have had their effects upon the membership returns, as will be noted in table IV wherein it is seen that the total number of applications received is slightly smaller than last year. The hard work of the Section committees has done much to prevent a smaller figure appearing in this column. It is interesting to note in the same table that the applications received from students has, however, slightly increased over last year, in spite of the current economic conditions. The strong efforts made by the committees to induce Enrolled Students whose terms were expiring to apply for admission as Associates has been largely responsible for this figure not falling below last year's report. Though the total number of applications received is less than last year, it must be said, in all fairness, that seven of the ten Districts returned more applications than they did for the year 1936–37.

In spite of the decrease in the number of applications received, the total membership of the Institute has been increased by 770 members. This makes the total membership 16,078, as compared with 15,308 one year ago. This compares with the 708 increase for last year. The number of members dropped during the year, due to failure to meet Institute requirements, was 521, as compared with 526 last year.

Table V shows an interesting increase in the number of Enrolled Students. These men form a very important source of new members for the Institute each year.

Table VI. Number of Members in Section Territory Reinstated

August 1, 1937 to April 30, 1938	306
Year beginning August 1, 1936	503
Year beginning August 1, 1935	663
Year beginning August 1, 1934	831
Year beginning August 1, 1933	741

Table VI indicates a decrease in the number of former members who have been reinstated in Section territory. The number of delinquent members available for reinstatement has been decreasing continuously, which, of course, accounts for the smaller number reinstated each year, but it is also probable that present economic conditions have had their effects in further decreasing the number this year. It will be noted that the figure 306 applies to a nine-month period. The figure for the same months last year was 460.

It is encouraging to note that the record of members who have their dues fully paid as of April 30th has again increased slightly in spite of the conditions now existing. This is shown in table VIII.

Although the Institute's membership is still somewhat less than the peak reached in 1927—see table IX—it has been continually increasing since the low figure of 1935 reached as an effect of the business depression.

DEATHS

The following deaths occurred during the year:

Honorary Members: Guglielmo Marconi, Ambrose Swasey.

Fellows: Philip P. Barton, Byron B. Brackett, Kay A. Christiansen, Clarence L. Cory, Maurice Costa, David F. Crawford, Henry W. Fisher, George E. Hayler, Samuel E. M. Henderson, John W. Howell, Arthur L. Mudge, Peter W. Sothman, Edwin R. Stoekle, Charles W. Stone, Joseph A. Thaler, Norman T. Wilcox.

Members: Harry A. Baker, Clifford W. Bates, Norman M. Baxter, Bennett M. Brigman, Charles Brossman, Frank D. Burr, E. R. Carichoff, Walter Cary, E. S. Code, James A. Correll, H. Milton Dearmin, Edgar D. Edmonston, William B. Folline, Edwin P. Harder, H. Lester Harris, Joseph A. Hepp, William M. Joy, Albert B. Junkins, Emil M. Kaegi, Charles Max, William F. McLaren, Theodore B. Morgan, Edward L. Nichols, Edward J. Pratt, Frank B. Rae, Charles P. Randolph, Ralph T. Rossi, Howard T. Sands, Carl G. Schluederberg, James F. Schnabel, Carroll Shipman, Arthur Townsend, Henry H. Vrooman, Henry H. Wait.

Associates: David C. Bacon, Maurice Barrere, Max A. Berg, Robert C. Brown, Francis E. Cabot, Charles L. Cadle, Paul A. Callis, Robert A. Connor, L. W. Copeland, Harry G. Cotter, Frank L. Dalas, Alfred W. Dater, Lucius F. Deming, Robert J. Deneen, E. A. Enquist, Milton M. Gess, Joseph Goodman, Russell S. Gueffroy, William G. Heptinstall, Edward W. Judy, Emil E. Keller, Thomas H. Kettig, Robert King, Hugh Lesley, Mrs. Zella A. McBerty, George E. McLean, Winfred Morrill, Albert J. J. Murphy, William M. O'Brien, Joseph H. Paget, John Pearson, Benjamin Robinson, William G. Rogers, Michael Romano, Banka A. Roy, Henry W. Taylor, Robert Taylor, Wilbur S. Werner, Philip B. Woodworth, George R. Wright.

COMMITTEE ON TRANSFERS

The committee on transfers gave intensive consideration to the development of desirable methods of encouraging members who are qualified for the higher grades to submit applications for transfer.

The following paragraphs were prepared by the committee for inclusion (with modifications to conform to any amendments to constitution and by-laws that may be adopted) in the general letter sent to Section officers in September of each year by the national secretary:

> "The officers of each Section should consider the matter of encouraging members of the Institute who are fully qualified for the higher grades to submit their applications for transfer, and should set up in their Section means for such encouragement. The means should be appropriate to the situation in the Section. A Section committee on transfers consisting of three or five older men (Fellows and Members), who know the Section membership well, is probably the best. Such a committee continuing on from year to year will come to know the situation and can act with discretion. The Section membership committee can no doubt be of considerable help in this work, especially in encouraging Associate to Member transfers.
>
> "The regulations covering transfer will be found in the constitution, sections 10–14, 16, and by-laws, sections 2–4, 7–9, 13.
>
> "Any questions which any group has should be referred to H. E. Farrer, secretary, board of examiners, AIEE, 33 West 39th Street, New York, N. Y."

Table X contains the numbers of applications for transfer to the grades of Fellow and Member recommended and not recommended by the board of examiners during the past 12 years.

BOARD OF EXAMINERS

The board of examiners held 11 meetings during the past year, averaging about two and one half hours each, and considered 3,956 cases, divided as shown in table XI.

STANDARDS COMMITTEE

The report of the standards committee of a year ago reflected a considerably increased activity in the standardization field. That general high level has continued throughout the year just ended. Several Institute committees have been carrying on much preliminary development work later to be submitted to American Standards Association as the basis of American standards. Likewise, the sectional committees working under ASA procedure have brought to completion many projects that now have the full status of American standards.

At this point, it might be advisable to call attention to an action taken at the October 1937 meeting of the standards committee. A recommendation was made to the directors at that meeting, and later approved by them, suggesting the carrying out by the Institute's technical committees of a survey of the entire electrical standardization field. Such a survey, it was felt, would be the most effective way of determining what fundamental standardization should be under way and how the Institute could most effectively continue to play its rightful part in such work. Concrete indications of the results of the recommendation made are now becoming apparent, although no evidence of fundamental standardization overlooked has been uncovered in any survey reported to date.

A statement of actual detailed actions taken by the standards committee during the year will not be attempted here, as those actions are all matters of record. In general, it has been necessary to make many new committee appointments, because the work of many sectional committees has reached a stage calling for reorganization, and others long inactive have become active. Certain changes in sponsorship of sectional committees also have taken place.

Table VII. Membership of the Institute, April 30, 1938

Of the 16,078 members reported for April 30, 1938, 14,127 are fully paid to April 30, 1938. The balance of 1,951 are divided into the following groups:

1. Members owing dues to April 30, 1937. Total number of members who have not acted upon resolution of board of directors adopted in January 1938 providing an extension of time for payment of these dues	470
2. Members owing dues to April 30, 1938	1,481

(During the period May 1 to 17, 1938, 270 members have paid dues to April 30, 1938, reducing the total to 1,211.)

During the year, two Institute standards, "Relays" and "Automatic Stations," received in revised form the approval of ASA as American standards. Likewise, it was deemed desirable for the Institute to give official approval to the publication of a revision of its existing standard for "Oil Circuit Breakers." This action was taken in order to place immediately in the hands of industry many new data on breakers developed by the committee on protective devices. The revised standard, it is understood, will eventually become part of the American standard for "Oil Circuit Breakers" now in course of development by the sectional committee on power switchgear.

The standards committee put into operation a recommendation received from the committee on co-ordination of institute

Table VIII. Memberships Fully Paid

	Membership as of April 30	Number of Members Fully Paid as of April 30	Per Cent Fully Paid
1938	16,078	14,127	87.9
1937	15,308	13,439	87.8
1936	14,600	12,446	85.2
1935	14,269	11,512	80.5
1927 (year of maximum membership)	18,344	16,247	88.5

activities calling for the organization of a subcommittee to consider the electrical characteristics of bushings. As it is planned to make the personnel of this committee inclusive of all interested groups, it is felt their report will eventually be generally acceptable.

At the February meeting, one subject was discussed, which may prove of great interest to the entire electrical industry. It was suggested that consideration be given to a possible revision of many of the established ideas on the rating of all types of electrical machinery. There would be involved such questions as temperature endurance limits of insulating materials, the classification of new materials, methods of temperature measurement, and rating versus performance under operating conditions. As the first step in the proper approach to these questions, it was agreed that if possible a symposium should be arranged at an Institute convention calling for a thorough discussion of the points outlined, together with the presentation of related data. Arrangements for a symposium are now under way.

UNITED STATES NATIONAL COMMITTEE OF THE IEC

Although no plenary meeting of the International Electrotechnical Commission was held during the past year, meetings of seven advisory committees were held. These covered: section *B* on transformers of advisory committee No. 2 on rating of electrical machinery; No. 8 on standard voltages and currents and high voltage insulators, with special relation to impulse voltages; No. 9 on electric traction equipment; No. 13 on electrical measuring instruments; No. 22 on electronic devices; No. 12 on radio-communications; and No. 7 on aluminum. In addition, a meeting of the committee of action of the IEC was held, at which a number of important decisions with respect to the work of the IEC were taken.

It will be recalled that last year it was reported that consideration was being given to international standardization on acoustics. After full discussion in the committee of action, the IEC decided to recommend that the International Standards Association (ISA) be invited to undertake the general organization of the standardization work in the field of acoustics, the IEC being ready to co-operate with the ISA through an advisory committee on electroacoustics. This recommendation was accepted by the ISA, and a meeting was held in Paris in June 1937 at which a number of important decisions were taken. The most important of these had to do with the acceptance of the American reference level for sound measurements of 10^{-16} watts per square centimeter, thus making this effectively a world standard.

Table IX. Record of AIEE Membership

	Total Membership May 1		Total Membership May 1		Total Membership May 1
1884	71	1903	2,229	1921	13,215
1885	209	1904	3,027	1922	14,263
1886	250	1905	3,460	1923	15,298
1887	314	1906	3,870	1924	16,455
1889	333	1907	4,521	1925	17,319
1890	427	1908	5,674	1926	18,158
1891	541	1909	6,400	1927	18,344
1892	615	1910	6,681	1928	18,265
1893	673	1911	7,117	1929	18,133
1894	800	1912	7,459	1930	18,003
1895	944	1913	7,654	1931	18,334
1896	1,035	1914	7,876	1932	17,550
1897	1,073	1915	8,054	1933	17,019
1898	1,098	1916	8,202	1934	15,200
1899	1,133	1917	8,710	1935	14,269
1900	1,183	1918	9,282	1936	14,600
1901	1,260	1919	10,352	1937	15,308
1902	1,549	1920	11,345	1938	16,078

The committee of action also undertook a study of the most effective manner of handling within the IEC standardization work on co-ordination of insulation. The various advisory committees concerned have been contacted and the opinion of the various national committees sought. A decision in the matter will be taken shortly.

New IEC projects on electric welding—No. 26—and electroheating—No. 27—have been initiated recently.

New Publications. New publications issued during the past year include a new IEC specification for a-c circuit breakers (Publication No. 56) which covers definitions, rules for rating, and rules for type tests for a-c circuit breakers; an appendix to IEC Publication No. 46 on steam turbines covering supplementary notes to section 4 of instruments and methods of measurement of the rules for acceptance tests. Also a draft of a revised edition of Publication No. 38 on standard voltages was issued.

Table X. Applications for Transfer During the Past 12 Years

	Fellow Grade			Member Grade		
Year Ending April 30	Recommended	Not Recommended	Total	Recommended	Not Recommended	Total
1927	30	5	35	293	32	325
1928	21	3	24	280	17	297
1929	45	2	47	229	19	248
1930	28	2	30	211	29	240
1931	44	3	47	322	31	353
1932	7	2	9	149	17	166
1933	29	2	31	109	11	120
1934	25	2	27	154	3	157
1935	19	2	21	199	23	222
1936	27	1	28	205	24	229
1937	24	2	26	167	27	194
1938	26	0	26	137	7	144
Totals	325	26	351	2,455	240	2,695

International Vocabulary—No. 1. The International Electrotechnical Vocabulary, which it was expected would be available during the past year, entailed so much work that it was not possible to have the first edition ready for distribution. It is now hoped, however, that it will be available before the end of the current year.

Radio Interference. Several meetings of the international special committee on radio interference were held, and a considerable volume of work done concerning the methods of measuring radio noise which are used in the different countries. This work has resulted in the development of a standard measuring apparatus which is being manufactured in Belgium and sold to the various countries interested. Consideration is being given in this country to securing this apparatus for test in this country. After the results obtained by the use of this apparatus are available to the international special committee, discussions of the results on a comparable basis will be possible.

Table XI. Applications for Admission and Transfer

Applications for Admission		
Recommended for grade of Associate	964	
Re-elected to the grade of Associate	119	
Not recommended	16	1,099
Recommended for grade of Member	140	
Re-elected to the grade of Member	28	
Not recommended	34	202
Recommended for grade of Fellow	1	
Re-elected to the grade of Fellow	—	
Not recommended	1	2
Applications for Transfer		
Recommended for grade of Member	159	
Not recommended	11	170
Recommended for grade of Fellow	31	
Not recommended	—	31
Students		
Recommended for enrollment as Students		2,452
Total		3,956

Plenary Meeting. A plenary meeting of the IEC is now scheduled to be held in Torquay and London, England, June 22 to July 1. At this time meetings of 21 advisory committees, as well as of the international committee mixed on traction, the committee of action and the council of the IEC will be held. As would be expected, much of the work of the advisory committees during the past year has been devoted to preparations for this complete series of meetings. The United States national committee will be well represented, its total delegation probably numbering about 22 individuals, including the president and secretary of the USNC. In addition, James Burke, the president of the IEC, who is an American, will be in attendance.

COMMITTEE ON SAFETY

In line with the change in the by-laws of the Institute, the committee on safety has, with an enlarged program, replaced the committee on safety codes which had been in existence for a number of years, and had rendered valuable service to the Institute. It was felt that the time had come to enlarge the functions of this committee and hence the change in the by-laws and organization were approved.

The committee held three meetings during the year, and has instituted a program based primarily on education. Through co-operation with the appropriate committees of the Institute, letters were forwarded to the counselors of Student Branches and to the chairmen of Sections, recommending that during the year at least one meeting should be devoted to the reception and discussion of a paper dealing with some phase of safety or remedial measures after electrical shock. As a result of these letters, a considerable number of Student Branches and Sections have had papers presented to them and interesting discussions of these papers have taken place. In the case of other Sections and Student Branches, their programs for the year were completed when the committee's letter was received, but arrangements have been made that the matter will receive consideration during next year. From the letters received from the Sections which had these papers presented to them, it is quite apparent that there is a very active interest in this subject among the members and among the students.

Letters to professors of electrical engineering in some 120 universities and colleges were sent out, recommending that at least the senior students receive instruction and training in artificial respiration, as they would be required to have this knowledge on entering industry. As a result of these letters, active training of senior students has been either increased or instituted in more than half of these colleges. As a result of the answers to these letters, it was found that, in some colleges and universities, courses in accident prevention are carried out, in some instances on a voluntary basis, but in other instances being required for the degree. The committee is very appreciative of the general co-operation given by the professors of electrical engineering.

Arrangements are on foot to have presented to the Institute papers on specific subjects dealing with the prevention of accidents. One paper that has been generally requested is at present in preparation.

Representatives of the committee have attended meetings of the National Fire Waste Council, the National Fire Protection Association, and a meeting of representatives of safety committees in engineering associations called by the engineering section of the National Safety Council.

In laying down a basic principle, the committee feels that a design to be efficient must be safe to construct, operate, and maintain, and that electrical engineers not only deal with material things, but also are in many cases responsible for the organization, training, and direction of men in the construction, installation, and operation of plant and equipment. For this reason, they are vitally interested in the protection of these men from accident.

Every endeavor has been made in carrying out the work of the committee to keep it practical and on a sound foundation upon which more extensive programs can, in the future, be built.

Co-ordination Committee

In addition to its duties concerned with the national and District meetings referred to it by the board of directors, the committee has given serious attention, at the request of the president, to the problem of long range planning for the Institute as a whole. It has recommended to the board a reconstitution of the committee, to be called the committee on planning and co-ordination, and alterations in the by-laws which would assign to this new committee the task of long range planning studies, with particular reference to the interests of special groups within the Institute membership, as well as the responsibilities of the present committee.

Institute Policy Committee

The Institute policy committee was appointed in accordance with the requirements in the by-laws, and was ready to consider any matters that might be brought to its attention. However, the board of directors did not find it necessary to refer any questions to the committee.

Committee on Code of Principles of Professional Conduct

The committee has undertaken no revision of the code and no proposals of this sort have been submitted to the committee.

Committee on Constitution and By-Laws

This committee conducted its work by correspondence, and considered and recommended several proposed amendments to the constitution and by-laws of the Institute.

Committee on Economic Status of the Engineer

The committee has met at convenient times during conventions. The committee members have also carried on correspondence relating to the work of the committee. At a meeting held during the winter convention, the committee had for discussion a considerable amount of data, largely in the form of bulletins published by the Bureau of Labor Statistics of the United States Department of Labor. Also the committee had available, by virtue of C. F. Scott's membership on the committee, the accumulated experience of Engineers' Council for Professional Development. As data available were reviewed and discussed, the chairman of the committee made the remark: "The economic status of the engineer is largely a matter determined by each individual engineer according to his particular personal qualifications and the relations these bear to the work he does and to the personalities of those persons with whom and by whom he is employed." Whereupon he was instructed by the committee to write a paper setting forth the reasons leading to the comment made. That paper has been submitted to the technical program committee for use at the summer convention in June, and may be referred to as a part of this report.

Committee on Award of Institute Prizes

Four national and 12 District prizes were awarded in 1937 for papers presented in the calendar year 1936 and for student papers presented during the academic year ending June 30, 1937. These awards were announced in the issues of Electrical Engineering for June, September, October, and November 1937.

The committee considered a large number of papers and the gradings and recommendations of the technical committees which had reviewed the papers. Many papers considered were of a high quality, and, in addition to the national prizes, ten other papers were given honorable mention.

Copies of a revised edition of the pamphlet "National and District Prizes," containing revisions recommended by the committee and approved by the board of directors, were distributed in August 1937.

Committee on Award of Columbia University Scholarships

During February and March 1937, 15 inquiries regarding Columbia University scholarships were received from interested students in some ten institutions. Application forms and information were sent to each of these, but not a single formal application for the scholarship was received.

This is ascribed to the fact that, during May and June 1937, the industries made such a demand for technical graduates that all Columbia University graduates obtained positions in industry immediately upon graduating, and many had been engaged long before commencement.

Edison Medal

The Edison Medal, which is awarded by a committee composed of 24 members of the Institute, was, for 1937, awarded to Gano Dunn "for distinguished contributions in extending the science and art of electrical engineering, in the development of great engineering works, and for inspiring leadership in the profession," and was presented on January 26, 1938, during the winter convention. The medal may be awarded annually "for meritorious achievement in electrical science, electrical engineering, or the electrical arts."

John Fritz Medal

The John Fritz Medal board of award, composed of representatives of the national societies of civil, mining, mechanical, and electrical engineers, awarded the 34th medal (for 1938) to Doctor Paul Dyer Merica, vice-president, International Nickel Company, for "important contributions to the development of alloys for industrial uses."

Lamme Medal

The Lamme Medal committee awarded the medal for 1937 to Doctor Robert E. Doherty, president, Carnegie Institute of Technology, "for his extension of the theory of a-c machinery, his skill in introducing that theory into practice, and his encouragement of young men to aspire to excellence in this field." Arrangements are being made for the presentation of the medal at the annual summer convention in Washington, D. C., June 20–24, 1938. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery."

Alfred Noble Prize

This prize, established in 1929, consists of a certificate and a cash award of $500 from the income from a fund contributed by engineers and others to perpetuate the name and achievements of Alfred Noble, past-president of the American Society of Civil Engineers and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1937 was presented to Doctor

G. M. L. Sommerman, research engineer, American Steel & Wire Company, Worcester, Mass.

Washington Award

The Washington Award for 1938 was bestowed upon Doctor Frank B. Jewett, for "inspiring and directing scientific research leading to improvements in the art of communication," and was presented to him at a dinner on May 5, 1938. This award may be made annually to an engineer by the commission of award composed of nine representatives of the Western Society of Engineers and two each of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE.

Hoover Medal

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The last award was made to Doctor Ambrose Swasey in 1936.

Iwadare Foundation Committee

No Iwadare lecturer was chosen to go to Japan for the current year. One Iwadare fellow, Kiyoshi Abe, assistant professor of Kyoto Imperial University, is at present in the United States.

Employment Service

The Institute co-operates with the national societies of civil, mining, and mechanical engineers in the operation of the Engineering Societies Employment Service with its main office in the Engineering Societies Building, New York. Offices are operated in Chicago and San Francisco also. In addition to the societies named, others cooperate in certain of the offices as follows: New York—Society of Naval Architects and Marine Engineers; Chicago—Western Society of Engineers; San Franciso—California Section of the American Chemical Society; and the Engineers' Club of San Francisco.

The service is supported by the joint contributions of the societies and their individual members who are benefited. In addition to the publication of the employment service announcements monthly in Electrical Engineering, weekly subscription bulletins are issued for those seeking positions.

An analysis of this employment service as reported to the national societies is given in table XII.

Table XII. Analysis of Employment Service

Month	Men Registered				Men Placed			
	New York	Chicago	San Francisco	Total	New York	Chicago	San Francisco	Total
1937								
May	288	82	89	459	51	32	25	108
June	310	99	108	517	53	33	26	112
July	233	92	59	384	50	34	16	100
August	206	82	53	341	49	31	25	105
September	205	77	64	346	54	22	16	92
October	212	71	74	357	51	18	22	91
November	185	97	80	362	48	8	12	68
December	179	91	66	336	34	3	13	50
1938								
January	244	90	73	407	37	10	10	57
February	216	58	68	342	30	8	17	55
March	237	96	101	434	33	8	16	57
April	229	135	92	456	25	9	17	51
Total	2,744	1,070	927	4,741	515	216	215	946

American Engineering Council

The American Engineering Council has continued its activities in the wide range of affairs which are found within the scope of its objectives: "to further the public welfare wherever technical and engineering knowledge and experience are involved, and to consider and act upon matters of common concern to the engineering and allied technical professions," and within the interests of its many member-bodies.

The 18th annual meeting of the assembly was held in Washington, D. C., January 13–15, 1938. The eighth conference of engineering society secretaries was held on the 13th.

The four major themes occupying the attention of the assembly were the engineer's economic status, the evaluation of technology, planning of public and private enterprise, and government reorganization. Reports of officers and standing and special committees were presented. Much attention was given to efforts to clarify the objectives and procedures of the Council in order to give engineers a more definite place in the consideration of public questions.

The discussions and actions at the annual meeting and subsequent actions by the executive committee gave the Council a modified program for 1938, consisting of the five principal functions:

I. Public Affairs—Contacts between the engineering profession and the federal government.

II. Public Discussion—Holding of forums, in cooperation with member bodies, for discussion of public problems involving engineering.

III. Engineers' Embassy—Service to members on engineering matters involved in federal government activities.

IV. Publicity—Regular reporting upon above mentioned functions to member bodies.

V. Fact Finding—Surveys and investigations to determine the effects of technology upon employment, relation of engineering to economics in public questions, etc.

A more complete statement of the 1938 program may be found in the May 1938 issue of Electrical Engineering, page 228, and accounts of activities in progress appeared in various issues of that publication during the past year.

United Engineering Trustees, Inc.

This organization manages the Engineering Societies Building and administers certain joint funds for the four founder societies. The American Institute of Chemical Engineers has moved its headquarters into the building, which is now fully occupied.

Provisions have been made for the resumption of annual additions to the depreciation and renewal fund.

A new edition of the "History, Charter, and By-laws" was issued in 1937.

An abstract of the annual report of the United Engineering Trustees, Inc., was published in Electrical Engineering for December 1937, page 1529.

Engineering Foundation

The Engineering Foundation is a joint organization of the national societies of civil, mining and metallurgical, mechanical, and electrical engineers established for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind."

The foundation suffered a serious loss in the death of Doctor Ambrose Swasey, its founder, on June 15, 1937.

The foundation has been assisting in a wide range of technical researches sponsored by the founder societies. Some of the principal groups now in progress are: ASCE—soil mechanics and foundations, hydraulics; AIME—alloys of iron, barodynamic problems; ASME—critical pressure steam boilers, fluid meters, lubrication, cottonseed processing, plasticity; AIEE—stability of impregnated paper insulation; AIEE and AWS—welding; University of California—plastic flow of concrete.

Assistance in nontechnical matters related to engineering has been granted to the Engineers' Council for Professional Development and the Personnel Research Federation.

An abstract of the annual report of the Engineering Foundation was published in the December 1937 issue of Electrical Engineering, pages 1529–30.

Engineering Societies Library

The Engineering Societies Library, which was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, and the preparation of a composite card catalog, has been expanded as a single engineering library, which probably constitutes the best collection of this type of literature in the United States.

On September 30, 1937, the library had 141,193 volumes, 7,281 maps, and 4,362 bibliographies. Books, pamphlets, and maps totaling 11,003 were received during the year ending at that time. Current issues of 1,416 periodicals were received. Work progressed rapidly on a classified index to periodicals, and the index now contains more than 187,000 references to articles published since 1927.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail, etc.

An abstract of the annual report of the library was published on page 1530 of ELECTRICAL ENGINEERING for December 1937.

ENGINEERS' COUNCIL FOR PROFESSIONAL DEVELOPMENT

This council was organized in 1932 to engage in activities leading to the enhancement of the professional status of the engineer. It includes three representatives of each of the seven participating organizations: the national societies of chemical, civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, and the National Council of State Boards of Engineering Examiners.

The principal activities of ECPD include programs for the guidance of young individuals thinking of entering the engineering field, the accrediting of curricula of engineering schools, encouragement and assistance to individuals in their engineering and cultural studies during several years after graduation, and the development of criteria for indicating the attainment of the status of an engineer.

At the annual meeting held on October 1, 1937, many additional curricula of engineering schools were accredited, bringing the total number to 445 in 107 schools. The complete list appeared in the November 1937 issue of ELECTRICAL ENGINEERING, page 1418. Seventy-one of the 445 curricula were accredited for limited periods, and will be re-examined as those periods expire. Prior to the date of the annual meeting, the committee on engineering schools had prepared recommendations on 626 curricula in 129 institutions.

Comprehensive excerpts from the reports of the committees on student selection and guidance, engineering schools, and professional training as presented at the annual meeting were published in the November 1937 issue of ELECTRICAL ENGINEERING, pages 1416–19. The report of the committee on professional recognition was referred back to the committee.

At its meeting held on October 28, 1937, the board of directors of the Institute disapproved the recommendations of ECPD: (1) That the minimum definition of an engineer adopted by ECPD be applied as a minimum requirement for admission to the professional grades of membership, and evidence of professional education also be required; and (2) that membership grades be adjusted to conform to the ECPD standard grades of membership.

REPRESENTATIVES

The Institute has continued its representation upon many joint committees and national bodies, with which it co-operates in a wide range of activities of interest and importance to engineers and others.

A list of representatives was published in the September 1937 issue of ELECTRICAL ENGINEERING and in the 1938 Year Book.

FINANCE COMMITTEE

The committee, as usual, recommended a detailed budget to the board of directors, passed upon the expenditures for various purposes, made recommendations regarding delinquent members, and performed the other duties prescribed for it in the constitution and by-laws.

Haskins and Sells, certified public accountants, have audited the books, and their report follows.

The year has been so productive of constructive work throughout every activity of the Institute that the board of directors extends to the District and Section officers, the national committees, and the membership its sincere appreciation of their continuing interest, untiring efforts, and effective services.

Respectfully submitted for the board of directors.

H. H. HENLINE,
National Secretary

May 26, 1938

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

22 EAST 40TH STREET
NEW YORK

May 24, 1938

American Institute of Electrical Engineers,
33 West 39th Street,
New York.

Dear Sirs:

We have made an examination of your balance sheet as of April 30, 1938, and of your recorded cash receipts and disbursements for the year ended that date. In connection therewith, we examined or tested your accounting records and other supporting evidence in a manner and to the extent which we considered appropriate in view of your system of internal accounting control. We present the following financial statements:

Balance Sheet, April 30, 1938 (Exhibit A).
Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value (Schedule 1).

Statement of Recorded Cash Receipts and Disbursements of General Fund for the Year Ended April 30, 1938 (Exhibit B).

Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1938 (Exhibit C).

In accordance with the terms of our engagement, members and other debtors were not requested to confirm to us the amounts receivable from them at April 30, 1938, and, in accordance with the usual practice of the Institute, no provision has been made for dues which may prove to be uncollectible.

In our opinion, based upon such examination and subject to the comments in the foregoing paragraph, the accompanying Exhibit A fairly presents your financial condition at April 30, 1938, and the accompanying Exhibits B and C set forth your recorded cash receipts and your disbursements of funds, as indicated, for the year ended that date.

Yours truly,

HASKINS & SELLS

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet, April 30, 1938

Exhibit A

ASSETS		
Property Fund Investments:		
One-fourth interest in real estate and other assets of United Engineering Trustees, Inc., exclusive of trust funds	$498,448.48	
Equipment:		
Library—volumes and fixtures	37,296.37	
Office furniture and fixtures (less reserve for depreciation, $28,749.43)	5,695.90	
Works of art, etc	3,001.35	
Securities—at cost (market quotation value, $8,997.53)—Schedule 1	10,042.53	
Cash (see Exhibit C)	22.47	
Total property fund investments		$554,507.10
Restricted Fund Investments:		
Securities—at cost (market quotation value, $180,615.66) less reserve for securities of doubtful value—Schedule 1	$206,824.65	
Cash (see Exhibit C)	5,070.95	
Accrued interest receivable	209.16	
Total restricted fund investments		212,104.76
Current Assets:		
Cash (see Exhibit B)	$ 49,616.89	
Accounts receivable:		
Members—for dues	18,753.01	
Advertisers	140.00	
Miscellaneous	2,124.39	
Accrued interest on investments	2,591.98	
Inventories:		
TRANSACTIONS, etc	2,082.25	
Text and cover paper	7,231.60	
Work in process (May issue of ELECTRICAL ENGINEERING)	3,443.04	
Badges	1,028.55	
Total current assets		87,011.71
Total		$853,623.57

LIABILITIES		
Property Fund Reserve		$554,507.10
Restricted Fund Reserves:		
Reserve Capital Fund	$192,002.66	
Life Membership Fund	9,121.52	
International Electrical Congress of St. Louis Library Fund	5,357.82	
Lamme Medal Fund	4,578.03	
Mailloux Fund	1,044.73	
Total restricted fund reserves		212,104.76
Current Liabilities—Accounts Payable		6,079.74
Deferred Income:		
Dues received in advance	$ 4,509.21	
Entrance fees and dues advanced by applicants for memberships	659.04	
Deferred credits and other unallocated receipts	444.82	
Subscriptions for TRANSACTIONS received in advance	40.00	
Reserve for prepaid subscriptions for ELECTRICAL ENGINEERING	8,500.00	
Total deferred income		14,153.07
Surplus		66,778.90
Total		$853,623.57

Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value, April 30, 1938

Exhibit A, Schedule 1

	Face Value of Bonds or Number of Shares of Stock	Property Fund (Equipment Replacement)	Restricted Funds					
			Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total
Railroad Bonds:								
Alleghany Corporation 20-year collateral trust convertible 5%, due 1949	$15,000.00		$ 10,627.50					$ 10,627.50
Baltimore & Ohio Railroad Company 6% refunding and general mortgage series C, due 1995	12,000.00		8,940.00			$4,330.00		13,270.00
Central of Georgia Railway Company 5% consolidated mortgage, due 1945	3,000.00		1,477.50					1,477.50
Chicago, Burlington & Quincy Railroad Company 5% first and refunding mortgage series A, due 1971	1,000.00		1,010.00					1,010.00
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00		1,105.00					1,105.00
Chicago & Northwestern Railway Company 6 1/2%, due March 1, 1936	9,000.00		7,202.50					7,202.50
Cleveland Union Terminals Company 5% sinking fund series B, due 1973	4.000.00	$ 4,010.00						
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974 (certificates of deposit)	10.000.00		9,818.75					9,818.75
New York Central Railroad Company 5% refunding and improvement mortgage series C, due 2013	6,000.00		5,742.50					5,742.50
Northern Pacific Railway Company 6% refunding and improvement mortgage series B, due 2047	10,000.00		10,962.50					10,962.50
Pennsylvania Railroad Company 30-year secured serial 4%, due 1944	6,000.00		5,337.50		$1,067.50			6,405.00
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00		5,497.50					5,497.50
Southern Pacific Company Oregon Lines 4 1/2% first mortgage series A, due 1977	1,000.00				996.25			996.25
Texas and Pacific Railway Company general and refunding series B 5%, due 1977	5,000.00			$5,306.25				5,306.25
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00		7,225.00					7,225.00
Total railroad bonds—(Forward)		$ 4,010.00	$ 74,946.25	$5,306.25	$2,063.75	$4,330.00		$ 86,646.25

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Property and Restricted Funds Securities, Less Reserve for Bonds of Doubtful Value, April 30, 1938

Exhibit A, Schedule 1 (Concluded)

			Restricted Funds					
	Face Value of Bonds or Number of Shares of Stock	Property Fund (Equipment Replacement)	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total
TOTAL RAILROAD BONDS—(Forward)		$ 4,010.00	$ 74,915.25	$5,308.25	$2,063.75	$4,330.00		$ 86,646.25
Public Utility Bonds:								
American Gas & Electric Company 5% debenture, due 2028	$ 9,000.00		$ 9,596.25					$ 9,596.25
Georgia Power Company first and refunding mortgage 5%, due 1967	10,000.00		9,725.00					9,725.00
Monongahela-West Pennsylvania Public Service Company 6% debentures, due 1965	8,000.00		8,660.00					8,660.00
New York Telephone Company first and general mortgage $4^{1}/_{2}$%, due 1939	1,000.00						$1,000.00	1,000.00
Philadelphia Company secured 5% series A, due 1967	10,000.00		10,000.00					10,000.00
Texas Electric Service Company 5% first mortgage, due 1960	10,000.00		9,838.75					9,838.75
Total public utility bonds			$ 47,820.00				$1,000.00	$ 48,820.00
Industrial and Miscellaneous Bonds, Etc.:								
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.), 4%, due 1944	$14,663.00	$ 977.53	$ 13,685.47					$ 13,685.47
General Motors Acceptance Corporation $3^{1}/_{4}$%, due 1951	7,000.00		7,140.00					7,140.00
New York Steam Corporation 6% first mortgage, due 1947	10,000.00		10,837.50					10,837.50
Total industrial and miscellaneous bonds, etc		$ 977.53	$ 31,662.97					$ 31,662.97
Municipal Bonds:								
City of Detroit public lighting $4^{1}/_{2}$% series A, due 1945	$10,000.00		$ 10,262.50					$ 10,262.50
New York City $4^{1}/_{2}$% corporate stock, due 1957	2,000.00				$2,204.05			2,204.05
Total municipal bonds			$ 10,262.50		$2,204.05			$ 12,466.55
United States Government Bonds and Notes:								
Federal Farm Mortgage 3%, due 1949/44	$12,000.00		$ 12,405.00					$ 12,405.00
Treasury Bonds $3^{1}/_{4}$%, due 1941	10,000.00		10,650.00					10,650.00
Treasury Bonds $2^{3}/_{4}$%, due 1947/45	10,000.00		10,409.38					10,409.38
Treasury Notes 2% series B, due September 15, 1942	8,000.00		8,037.50					8,037.50
Total United States Government bonds and notes			$ 41,501.88					$ 41,501.88
Capital Stocks:								
Commonwealth Edison Company	48 shares		$ 2,892.00					$ 2,892.00
Commercial Investment Trust Corporation $4^{1}/_{4}$% preferred, series of 1935	100 "		10,100.00					10,100.00
Consolidated Edison Company of New York, Inc. $5.00 cumulative preferred	30 "	$ 3,060.00						
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 "		2,319.15					2,319.15
Public Service Corporation of New Jersey $5.00 preferred	30 "		2,958.75					2,958.75
United Gas Improvement Company $5.00 preferred	30 "	1,995.00	997.50					997.50
Total capital stocks		$ 5,055.00	$ 19,267.40					$ 19,267.40
Total		$10,042.53	$225,461.00	$5,306.25	$4,267.80	$4,330.00	$1,000.00	$240,365.05
Less Reserve for Securities of Doubtful Value:								
Central of Georgia Railway Company 5% consolidated mortgage, due 1945	$ 3,000.00		$ 1,477.50					$ 1,477.50
Chicago & Northwestern Railway Company $6^{1}/_{2}$%, due March 1, 1936	9,000.00		7,202.50					7,202.50
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974	10,000.00		9,818.75					9,818.75
International Match Realization Company, Ltd. voting trust certificates for capital shares of International Match Corporation	6 shares		2,319.15					2,319.15
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950	$ 6,000.00		5,497.50					5,497.50
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00		7,225.00					7,225.00
Total reserve for bonds of doubtful value			$ 33,540.40					$ 33,540.40
Total securities, less reserve		$10,042.53	$191,920.60	$5,306.25	$4,267.80	$4,330.00	$1,000.00	$206,824.65
Total Property Fund Securities		$10,042.53						
Total Restricted Fund Securities			$191,920.60	$5,306.25	$4,267.80	$4,330.00	$1,000.00	$206,824.65

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Recorded Cash Receipts and Disbursements of General Fund for the Year Ended April 30, 1938

Exhibit B

Cash on Deposit With the National City Bank of New York, May 1, 1937		$ 51,700.32
Receipts:		
Dues (including $84,762.00 allocated to ELECTRICAL ENGINEERING subscriptions)	$193,344.84	
Advertising	34,842.44	
TRANSACTIONS subscriptions	7,278.99	
ELECTRICAL ENGINEERING subscriptions	14,483.41	
Miscellaneous publications, etc	12,237.45	
Student fees	12,340.50	
Entrance fees	7,798.02	
Membership badges	1,849.21	
Transfer fees	870.00	
Interest on investments, less purchased interest	9,446.50	
Miscellaneous	495.07	
Total receipts		294,986.43
Total		$346,686.75
Disbursements:		
Publication expense:		
ELECTRICAL ENGINEERING	$ 89,285.12	
TRANSACTIONS	6,125.37	
Preprints—technical papers	2,228.72	
YEAR BOOK	6,452.59	
Miscellaneous publications, etc	8,437.87	
Institute meetings	13,537.95	
Institute Sections	34,130.54	
Institute Branches	2,671.66	
Edison Medal committee	197.46	
Finance committee	1,600.00	
Headquarters committee	95.25	
Membership committee	7,642.85	
Standards committee	6,827.46	
Technical committee	171.96	
Forward	$179,404.80	$346,686.75

Total—(Forward)		$346,686.75
Disbursements—(Forward)	$179,404.80	
Traveling expenses:		
Geographical Districts:		
Executive committees	1,947.77	
Vice-presidents	246.03	
Branch counselors	6,550.60	
President's appropriation	821.76	
Board of directors	4,396.59	
National nominating committee	1,062.55	
Administrative expenses	45,564.12	
Geographical Districts—best paper prizes	141.50	
Geographical Districts—initial paper prizes	26.50	
National prizes	6.00	
American Engineering Council	12,000.00	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	11,022.64	
Library assessment	9,313.83	
Library stacks	1,500.00	
Engineering Societies employment service	1,332.96	
Engineers' Council for Professional Development	415.00	
John Fritz Medal	50.00	
National Fire Protection Association—dues	60.00	
United States Committee of International Commission on Illumination	300.00	
Membership badges	1,650.85	
Legal services	250.00	
Retirement salary	2,250.00	
Miscellaneous	256.36	
Transfers to reserve capital fund	15,000.00	
Total disbursements		297,069.86
Cash on Deposit With The National City Bank of New York, April 30, 1938		$ 49,616.89

Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1938

Exhibit C

		Restricted Funds					
	Property Fund (Equipment Replacements)	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total Restricted Funds
Cash on Deposit, May 1, 1937, With East River Savings Bank and The National City Bank of New York	$22.47	$ 306.40	$4,546.42	$ 938.62	$137.33	$48.92	$ 5,977.69
Receipts:							
Interest on bonds, and dividends on stocks			$250.00	$175.00	$240.00	$45.00	$710.00
Interest on bank balances			71.27				71.27
Proceeds from sale of securities		$33,417.54					33,417.54
Life membership fees			404.09				404.09
Transfers from general fund		15,000.00					15,000.00
Total receipts		$48,417.54	$ 725.36	$ 175.00	$240.00	$45.00	$49,602.90
Total	$22.47	$48,723.94	$5,271.78	$1,113.62	$377.33	$93.92	$55,580.59
Disbursements:							
Annual withdrawal authorized in by-laws			$1,477.34				$ 1,477.34
Gold and bronze replicas of Lamme Medal and certificate					$229.30		229.30
Purchase of securities		$48,641.88					48,641.88
All other disbursements				$ 89.43		$71.69	161.12
Total Disbursements		$48,641.88	$1,477.34	$ 89.43	$229.30	$71.69	$50,509.64
Cash on Deposit, April 30 ,1938, With East River Savings Bank and The National City Bank of New York	$22.47	$ 82.06	$3,794.44	$1,024.19	$148.03	$22.23	$ 5,070.95

Report of the Board of Directors

For the Fiscal Year Ending April 30, 1939

THE board of directors of the American Institute of Electrical Engineers presents herewith to the membership its 55th annual report, for the fiscal year ending April 30, 1939. A general balance sheet showing the condition of the Institute's finances on April 30, 1939 together with other detailed financial statements, is included herein. This report contains a brief summary of the principal activities of the Institute during the year, more detailed information having been published from month to month in ELECTRICAL ENGINEERING.

BOARD OF DIRECTORS' MEETINGS

During the year, the board of directors held five meetings, four in New York City, and one in Washington, D. C. The executive committee meetings in December and March were held in place of regular meetings of the board. Information regarding many of the more important activities of the Institute which have been under consideration by the board of directors and the committees is published each month in the section of ELECTRICAL ENGINEERING devoted to "News of Institute and Related Activities."

PRESIDENT'S AND NATIONAL SECRETARY'S VISITS

President Parker and Secretary Henline attended the Pacific Coast and winter conventions, the Southern District meeting in Miami, and the South West District meeting in Houston, Texas. They also visited many Sections and Student Branches. During May and June, they will attend the North Eastern District meeting in Springfield, Mass., and the combined Summer and Pacific Coast convention in San Francisco, Calif. Visits will be made to the following Sections: Cincinnati, Cleveland, Erie, Mansfield, Rochester, Toledo, and Worcester.

The places visited by President Parker are listed below:

- **Alabama**
  - Joint meeting Alabama, East Tennessee, Memphis, and Muscle Shoals Sections, Huntsville
  - Alabama Section, Birmingham
  - University of Alabama Branch
- **Colorado**
  - Denver Section
- **District of Columbia**
  - Conference on student activities, District No. 2
  - Washington Section
  - American Engineering Council
- **Florida**
  - Southern District meeting, Miami
- **Georgia**
  - Georgia Section, Atlanta
- **Illinois**
  - Chicago Section
- **Louisiana**
  - New Orleans Section
  - Tulane University Branch
- **Maryland**
  - Baltimore and Washington Sections, joint meeting, Baltimore
  - Johns Hopkins University Branch, and representatives of Catholic University of America, George Washington University, and University of Maryland Branches, Baltimore
- **Massachusetts**
  - Boston Section
  - Worcester Section
  - Worcester Polytechnic Institute
  - Massachusetts Institute of Technology Branch and representatives of Tufts, Northeastern, and Harvard University Branches, Cambridge
- **Michigan**
  - Michigan Section, Detroit
  - University of Michigan Branch
- **Missouri**
  - Kansas City Section
  - St. Louis Section
- **Nebraska**
  - Nebraska Section, Omaha
- **New York**
  - New York Section
  - Winter convention
- **Oregon**
  - Pacific Coast convention, Portland
- **Pennsylvania**
  - Philadelphia Section
  - Pittsburgh Section (jointly with neighboring Branches)
- **Tennessee**
  - Memphis Section
- **Texas**
  - South West District meeting, Houston
- **Utah**
  - Utah Section, Salt Lake City
- **Virginia**
  - Virginia Section, Richmond
  - Virginia Military Institute Branch
  - Virginia Polytechnic Institute Branch
  - University of Virginia Branch
- **Canada**
  - Annual meeting, Engineering Institute of Canada, Ottawa

The places visited by the national secretary are the following:

- **Arkansas**
  - University of Arkansas Branch
- **District of Columbia**
  - Conference on student activities, District No. 2
  - Washington Section
  - American Engineering Council
- **Florida**
  - Southern District meeting, Miami
- **Georgia**
  - Georgia Section, Atlanta
- **Kansas**
  - Wichita Section
- **Kentucky**
  - Louisville Section
  - University of Kentucky Branch
- **Maryland**
  - Baltimore and Washington Sections, joint meeting, Baltimore
  - Johns Hopkins University Branch, and representatives of Catholic University of America, George Washington University, and University of Maryland Branches, Baltimore
- **Massachusetts**
  - North Eastern District executive committee meeting, Pittsfield
- **New York**
  - New York Section
  - Winter convention
- **Oregon**
  - Pacific Coast convention, Portland
- **Pennsylvania**
  - Philadelphia Section
- **Texas**
  - South West District meeting, Houston
  - San Antonio Section
- **Virginia**
  - Virginia Section, Richmond
  - Virginia Military Institute Branch
  - Virginia Polytechnic Institute Branch
  - University of Virginia Branch
- **Canada**
  - Annual meeting, Engineering Institute of Canada, Ottawa

NATIONAL CONVENTIONS

Three national conventions were held during the year, and a brief report on each follows:

Summer Convention. The 54th summer convention was held in Washington, D. C., June 20–24, 1938. In addition to the annual business meeting, conference of officers, delegates, and members, a conference of vice-presidents, district secretaries, and counselor delegates, there were 10 technical sessions, at which 48 papers were presented, one general session, and six conference sessions. The general session consisted of addresses by President W. H. Harrison, Dr. W. R. Gregg, chief of the weather bureau, and Colonel J. M. Johnson, assistant secretary of commerce.

Entertainment features of the convention were a luncheon, dance, banquet, at which the Gaston Plante Medal and 4,500-franc prize was presented to Dr. G. W. Vinal, tea, golf and tennis tournaments. The registration was 825.

Annual Meeting. The annual business meeting of the Institute was held on Tuesday morning, June 21. The annual report of the board of directors for the fiscal year which ended April 30, 1938, was presented in abstract by the national secretary. A report on the finances of the Institute was presented by National Treasurer W. I. Slichter. The report of the committee of tellers upon the election of officers for the year beginning August 1, 1938, was presented, and President-Elect Parker responded to his introduction with a brief address. During this session, the Lamme Medal for 1937 was presented to Dr. R. E. Doherty, president, Carnegie Institute of Technology, Pittsburgh, Pa.

Pacific Coast Convention. The 26th Pacific Coast convention was held in Portland, Oregon, August 9–12, 1938, with a registration of 454. Five technical sessions, including 22 papers, two student technical sessions, a general session, a joint conference on student activities, reception and dancing, buffet supper, banquet, inspection trips, golf tournament, and ladies' events constituted the principal features of the convention. A technical session on communication was held jointly with the Institute of Radio Engineers.

Winter Convention. The 27th winter convention was held in New York City, January 23–27, 1939, with a program including 77 papers in 17 technical sessions, two general sessions, and 5 technical conferences. During a general session on Wednesday morning, a brief address was given by President Parker, and Dr. Virgil Jordan, president of the National Industrial Conference Board, Inc., gave an address on "Enterprise and Social Progress." A second general session on Thursday afternoon was devoted to engineering conclusions drawn from the experiences encountered during the New England hurricane of

September 21, 1938. The 6 speakers included American Red Cross, railroad, power utility, and telephone representatives.

At an evening session, the Edison Medal was presented to Past-President Dugald C. Jackson, and the John Fritz Medal was presented to Past-President F. B. Jewett.

A smoker, numerous inspection trips, and ladies' events completed the program of the convention, which had a registration of 1,610, the largest since 1924.

District Meetings

Brief reports on the three District meetings held during the year are given below:

North Eastern District Meeting. This meeting was held in Lenox, Mass., May 18–20, 1938, with 3 technical sessions, at which 13 papers were presented, one session for student papers, and a general session with addresses by Roger Babson and K. K. Darrow, an informal banquet, stag smoker, District conference on student activities, inspection trips, and ladies' events. The registration was 417.

Southern District Meeting. This meeting was held in Miami, Florida, November 28–30, 1938, with 4 sessions, at which 10 papers and 4 addresses were presented, and a student session. A conference on student activities, inspection trips, banquet and dance, and ladies' events were included in the program. The registration was 258.

South West District Meeting. The meeting of this District was held in Houston, Texas, April 17–19, 1939, with 5 technical sessions, at which 15 papers and one address were presented, 2 symposiums, and 3 additional sessions for the presentation of 14 student papers, a dinner dance, inspection trips, sports, and ladies' events. The registration was 536.

Sections

Organization of the San Diego and Mansfield Sections, in January and March, respectively, brought the total number to 67. Beginning activities late in 1938 as the Mansfield division of the Cleveland Section, the members in that vicinity soon aroused much interest and secured many new members. The formation of a Section was authorized by the Institute executive committee on March 6.

All of the Sections have been active, and the total number of meetings reported to headquarters was 635, a slight increase over the 624 for the preceding year, and the 621 for 1936–37. The latter was far larger than the highest previous number, 540.

The name of the Detroit-Ann Arbor Section was changed to Michigan Section, with no change in territory.

Interest in the activities of technical groups within the Sections and in the holding of special technical meetings has continued to expand. Several Sections continued their arrangements for offering courses of instruction desired by their membership. The award of prizes by some Sections and co-operation between Sections and Branches in many localities were continued with good results.

The committee on safety wrote the Sections suggesting that each have presented each year a paper dealing with accident prevention or remedial measures after electrical shock. Replies indicated that many Sections were arranging to carry out this suggestion.

Table I. Section and Branch Statistics

	For Fiscal Year Ending			
	April 30, 1936	April 30, 1937	April 30, 1938	April 30, 1939
Sections				
Number of Sections	61	62	65	67
Number of Section meetings held	540	621	624	635
Total attendance	85,501	74,950	110,148	85,692
Branches				
Number of Branches	118	119	120	120
Number of Branch meetings held	1,045	1,363	1,334	1,190
Total attendance	45,304	46,121	60,446	53,380

Three topics of current lively interest are before the Sections as was indicated at the meeting of the Sections Committee in New York at the time of the winter convention:

Unassigned Territory. Chairman Race presented a map showing the territories in the United States not now assigned to any Section.

The general policy was endorsed that wherever practicable the members in such territories should receive notices from and be made to feel a part of the Section nearest to them. In large areas such as the states of South Carolina and West Virginia, Sections should be formed as early as there is sufficient local interest and justification.

In smaller areas such as groups of counties in Maine, New Hampshire, Vermont, and Pennsylvania, the chairman was empowered to consult with the vice-president and Section officers concerned to allocate to the nearest Section such auxiliary territory.

On January 9th, President Parker addressed a letter to Institute members in the unassigned territories requesting suggestions as to how the Institute could be of greater service to those individuals who because of location find it difficult to attend Section meetings. The Sections' opportunity in this respect is indicated in the previous discussion.

Section Activities. Chairman Race presented a chart summarizing the information received to date on Section activities in response to his letter of December 27, 1938. The object of this effort is to inform all Sections on the projects that have proved most interesting and helpful. The suggestion has been made that the Sections committee undertake the preparation of a pamphlet summarizing these numerous and diverse activities so as to make available to incoming Section officers the background of experience of all Sections.

Registration of Engineers. Mr. Beardsley reported on the present status of the licensing of professional engineers. He said that the National Council of State Boards of Engineering Examiners had invited the Institute to co-operate in the formulation of examinations for candidates for the license. Also the board of directors of the Institute had authorized funds for the printing and distribution to Section officers of copies of the latest revision of the AIEE model law. Professor Timbie was appointed to represent the Sections committee to assist in this project.

More detailed information on these activities may be found in the annual report on Section and Branch activities in the June issue of ELECTRICAL ENGINEERING, pages 268–71.

Student Activities

A new Branch organized at the New Mexico State College restored the total number to 120, after the University of North Carolina Branch had been discontinued due to consolidation of the engineering school with that of the North Carolina State College.

Only one Branch failed to report any activity, but the total number of meetings, 1,190, was much lower than in each of the preceding fiscal years, 1,363 in 1936–37, and 1,334 in 1937–38. There was also a material reduction in the number of student talks at Branch meetings.

As indicated in the reports on national conventions and District meetings, the interest in student technical papers has continued, and the following sessions were held:

North Eastern District meeting in Lenox, one; Pacific Coast convention, Portland, two; Southern District meeting, Miami, one, and South West District meeting, Houston, three.

With the approval of the committee on Student Branches, the committee on safety again suggested to all counselors that each Branch have presented a paper dealing with the prevention of accidents or remedial measures after electrical shock. Gratifying responses were received from many Branches.

The midwinter meeting of the committee on Student Branches, held January 26th, was attended by 52 members and invited guests. The meeting was made general to include all those present, and business was transacted as reported on page 124 of ELECTRICAL ENGINEERING for March. Pursuant to the actions taken at that time, a committee is making good progress in rewriting the pamphlet "The Electrical Engineer," which it is expected will be ready for distribution during the coming summer. Another committee, including A. C. Stevens, chairman, F. C. Caldwell, A. G. Conrad, C. E. Skinner, and C. C. Whipple, is busily engaged in preparing a suitable description of the work of Dr. C. F. Scott, the founder of AIEE Student Branches, which can be used at Student Branch meetings next year to celebrate Dr. Scott's 75th anniversary.

According to records at headquarters, the terms of 1,564 enrolled students were expected to expire on April 30. Applications for admission as Associates were received from 849, or about 54 per cent. Some of

Table II. Technical Programs, Last Two Years

	Year Ending April 30, 1939	Year Ending April 30, 1938
Number of national conventions	3	3
Number of District meetings	3	2
Registration at national conventions and District meetings	4,100	3,587
Number of papers presented	184	154
Number of papers recommended for TRANSACTIONS	169	143
Number of pages required for printing papers in TRANSACTIONS	986*	935
Number of technical sessions	50	43
Number of technical conferences	12	8

* Partly estimated.

the students were eligible to continue enrollment as they had remained in school. The corresponding percentage for the preceding fiscal year was 48.

Student activities are summarized more thoroughly in the annual report on Section and Branch activities in the June issue of ELECTRICAL ENGINEERING, pages 268–71.

TECHNICAL PROGRAM COMMITTEE

Convention Programs. The technical program at the 1939 winter convention was more extensive and diversified than at any previous Institute convention. There was a total of 24 sessions—17 technical sessions, at which 77 technical papers were presented and discussed, 5 technical conferences, and 2 general sessions. This exceptional program met with a correspondingly exceptional response from the membership, as is indicated by the fact that registration at the convention was greater than at any convention since 1924. The unusually large technical program did not, by any means, exhaust the committee's fund of papers. After the formation of the winter convention program, there still remained on hand 25 high-grade papers which were held over for use at subsequent meetings.

In addition to the winter convention, the committee arranged programs for the 1938 summer and Pacific Coast conventions, and assisted in the provision of programs for district meetings at Lenox, Mass.; Miami, Florida; and Houston, Texas. A total of 184 papers were presented at these meetings, of which 169 were recommended by the committee for inclusion in TRANSACTIONS.

Technical conferences continue to be an important feature of the technical programs. A total of 12 were held during the year.

The registration at national conventions and District meetings showed an increase of about 15 per cent as compared with the previous year. This is attributable to some extent to the fact that there was one more District meeting than in the previous year. A very great increase in the attendance at the Pacific Coast convention (over 70 per cent) and a 12 per cent increase in the attendance at the winter convention was experienced. Data on attendance and other statistics are shown in table II.

Advance Copies of Technical Papers. Beginning with the 1938 summer convention in Washington, last year was the first year during which the new procedure relative to the provision of advance copies of technical papers was completely in force. The year's experience has proved the value of this procedure. This is indicated by the fact that a total of 61,000 preprints have been distributed during the past year, an average of 330 copies per technical paper. From the standpoint of authors, the new procedure is advantageous since it shortens from 90 to 60 days the minimum interval between the time when a paper is submitted to the Institute and the time when it may be presented. This latter advantage has been made effective through the adoption during the year of a revision of Section 93 of the bylaws as recommended by the committee.

This year, under the new procedure, preprints have been made available not only of all technical papers presented at national conventions but also of all papers presented at District meetings. This feature has been much prized by the District committees, and it is recommended that it be continued in so far as budget conditions permit.

General Sessions. In accordance with the general policy recommended by the committee and approved by the board of directors in 1937, "sessions at which subjects of general interest to all members are discussed" were arranged by the committee for the 1938 summer convention and the 1939 winter convention. At the 1938 summer convention, addresses were given by Dr. W. R. Gregg, Chief of the U. S. Weather Bureau, and Colonel J. M. Johnson, Assistant Secretary of the Department of Commerce, who spoke on weather forecasting, and aviation, respectively.

Table III. Membership Statistics for the Fiscal Year Ending April 30, 1939

	Honorary	Fellow	Member	Six-Year Associate	Associate	Total
Membership on April 30, 1938	9	710	4,316	5,736	5,307	16,078
Additions						
Transferred	1	33	136	376		
New members qualified		1	100	35	1,265	
Former members reinstated			8	22	11	
	10	744	4,560	6,169	6,583	18,066
Deductions						
Died	1	18	42	43	13	
Resigned		2	45	145	107	
Transferred		1	29	127	389	
Dropped		1	52	187	259	
Membership on April 30, 1939	9	722	4,392	5,667	5,815	16,605

At the 1939 winter convention, the committee arranged for an address by Dr. Virgil Jordan on "Enterprise and Social Progress," and for a series of addresses constituting a symposium on the hurricane of September 1938. The speakers at the latter session were Mr. Walter Wesselius, of the American Red Cross, and Messrs. Sidney Withington, E. W. Doebler, C. W. Brown, T. H. Haines, and W. H. Harrison, who among them represented the power, railroad, and communication utilities, and who related the experiences of these utilities as a result of the hurricane. These general sessions have been very popular, attractive, interesting, and valuable additions to convention programs.

Table IV. Number of Applications Received From Enrolled Students and From All Others

Year Ending	From Students	From All Others	Total
April 30, 1939	849	872	1,721
April 30, 1938	739	932	1,671
April 30, 1937	716	1,040	1,756
April 30, 1936	631	946	1,577
April 30, 1935	575	715	1,290

Table V. Number of Enrolled Students

April 30, 1939	5,242	(2,271)
April 30, 1938	5,037	(2,428)
April 30, 1937	4,503	(2,249)
April 30, 1936	4,049	(1,991)
April 30, 1935	3,806	(1,983)

Following the number of Students reported for April 30 of each year is indicated within parentheses the number of new applications received during that year; the difference between this number and the reported total, of course, reflects the number of renewals of Student enrollment for the corresponding period.

Manual of the Technical Program Committee. The committee has in preparation a revised manual of the practices and regulations which govern its operations. It is expected that the manual will be completed and submitted for duplication before the expiration of the committee's term of office. The committee's objective is to include in the manual all the information ordinarily needed by members of the technical program committee, technical committees, and District meeting committees in connection with the planning, procurement, and consideration of material for and conduct of technical sessions and conferences at national conventions and District meetings of the Institute.

Acknowledgment. The effectiveness of the committee's work has been the result of the efforts and devotion of many people—of its members, of course, of the chairmen of the technical committees, of the members of the technical committees, and of other cooperating committees, of the Institute's staff, and in particular of its secretary, Mr. Rich.

PUBLICATION COMMITTEE

The principal activity of the publication committee during the year has been the effort to bring to full fruition the improved publication service to our members made possible by the publication policy approved by the board of directors in the fall of 1937.

As a result of the change in policy, many changes in publication procedure were necessary, and, because it was quite impossible to make all of the changes simultaneously, ELECTRICAL ENGINEERING and the TRANSACTIONS for the year 1938 are, to some extent, non-uniform, part reflecting the old policy and part the new.

Starting with the September 1938 issue, ELECTRICAL ENGINEERING assumed its present standard form. The final change was a shift from a two-column to a three-column format for the technical program papers and discussions. Some had found the reading of the discussions somewhat difficult because of the small type size. Considering ease of reading, it is well established that there is a definite relation between the type size and column width, a wider column requiring a somewhat larger size of type. In this case, the column width was reduced, which gave the desired improvement in readability without incurring the additional expense that would have been involved in enlarging the size of type used for discussions. In fact, a small saving was made by using a slightly smaller size of type for the body of the technical program papers, the combined effect of the new type size and the narrower column being to increase the ease of reading here also. A special effort was made to get the reaction of the members to this change in typography, and, almost without exception, the responses were very favorable.

Special attention has been given to developing the general interest section of ELECTRICAL ENGINEERING, and here again, with very few exceptions, the comments of members have been favorable. The very few cases of adverse criticism of our publications under the new publication policy have been due in almost every instance to a lack of understanding of all of the provisions of the publication policy. To take care of this situation, plans are under way to include a full statement of the publication policy in the pamphlet "Suggestions to Authors," which is regularly issued by the Institute. It is probable that this more comprehensive pamphlet will be available by late summer or early fall.

The Institute has not published a cumulative index of the TRANSACTIONS since 1921, and there have been numerous requests for such an index. During the year, the publication committee sent a questionnaire to the membership to determine the amount of interest in a 17-year cumulative index to cover the years 1921–38, inclusive. The publication committee is happy to report that the response to the questionnaire was such as to indicate that the 17-year cumulative index could be made available on a practically self-supporting basis. As a result, the board of directors, at its January 1939 meeting, approved the publication of the index, and copies are now available.

Table VII. Membership of the Institute, April 30, 1939

Of the 16,605 members reported for April 30, 1939, 14,371 are fully paid to April 30, 1939. The balance of 2,234 are divided into the following groups:

1. Members owing dues to April 30, 1938	
Total number of members who have not acted upon resolution of board of directors adopted in January 1939 providing an extension of time for payment of these dues	**666**
2. Members owing dues to April 30, 1939	**1,568**

(During the period May 1 to 20, 1939, 397 members have paid dues to April 30, 1939, reducing the total to 1,171.)

Table VI. Number of Members in Section Territory Reinstated

August 1, 1938 to April 30, 1939	293
Year Beginning August 1, 1937	325
Year Beginning August 1, 1936	503
Year Beginning August 1, 1935	663
Year Beginning August 1, 1934	831

The same questionnaire made inquiry as to the interest of the membership in a TRANSACTIONS supplement, which would include all of those technical program papers for the year 1938 which did not appear in ELECTRICAL ENGINEERING. The response to this inquiry was also favorable, with the result that those members who do not wish to purchase the bound volume of TRANSACTIONS may obtain the TRANSACTIONS supplement at a price of $0.50, and this supplement, together with the 12 issues of ELECTRICAL ENGINEERING, will give them a complete file of the technical program papers for the year.

The publication committee wishes to thank the membership for its splendid co-operation in the work of making effective the changes called for by the new publication policy. This co-operation has made the task of the publication committee a great deal lighter than it otherwise would have been.

MEMBERSHIP COMMITTEE

The membership committee has put forth special effort this year to overcome the effects of current economic conditions on new membership results. Current business conditions are reflected in the reduction in applications received in 1937–38, and by a reduction in the percentage of members whose dues are fully paid as of April 30, 1939, as will be noted in tables IV and VIII.

The Section committees were organized early this year and were given complete written instructions regarding their work, together with the needed literature, to avoid any delay in getting started. Contacts with the students eligible for Associate grade were organized carefully to assure that each man would be reminded on several occasions of the importance of early affiliation with the Institute. The aid of the Branch counselors was enlisted in these contacts. Their response was splendid, and their efforts did much to assist this phase of the work.

Table VIII. Memberships Fully Paid

	Membership as of April 30	Number of Members Fully Paid as of April 30	Per Cent Fully Paid
1939	16,605	14,371	86.5
1938	16,078	14,127	87.9
1937	15,308	13,439	87.8
1936	14,600	12,446	85.2
1935	14,269	11,512	80.7
1927 (year of maximum membership)	18,344	16,247	88.6

The national committee was organized as last year with 21 members, 9 of whom were new additions. An organization meeting was held in September 1938, and another meeting was called during the winter convention, in January 1939, to review the progress made to date and to plan the work for the remainder of the year. No important changes have been made in the method of conducting the committee's work.

The Section membership committees have co-operated with the committee on transfers in encouraging eligible members to transfer to their proper grade of membership.

Table IV shows the large increase of applications received from enrolled students, which is due both to the increased activity already mentioned and to about 7$^1/_2$ per cent more prospects being available as compared with last year. The reduction in applications received "From All Others" is due largely to reduced returns from the Northeastern industrial section of the country. The total applications received is above last year's record by 50, but is 35 under the number received in 1936–37.

The total Institute membership has increased to 16,605, as compared with 16,078 on April 30, 1938—see table III.

The committee is pleased to see another healthy increase in the number of enrolled students—table V—since these men form the best source for future Associates.

Table VI shows a relatively small change in the number of delinquents reinstated in

Table IX. Record of AIEE Membership

	Total Membership May 1		Total Membership May 1		Total Membership May 1
1884	71	1904	3,027	1923	15,298
1885	209	1905	3,460	1924	16,455
1886	250	1906	3,870	1925	17,319
1887	314	1907	4,521	1926	18,158
1889	333	1908	5,674	1927	18,344
1890	427	1909	6,400	1928	18,265
1891	541	1910	6,681	1929	18,133
1892	615	1911	7,117	1930	18,003
1893	673	1912	7,459	1931	18,334
1894	800	1913	7,654	1932	17,550
1895	944	1914	7,876	1933	17,019
1896	1,035	1915	8,054	1934	15,200
1897	1,073	1916	8,202	1935	14,269
1898	1,098	1917	8,710	1936	14,600
1899	1,133	1918	9,282	1937	15,308
1900	1,183	1919	10,352	1938	16,078
1901	1,260	1920	11,345	1939	16,605
1902	1,549	1921	13,215		
1903	2,229	1922	14,263		

comparison with the annual reductions of the past few years. The top figure of 293 is for nine months only and compares closely with 306 for the same period one year ago. The figures in this table may be expected to change little, or even to increase if business conditions cause an increasing number to become delinquent.

Table VII indicates again, as does table VIII, that there is a tendency on the part of a larger number to let dues payments wait. Last year, only 470 had not accepted the offer passed by the board of directors in January 1938, and 1,481 owed dues to April 30, 1938 (this latter number had been reduced to 1,211 by May 17, 1938). This condition should improve when world and business conditions become more stable.

DEATHS

The deaths of 117 members reported during the year are listed in table X.

Table X. Deaths of AIEE Members Reported During the Fiscal Year

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in ELECTRICAL ENGINEERING
Aanonsen, Hans E.	Associate '07		Associate	Jan. 1939, p. 53
Abell, Harry C.	Associate '03	Nov. 24, 1938	Associate	Jan. 1939, p. 53
Ahearn, Thomas	Associate '87	June 28, 1938	Member	Aug. 1938, p. 363
Alderman, Haywood L.	Associate '27	Jan. 11, 1939	Member	Feb. 1939, p. 93
Alexander, James P.	Member '21	Oct. 8, 1938	Member	Dec. 1938, p. 523
Battern, Algy R.	Associate '20		Associate	Aug. 1938, p. 364
Benham, Claude F.	Associate '16	Sept. 12, 1938	Member	Nov. 1938, p. 473
Bird, Montgomery R.	Associate '36	Nov. 27, 1936	Associate	Dec. 1938, p. 524
Blondel, Andre E.	Associate '05	Nov. 15, 1938	Honorary Member	Feb. 1939, p. 93
Bracken, James L. F.	Associate '18	Oct. 21, 1938	Associate	Dec. 1938, p. 523
Brandenburger, Leo	Associate '37	Feb. 11, 1938	Associate	Oct. 1938, p. 436
Bronson, Frederick M.	Member '36	Oct. 23, 1938	Member	Dec. 1938, p. 523
Brown, Glendon C.	Member '37	1938	Member	March 1939, p. 141
Brubaker, Charles N.	Associate '19	May 3, 1938	Member	Aug. 1938, p. 364
Buck, Nelson E.	Associate '19	Dec. 12, 1938	Associate	March 1939, p. 141
Burt, Austin	Associate '07	Sept. 1938	Fellow	April 1939, p. 186
Cammack, John E.	Associate '19	March 1939	Associate	May 1939, p. 229
Carpenter, Dan E.	Member '15	Sept. 7, 1938	Member	Oct. 1938, p. 436
Clothier, Henry W.	Member '17	March 11, 1938	Member	June 1938, p. 275
Coates, Charles B.	Associate '03	March 17, 1939	Member	May 1939, p. 229
Collings, Llewellyn W.	Associate '37	Dec. 27, 1938	Associate	Feb. 1939, p. 93
Connolly, Stephen J.	Associate '08	Jan. 29, 1938	Associate	July 1938, p. 325
Crates, Royal R.	Associate '37	March 10, 1939	Associate	May 1939, p. 229
Cunningham, Andrew J.	Associate '35	Dec. 1937	Member	Aug. 1938, p. 364
Dalton, William J.	Associate '24	July 9, 1938	Associate	Sept. 1938, p. 395
Davis, Albert G.	Associate '98	April 25, 1939	Fellow	June 1939, p. 279
Dawson, William F.	Associate '05	Jan. 19, 1939	Fellow	March 1939, p. 141
Deck, Frederick W.	Member '30	Sept. 26, 1938	Member	Nov. 1938, p. 473
Denmann, Burt J.	Associate '11	June 25, 1938	Associate	Aug. 1938, p. 364
Dick, William A.	Associate '02	Dec. 5, 1938	Fellow	May 1939, p. 229
Dickerson, E. N.	Associate '84		Associate	
Dodd, Maynard	Member '24	April 10, 1938	Member	June 1938, p. 275
Doty, Paul	Associate '04	Dec. 3, 1938	Associate	March 1939, p. 140
Ducey, Walter J.	Associate '22	Sept. 29, 1938	Member	Nov. 1938, p. 473
Ellis, Joseph	Associate '17	Oct. 2, 1938	Associate	Jan. 1939, p. 54
Frederiksen, Victor	Associate '32	April 23, 1938	Associate	Oct. 1938, p. 436
Freidenmann, John W.	Member '30	Feb. 13, 1938	Member	June 1938, p. 274
Furness, Douglas L.	Associate '08	June 19, 1938	Associate	Sept. 1938, p. 395
Gallagher, Francis W.	Associate '37	Sept. 18, 1938	Associate	Jan. 1939, p. 54
Gallatin, Albert R.	Associate '98	March 1939	Associate	June 1939, p. 281
Gillespie, Leigh R.	Associate '19	Nov. 1938	Member	Jan. 1939, p. 53
Geisler, Hugo P., Jr.	Member '36	Sept. 10, 1938	Member	Dec. 1938, p. 523
Glaubitz, Hugh J.	Member '17	Jan. 14, 1939	Member	March 1939, p. 140
Green, Richard	Associate '34	March 29, 1938	Associate	July 1938, p. 326
Guttmann, Raymond	Associate '35	Feb. 22, 1938	Associate	Dec. 1938, p. 524
Hardy, Carl E.	Associate '99	Feb. 15, 1939	Associate	April 1939, p. 186
Harris, Charles H.	Associate '34	Oct. 22, 1938	Associate	Dec. 1938, p. 523
Hawkins, Charles C.	Associate '03	Aug. 1938	Associate	Nov. 1938, p. 473
Hayes, Timothy A. J.	Associate '32	April 14, 1938	Associate	June 1938, p. 275
Henning, Clarence I. B.	Associate '13	Jan. 26, 1939	Associate	March 1939, p. 141
Hermann, Henry	Associate '15	Oct. 17, 1938	Associate	April 1939, p. 186
Hirshfeld, Clarence F.	Associate '05	April 19, 1939	Fellow	May 1939, p. 229
Hopewell, Charles F.	Associate '97	Oct. 17, 1938	Member	March 1939, p. 141
Hoppe, Walter H.	Associate '30	Nov. 1938	Associate	Jan. 1939, p. 54
Horne, George H.	Associate '10	Dec. 6, 1938	Associate	Feb. 1939, p. 94
Huseby, Gisle E.	Associate '26	Oct. 2, 1938	Associate	Dec. 1938, p. 524
Hussey, Abram	Associate '06	Aug. 17, 1937	Member	Oct. 1937, p. 1334
Hutchinson, Cary T.	Associate '90	Jan. 16, 1939	Fellow	March 1939, p. 140
Insull, Frederick W.	Member '37	Jan. 14, 1939	Member	March 1939, p. 141
Insull, Samuel	Associate '86	July 15, 1938	Fellow	Aug. 1938, p. 363
Jackson, Ray P.	Associate '06	Nov. 1937	Associate	July 1938, p. 326
Jacoby, S. Clifford	Associate '21	Oct. 20, 1938	Member	Dec. 1938, p. 523
Jennison, Herbert C.	Associate '05	June 12, 1938	Associate	Oct. 1938, p. 437
Jorgensen, Lars R.	Associate '05	May 6, 1938	Member	July 1938, p. 326
Keyes, Clift B.	Associate '03	Dec. 7, 1938	Member	Jan. 1939, p. 53
Killgore, Lloyd M.	Associate '18	March 1938	Associate	Jan. 1939, p. 54
Knaur, Richard J.	Associate '22	April 26, 1938	Associate	Aug. 1938, p. 364
Lackie, Walter J.	Associate '28	May 26, 1938	Associate	Sept. 1938, p. 395
Lanphier, Robert C.	Associate '01	Jan. 28, 1939	Fellow	March 1939, p. 141
Lawton, Arthur H.	Associate '03	Dec. 2, 1938	Fellow	Jan. 1939, p. 53
Maxwell, Eugene	Associate '96	July 28, 1938	Associate	Nov. 1938, p. 473
McQuarrie, James La	Associate '07	March 1, 1939	Fellow	April 1939, p. 186
Meredith, Gailen E.	Associate '19	April 20, 1938	Member	July 1938, p. 326
Miller, Charles A.	Associate '21	Aug. 23, 1938	Associate	Oct. 1938, p. 437
Mintzner, Watkins F.	Associate '20	Aug. 22, 1938	Associate	Dec. 1938, p. 523
Moody, Walter S.	Associate '06	Nov. 7, 1938	Fellow	Dec. 1938, p. 523
Mordey, William M.	Associate '91	July 1, 1938	Associate	Sept. 1938, p. 395
Morgan, Oliver J.	Associate '36		Associate	April 1939, p. 186
Munshi, Dinshaw P.	Associate '32	Dec. 16, 1937	Associate	Aug. 1938, p. 364
Nagel, William G.	Associate '03	July 20, 1938	Associate	Oct. 1938, p. 437
Packer, Edson F., Jr.	Associate '36	June 13, 1938	Associate	Aug. 1938, p. 364
Panter, Thomas A.	Associate '03	March 12, 1939	Fellow	May 1939, p. 229
Parry, Evan	Associate '95	Dec. 17, 1938	Associate	Feb. 1939, p. 93
Peck, Emerson P.	Associate '08	Nov. 14, 1938	Fellow	June 1939, p. 280
Pike, Clayton W.	Associate '91	Dec. 30, 1938	Member	April 1939, p. 186
Ratcliff, Henry A.	Member '22	April 26, 1938	Member	June 1938, p. 275
Reid, Edwin S.	Associate '96	July 20, 1938	Member	Nov. 1938, p. 473
Robbins, Percy A.	Associate '03	April 23, 1938	Associate	July 1938, p. 325
Roberts, Samuel N.	Associate '21	Jan. 1939	Associate	March 1939, p. 141
Robertson, James T.	Associate '06	Sept. 6, 1938	Member	Nov. 1938, p. 473
Roller, Frank W.	Associate '95	Aug. 21, 1938	Fellow	Sept. 1938, p. 395
Ross, James D.	Associate '08	March 14, 1939	Fellow	April 1939, p. 185
Ruffner, Charles S.	Associate '02	Jan. 21, 1939	Fellow	March 1939, p. 140

COMMITTEE ON TRANSFERS

Since the last report, the membership has voted amendments to the constitution, Section 4 of Article II and Section 10 of Article III, providing that application for transfer to grade of Fellow shall result only from proposal, and that at least five years of Member grade shall be a requirement; this with certain exceptions. These provisions, published in ELECTRICAL ENGINEERING, July 1938, pages 303–04, became effective July 21, 1938.

Material relative to Section transfer committee activity has been regularly circulated to all Section chairmen.

In order to ascertain the status of transfer committees in the Sections, questionnaire letters were sent to the Section chairmen. These were returned with results as follows:

56 Sections reported.
Of these, 19 have Section transfer committees, 16 receiving aid from the Section membership committee.
37 have no Section transfer committee. Of these, 33 Sections look to the Section membership committee for transfer responsibility. Twelve of these are discussing the matter or planning on having Section transfer committees.

The results of this survey would indicate that either through the Section transfer committee or the Section membership committee, as seems best to the Section executive committee, the coverage of transfer activities in the Sections is quite complete, needing only stimulation for increased performance.

The number of applications for transfer since 1927 is shown in table XI.

BOARD OF EXAMINERS

The board of examiners held 11 meetings during the past year, averaging about two and one-half hours each, and considered 3,820 cases, divided as shown in table XII.

STANDARDS COMMITTEE

The increasing interest and activity of the various technical committees of the Institute in standardization which was reported during the previous year has been mani-

Table X (Continued). Deaths of AIEE Members Reported During the Fiscal Year

Name	Date of Election	Date of Death	Grade at Death	Obituary Notice in ELECTRICAL ENGINEERING
Ryan, William T.	Associate '07	Feb. 5, 1939	Member	March 1939, p. 140
Seletzky, Anatoli C.	Associate '29	May 11, 1938	Member	June 1938, p. 274
Shearer, Harold H.	Member '20	Nov. 10, 1938	Member	Dec. 1938, p. 523
Shute, Loren H.	Associate '15	Feb. 10, 1939	Associate	June 1939, p. 280
Snell, Sir John F. C.	Associate '06	July 6, 1938	Fellow	Aug. 1938, p. 363
Sterba, Ernest J.	Associate '21	April 24, 1939	Member	June 1939, p. 280
Strasburger, Edgar	Associate '03		Associate	May 1939, p. 229
Sutherland, Wm. J. K.	Associate '15	March, 1938	Associate	Oct. 1938, p. 436
Swallow, Joseph G.	Associate '09	Jan. 14, 1939	Associate	April 1939, p. 186
Terry, Charles A.	Associate '87	Feb. 18, 1939	Member	April 1939, p. 186
Thompson, John H.	Associate '21	Oct. 31, 1938	Associate	Jan. 1939, p. 54
Thornton, Kenneth B.	Associate '01	Feb. 10, 1938	Member	Dec. 1938, p. 523
Thrush, George H., Jr.	Associate '34	Sept. 25, 1938	Associate	Dec. 1938, p. 523
Uhlenhaut, Fritz, Jr.	Associate '89	Jan. 1, 1937	Member	Feb. 1939, p. 94
Upley, Arnold S.	Associate '37		Associate	Dec. 1938, p. 524
Walker, Earle T.	Associate '35	Sept. 22, 1937	Associate	July 1938, p. 326
Warner, John C.	Member '34	July 21, 1938	Member	Sept. 1938, p. 395
Watson, Malcolm V.	Associate '07	April 25, 1938	Member	June 1938, p. 274
Weeks, Edwin R.	Associate '87	Aug. 17, 1938	Fellow	Oct. 1938, p. 436
Whitmore, Walter G.	Associate '90	Nov. 30, 1938	Associate	Feb. 1939, p. 94
Wilbraham, F. M.	Associate '19	Jan. 21, 1939	Associate	April 1939, p. 186
Wilcox, Herbert M.	Member '37	July 28, 1938	Member	Sept. 1938, p. 395
Wittenberg, Michael	Member '24	Jan. 1939	Member	March 1939, p. 141
Young, Walter E.	Associate '19	June 22, 1938	Member	Sept. 1938, p. 395

fested during the present year. There was also increased participation by the Institute in ASA projects through the appointment of representatives on a number of new sectional committees. Of particular interest is the work started in the ASA on lightning arresters, storage batteries, and on a test code for fractional horsepower motors initiated by a subcommittee of the AIEE committee on electrical machinery. A reorganization of the sectional committee on electric-welding is now under way. The Institute has also appointed representatives on a sectional committee that will attempt to develop international marine standards as affecting electrical installations on shipboard.

During the year, important new AIEE standards have been issued and others revised as follows: Notably, the completely revised edition of the Marine Rules, a new test code on apparatus noise measurement and standards for oil circuit breakers and indicating instruments.

In accordance with plans made during the previous year, three regular sessions and two symposiums at the winter convention were devoted to discussion of standards for the rating of various types of electrical machinery, especially motors and transformers. A total of 12 formal papers were presented. The sessions were very well attended and a great deal of interest was displayed by the membership at large, indicating that future meetings of similar character will be justified. Several papers relating to standards activities are contemplated for inclusion in the program for the summer convention.

The standards committee, realizing that a certain amount of co-ordination of standardization work carried on by the technical committees is desirable, and also that the Institute might carry on to good advantage some standards activities not ordinarily falling within the scope of the existing technical committees, has organized a number of co-ordinating committees for the following purposes:

(*a*). To compile data on existing conditions and standards which will assist in determining the need for better co-ordination.

(*b*). To prepare guiding principles to be used by the other AIEE committees in their standards work.

(*c*). To maintain contact with all interested AIEE committees and also with national and international standardization agencies outside of the Institute.

(*d*). To recommend, initiate, or sponsor setups in the ASA, IEC, and other standardization bodies which seem desirable or expedient for bringing about maximum co-ordination of standards in the electrical field.

(*e*). To initiate, in co-operation with interested AIEE technical committees, regular or informal Institute sessions for the purpose of discussing vital issues of standardization.

At present these committees cover the following subjects: Reference Values for Standards, Standard Voltages and Currents, Insulation Testing and Co-ordination, and Basic Principles for Rating of Electrical Machines and Apparatus. The last-named committee will first work on a revision of AIEE standard No. 1, General Principles Upon Which Temperature Limits Are Based in the Rating of Electrical Machinery and Apparatus, utilizing in part information and data contributed in papers and symposiums during the winter and summer conventions

Table XI. Applications for Transfer

Year Ending April 30	Fellow Grade: Recommended	Fellow Grade: Not Recommended	Fellow Grade: Total	Member Grade: Recommended	Member Grade: Not Recommended	Member Grade: Total
1927	30	5	35	293	32	325
1928	21	3	24	280	17	297
1929	45	2	47	229	19	248
1930	28	2	30	211	29	240
1931	44	3	47	322	31	353
1932	7	2	9	149	17	166
1933	29	2	31	109	11	120
1934	25	2	27	154	3	157
1935	19	2	21	199	23	222
1936	27	1	28	205	24	229
1937	24	2	26	167	27	194
1938	26	0	26	137	7	144
1939	25	2	27	126	13	139
Totals	350	28	378	2,581	253	2,834

As a further means for increasing the interest of the membership in the standardization activities of the Institute, it has been decided to augment the columns of ELECTRICAL ENGINEERING where brief reports on standards activities are given.

A plenary meeting of the International Electrotechnical Commission was held in Torquay, England, during the past year, at which 25 or more committees participated. A number of members of the Institute interested in standardization attended this meeting.

UNITED STATES
NATIONAL COMMITTEE OF THE IEC

Nineteen-thirty-nine was an active year for the International Electrotechnical Commission. A very successful plenary meeting was held at Torquay, England, June 22 to July 1, this being the ninth plenary meeting. It was attended by 400 delegates from 22 countries, the United States being represented by 22 delegates.

Many important final decisions, as recorded in the brief reports below, were taken. Of outstanding importance was the recommendation of advisory committee 24 that the permeability of free space be the connecting link between the electrical and mechanical units of the Giorgi (MKS) System.

At the closing dinner to the delegates given by the British Standards Institution at the Guildhall, London, the Duke of Kent said:

> The International Electrotechnical Commission's work in co-ordinating and simplifying industrial requirements in the electrical field should prove of the highest value to international exchange of electrical goods. This co-ordination should be increasingly valuable as the economic interdependence of the nations is more fully recognized.

A brief summary of some of the actions taken at the Torquay meeting is given below:

International Electrotechnical Vocabulary, IEC. 1. The first edition of the vocabulary, containing some 2,000 scientific and technical terms defined in both French and English, and with the titles in German, Spanish, Italian, and Esperanto, was approved for publication. About 150 copies have been distributed in this country.

Table XII. Applications for Admission and Transfer

Applications for Admission		
Recommended for grade of Associate	1,330	
Re-elected to the grade of Associate	86	
Not recommended	10	1,426
Recommended for grade of Member	95	
Re-elected to the grade of Member	11	
Not recommended	43	149
Recommended for grade of Fellow	1	
Re-elected to the grade of Fellow	—	
Not recommended	1	2
Applications for Transfer		
Recommended for grade of Member	126	
Not recommended	13	139
Recommended for grade of Fellow	25	
Not recommended	2	27
Students		
Recommended for enrollment as Students		2,079
Total		3,820

Transformers, IEC 2B. Two divergent methods of rating transformers have been in general use in different countries, one based on the full-load secondary current and the no-load secondary voltage, and the other based on the full-load secondary current and the full-load secondary voltage at a specified power factor (0.8 or 0.85). The IEC meeting at Torquay agreed that both the IEC test rating and the IEC service rating should be shown on the nameplate. It was also agreed that when no power factor is specified by the purchaser the service rating will be based on a power factor of 0.8, and that losses should be expressed in kilowatts and not as a percentage efficiency. Fractional loads are to be expressed in terms of current. The test rating agreed upon by the IEC as one of the factors to be used in rating transformers defines a transformer in which it might be desired to reverse the direction of flow of power, and the service rating defines a transformer with a predetermined direction of flow.

It was agreed that ambient temperature should be defined to mean that the peak value of 40 degrees centigrade should be associated with an average temperature not exceeding 35 degrees centigrade over any 24-hour period. The maximum temperature rise for oil-immersed transformers, with forced oil circulation, it was agreed, should be 65 degrees centigrade.

Graphical Symbols, IEC 3. IEC Publication 42, graphical symbols for heavy current systems, will be revised on the basis of decisions taken at Torquay. Publication 42, graphical symbols for weak current systems, will be submitted to the national committees for approval of the revision agreed to at Torquay.

Steam Turbines, IEC 5. Two IEC publications (45 and 46) were extended to cover all other kinds of turbines not heretofore covered. Certain decisions were also taken on fluid flow measurement.

Lamp Caps and Holders, IEC 6. Dimensions of lamp caps and holders, approved by this committee, will be published by the IEC. Drawings of gages were approved, but each National Committee will, of course, decide for itself what gages would be used for checking the dimensions.

Aluminum, IEC 7. It was unanimously agreed that annealed aluminum should be defined as aluminum which, in the form of wire of 1 mm diameter and greater, has a maximum tensile strength of 9.5 kg per square mm and a minimum elongation of 20 per cent in a length of 200 mm. The measurement of the elongation is to be taken after fracture. The normal resistivity value should be 0.0278 ohm mm^2/m at a temperature of 20 degrees centigrade. The temperature coefficient and density for hard-drawn aluminum are considered the same as for annealed aluminum. For the steel core it was agreed to adopt a minimum tensile strength of 120 kg/mm^2 and an elongation of 5 per cent for wires of 3 mm diameter and over, and 4 per cent for wires which are smaller than 3 mm diameter. Standards of resistivity for aluminum used for insulated cables and bus bars were agreed upon.

Standard Voltages and High Voltage Insulators, IEC 8. A column showing the three-phase phase-voltages corresponding to the three-phase phase-to-neutral voltages will be included in the new edition of the IEC publication on Standard Voltages, in the section on voltage between 100 and 1,000 volts. The first edition of an international specification on the testing of insulators will be issued, it was agreed. Impulse voltage tests for the testing of line insulators will be included.

The meeting referred a draft specification for testing wall bushing insulators to the national committees for consideration, and approved a report recommending the revision of the IEC calibration tables for sphere gaps.

Electric Traction Equipment, IEC 9. A revised edition of the specification for traction motors will be circulated to the national committees for approval.

Overhead Lines, IEC 11. A new edition, in French, of Publication No. 49, comparison of the regulations in force in various countries for the erection of overhead lines, has been prepared. Further consideration will be given to formulas proposed for the calculation of the loading of overhead lines.

Electrical Instruments, IEC 13. Revision of the publications on alternating current watt-hour meters; instrument transformers; and indicating instruments was discussed, and action will be taken later. This committee held a joint meeting with the advisory committee on terminal markings, and the two committees appointed a subcommittee to consider marking instrument transformers separately from instruments and meters.

Terminal Markings, IEC 16. A report by this committee summarized the systems of terminal markings now in use as follows:

(*a*). The system used in the United States as given in the American Standard for rotation, connections, and terminal markings for electric power apparatus.

(*b*). The system used in Great Britain which will be available soon as a report of the British Standards Institution.

(*c*). A composite system taking certain of its parts from the systems of the different nations on the continent of Europe, the composite being arrived at in discussions in IEC Advisory Committee Number 16.

It is expected that the IEC will print the composite system as a report in the hope that as the various nations find it necessary to change their existing systems they will be able to adopt this system in whole or in part.

The IEC will also publish a separate report giving the history of the whole situation on terminal markings, explaining the three separate systems and giving information as to where detailed information on all of them may be obtained.

Switchgear, IEC 17. Insulation tests, impulse voltage tests, the natural frequency of the test circuit and the rate-of-rise of recovery voltage and the testing of large circuit breakers were discussed.

Electric Cables, IEC 20. A revision of the IEC specifications was suggested and proposed changes were submitted to a subcommittee for consideration.

Electric and Magnetic Magnitudes and Units, IEC 24. It was agreed that the committee recommend that the connecting link between the electrical and mechanical units in the MKS system should be the permeability of free space (μ_0) with the value of 10^{-7} in the unrationalized system, $4\Pi 10^{-7}$ in the rationalized system. The committee also agreed to recommend that the name of the unit of force should be the "Newton."

Letter Symbols, IEC 25. A revised edition of IEC publication 27 will be circulated to the national committees for approval.

Electric Welding, IEC 26. The first meeting of this committee decided that it will start its program with work on welding plant and equipment, and that it will co-operate with the committee on welding of the International Standards Association. Definitions, rating of welding plant and equipment, non-automatic single-arc direct-current welding sets, automatic single-arc direct-current welding sets, constant-voltage multiple-arc direct-current welding sets, electric motors, air-cooled direct-current reactors, and chokes and transformers were discussed. Proposals made by the British committee will be reviewed and referred to the various national committees for their recommendations.

More detailed reports of all committee meetings are available. Anyone interested should write to J. W. McNair, secretary of the U. S. national committee of the International Electrotechnical Commission, American Standards Association, 29 West 39th Street, New York.

It is now expected that meetings of the following IEC advisory committees will be held in New York, September 5–8, 1939.

2b Transformers
4 Hydraulic Turbines
17 Switchgear
19 Internal Combustion Engines
26 Welding

The organization meeting of the new committee on insulation co-ordination and possibly the subcommittee on sphere gaps.

COMMITTEE ON PLANNING AND CO-ORDINATION

The recommendation of the predecessor, the committee on co-ordination of Institute activities, that the committee be reconstituted under the title "committee on planning and co-ordination" and assume the broader duties thus indicated was approved by the board of directors, with the change effective August 1, 1938. Section 70 of the bylaws was amended to assign to the committee the additional duty of giving continuous attention to the planning of Institute activities of the future.

The committee invited a considerable group of members to supply their views on the present activities of the Institute and their suggestions regarding any changes or additions which would make the organization more valuable to the profession in general and its members in particular.

A comprehensive report on present and proposed future activities of the Institute, based very largely on the 29 replies received and the views of members of the committee, has been submitted for consideration of the board of directors at its May meeting.

In accordance with past practice, the committee received requests from District and Section officers for national conventions and District meetings desired in their Districts in 1940, and submitted a recommended schedule of such meetings, which was approved by the board of directors in January.

INSTITUTE POLICY COMMITTEE

The only matter referred to the committee during the year was the question as to whether the Institute should include in its scope of activity consideration of an engineering approach to the physical facts of depreciation and obsolescence of electrical material.

The AIEE executive committee requested the Institute policy committtee to submit an outline of the principal factors, both for and against participation in this field, that should be considered by the board of directors.

Committee on Safety

By an action of the board of directors the functions of the committee on safety codes were expanded, and a committee on safety created to include the entire field of safety in electrical engineering.

The committee, in letters, has endeavored to interest universities, and in particular their electrical engineering departments, in the matter of accident prevention and including instruction in the proper method of resuscitation from electric shock, asphyxiation, drowning, et cetera. Suggestions have been included that other departments of the universities may also be interested in the subject, with emphasis on instruction to all students, particularly because of the frequent occurrence of such accidents outside of university laboratories. Such training among the electric and gas utility companies has, over the years, resulted in the saving of the lives of many persons who have accidentally come in contact with energized parts, or apparently have been drowned, or asphyxiated from gas fumes. The committee is convinced that continual training in this method is absolutely essential and that, to be most effective, such training should be given before students have left the universities. Letters were therefore sent to 120 professors of electrical engineering of the various universities and colleges, together with a booklet describing the Schafer Prone Pressure Method of Resuscitation. The replies were immediate and evidenced a highly co-operative spirit. Correspondence is continuing with such universities.

Letters were also sent by the committee to the counselors of student branches of the universities, requesting that during the year each student branch arrange to have presented before the branch a paper dealing with some phase of the problem of prevention of accidents or remedial measures after electrical shock. This had been discussed with the chairman of the committee on student branches and received his wholehearted endorsement. Assistance was offered, if necessary, to secure a local speaker to prepare and present such a paper. Again the reaction was spontaneous, and steps have been inaugurated to attain this during this or the coming year, where this subject had not already been included in the meetings.

A similar letter was authorized to be sent to the various Sections of the Institute to recommend to them that during each year they arrange to have presented before each Section a paper dealing with the prevention of accidents or remedial measures after electrical shock. It was found that, in general, arrangements were being made by the Sections to include this important subject in their programs.

There exists a great need for the distribution of a comprehensive booklet on resuscitation, of convenient size, which people could readily obtain for reference.

A movement has been started to obtain information relating to safety practices from all related foreign institutes of various kinds. Abstracts are being prepared of replies received, and it is hoped that a comprehensive summary of practices will be available soon.

The committee was successful early in the year in arranging for the preparation of a paper for a general meeting. The paper now under preparation is concerned with safe practices in college laboratories, dealing largely with methods to assure safety to the laboratory students and personnel. Arrangements have been made for the preliminary presentation of this paper at a meeting of the Society for the Promotion of Engineering Education.

During the year, a number of subjects relating to electrical safety were presented for discussion. It was found that such subjects being given active consideration were concerned with the developments in, or revision of, electrical codes or standards in general sponsored under the auspices of the American Standards Association. Some of the subjects, from a purely technical viewpoint, are being given further consideration.

The committee is of the opinion that the time to make apparatus safe for construction, operation, and maintenance is at the time that the apparatus is being designed. It has therefore recommended to the National Electrical Manufacturers Association that they request their members to add a line on the original drawings to provide that the design be checked for safety in the engineering department or drafting office. This recommendation might well be also carried out by manufacturers and public utilities generally.

Committee on Legislation Affecting the Engineering Profession

The committee on legislation affecting the engineering profession, consisting of fifteen members well representative of all of the Sections, has carried on its work along the lines similar to those of previous years.

Some thought has been given to possible methods which might enable the committee to keep in touch with proposed legislation regarding licensing of engineers that may be introduced in the various state legislatures so that the Institute could be promptly and properly informed regarding those developments with a view toward being helpful.

The Texas registration law was contested during the year in the courts, and, upon the request of the national secretary, all of the pertinent information available at the time in connection with the case was obtained from the secretary of the Texas State board and the dean of engineering of the University of Texas and forwarded to the Institute. The principal controversial point in this case hinged upon the definition of "principles of engineering" used in the Texas law, and upon final disposition of this matter a report will be made to the national secretary of the Institute.

Committee on Code of Principles of Professional Conduct

Members of this committee have each been provided with a copy of the codes of the American Society of Civil Engineers, the American Society of Mechanical Engineers, the American Institute of Chemical Engineers, the American Institute of Consulting Engineers, and the American Association of Engineers in addition to our own code, and have considered whether our code required modification. No suggestions have come forth proposing modifications, and it therefore may be interpreted that none is necessary at the present time.

Committee on Constitution and Bylaws

This committee considered and recommended several proposed amendments to the bylaws of the Institute. The committee conducted its work by correspondence.

Committee on Economic Status of the Engineer

Members of the committee met informally at the winter convention. In the matters of employment, unemployment security, and annual income there are available the publication of bulletins No. R-497, 543, 588, and 631 by the United States Department of Labor covering the engineering profession, 1929–34, Professor Sorensen's paper on the Economic Status of the Engineer, Transactions, 1938, and a bulletin of the National Bureau of Economic Research, February 1939, comparing engineers' income with that of other professions. The committee feels, therefore, that adequate data on that phase of its assignment have been presented.

The committee is observing the progress of the ECPD committee on professional recognition, and has noted its recent study of the qualification procedure in the fields of accounting, architecture, law, and medicine. The committee is watching the policies and organization plans of other professional engineering societies and federations of engineers, and the trend of possible reorganization and revision of the objectives of the American Engineering Council.

Committee on Award of Institute Prizes

Three national and 12 District prizes were awarded in 1938 for papers presented in the calendar year 1937 and for student papers presented during the academic year ending June 30, 1938. These awards were announced in the issues of Electrical Engineering for July, September, and December 1938 and April 1939.

The committee considered a large number of papers and the gradings and recommendations of the technical committees which had reviewed the papers. Many papers considered were of a high quality, and, in addition to the national prizes, ten other papers were given honorable mention.

Committee on Award of Columbia University Scholarships

During February and March 1938, there were twelve applications for the Columbia University scholarship. After due consideration of the merits of all the applicants, the committee voted to award the scholarship for 1938–39 to Mr. James E. Hulsizer, who received his B.S. degree from Princeton in June 1938. Mr. Hulsizer accepted the scholarship, and has been in regular attendance during the present academic year.

Edison Medal

The Edison Medal, which is awarded by a committee composed of 24 members of the Institute, was, for 1938, awarded to Dr. Dugald C. Jackson "for outstanding and inspiring leadership in engineering education and in the fields of generation and distribution of electric power," and was presented on January 25, 1939, during the winter convention. The medal may be awarded annually for "meritorious achievement in electrical science, electrical engineering, or the electrical arts."

John Fritz Medal

The John Fritz Medal board of award, composed of representatives of the national societies of civil, mining, mechanical, and electrical engineers, awarded the 35th medal (for 1939) to Dr. Frank B. Jewett, vice-president, American Telephone & Telegraph Company, and president, Bell Telephone Laboratories, Inc., for "vision and leadership in science, and for notable achievement in the furtherance of industrial research and development in communication."

Lamme Medal

The Lamme Medal committee awarded the medal for 1938 to Marion A. Savage, designing engineer, General Electric Company, "for able and original work in the development and improvement of mechanical construction and the efficiency of large high speed turbine alternators." Arrangements are being made for the presentation of the medal at the combined Summer and Pacific Coast convention in San Francisco, Calif., June 26–30, 1939. The medal may be awarded annually to a member of the AIEE "who has shown meritorious achievement in the development of electrical apparatus or machinery."

Alfred Noble Prize

This prize, established in 1929, consists of a certificate and a cash award of $500 from the income from a fund contributed by engineers and others to perpetuate the name and achievements of Alfred Noble, past-president of the American Society of Civil Engineers and of the Western Society of Engineers. It may be made to a member of any of the co-operating societies, ASCE, AIME, ASME, AIEE, or WSE, for a technical paper of particular merit accepted by the publication committee of any of these societies, provided the author, at the time of such acceptance, is not over 30 years of age. The award for 1938 was presented to Ralph J. Schilthuis for his paper on "Connate Water in Oil and Gas Sands."

Washington Award

The Washington Award for 1939 was bestowed upon Dr. Daniel Webster Mead, "for his superior contribution to sound theory, good practice, and high ethical standards in the creation of engineering works as an engineer and as a teacher," and was presented to him at a dinner on February 20, 1939. This award may be made annually to an engineer by the commission of award composed of nine representatives of the Western Society of Engineers and two each of the American Society of Civil Engineers, American Institute of Mining and Metallurgical Engineers, American Society of Mechanical Engineers, and AIEE.

Hoover Medal

The Hoover Medal was established through a trust fund created by a gift from Conrad N. Lauer, and is to be awarded periodically "to a fellow engineer for distinguished public service" by a board representing the national societies of civil, mining and metallurgical, mechanical, and electrical engineers. The third recipient of this medal was John Frank Stevens, who was cited as an "engineer of great achievement, as illustrated in his work on the Panama Canal, who, in his dealings with the inter-allied forces in Siberia in the Great War, demonstrated those broader capacities for humanitarian public service beyond his calling."

Iwadare Foundation Committee

No Iwadare lecturer was chosen to go to Japan for the current year, nor has any Iwadare Fellow come to the United States.

Table XIII. Analysis of Employment Service

Month	Men Registered New York	Chicago	San Francisco	Total	Men Placed New York	Chicago	San Francisco	Total
1938								
May	258	135	106	499	34	18	23	75
June	289	169	97	555	55	12	7	74
July	188	98	86	372	28	20	33	81
August	181	86	80	347	47	33	28	108
September	199	99	50	348	50	23	21	94
October	175	67	79	321	57	23	26	106
November	133	58	51	242	45	30	13	88
December	102	46	79	227	37	13	17	67
1939								
January	144	37	73	254	53	16	23	92
February	143	62	85	290	40	16	12	68
March	169	67	109	345	51	19	19	89
April	140	96	91	327	46	21	16	83
Total	2,121	1,020	986	4,127	543	244	238	1,025

Employment Service

The Institute co-operates with the national societies of civil, mining, and mechanical engineers in the operation of the Engineering Societies Employment Service with its main office in the Engineering Societies Building, New York. Offices are operated in Chicago and San Francisco also. In addition to the societies named, others co-operate in certain of the offices as follows: New York—Society of Naval Architects and Marine Engineers; Chicago—Western Society of Engineers; San Francisco—California Section of the American Chemical Society; and the Engineers' Club of San Francisco.

The service is supported by the joint contributions of the societies and their individual members who are benefited. In addition to the publication of the employment service announcements monthly in Electrical Engineering, weekly subscription bulletins are issued for those seeking positions.

An analysis of this employment service as reported to the national societies is given in table XIII.

American Engineering Council

The American Engineering Council has continued to carry on a wide range of activities within the scope of its objectives: "to further the public welfare wherever technical and engineering knowledge and experience are involved, and to consider and act upon matters of common concern to the engineering and allied technical professions."

The 19th annual meeting of the assembly was held in Washington, D. C., January 12–14, 1939. The 9th conference of engineering society secretaries was held on the 12th.

During 1938, the Council sponsored two forums which were held with the co-operation of local organizations of engineers, the first in Philadelphia, May 13, on the subject "Employment and the Engineer's Relation to It," and the second in Detroit, November 11, on "Invention and the Engineer's Relation to It." On account of the success of these two meetings, the annual meeting was planned as a series of forums on the following subjects, suggested by the work of several of the Council's committees:

1. National Planning and the Engineer's Relation to It.
2. The Economic Status of the Engineering and Kindred Professions.
3. Engineering Aspects of Government Reorganization.
4. Engineering and Economic Factors in the Size of Business.

At the all-engineers dinner, Dr. Vannevar Bush, president of the Carnegie Institution of Washington, and a director of the AIEE, delivered an address on "The Qualities of a Profession," which was published in the April 1939 issue of Electrical Engineering, pages 156–60.

The business session was devoted to consideration of committee reports and other matters, as well as the financial status of the Council. It was reported that the National Industrial Conference Board had recently granted $22,500 for the conduct of a special study under the direction of the Council's subcommittee on patents.

United Engineering Trustees, Inc.

This organization is the corporate body which holds title in the name of the four Founder Societies to their joint physical properties, namely, the Engineering Societies Building, the Engineering Societies Library, and the endowment funds of The Engineering Foundation. It operates and manages the Engineering Societies Building and administers certain joint activities of the four Founder Societies.

The annual report of the UET for the year which ended September 30, 1938, showed practically full occupancy of the building, gratifying use of meeting halls, and a stable financial situation.

An abstract of the report was published in Electrical Engineering for December 1938, pages 518–19.

Engineering Foundation

The Engineering Foundation is a joint organization of the national societies of civil, mining and metallurgical, mechanical, and electrical engineers established for "the furtherance of research in science and engineering, and the advancement in any other manner of the profession of engineering and the good of mankind."

The Foundation's funds were augmented by a bequest from the late Ambrose Swasey of $86,977.16, this being the fifth of his gifts to the Foundation. The total book value of the Foundation fund is now $957,698.

The Foundation has been assisting in a wide range of technical researches sponsored by the founder societies. Some of the principal groups now in progress are: ASCE—soil mechanics and foundations, hydraulics; AIME—alloys of iron, barodynamic problems; ASME—critical pressure steam boilers, fluid meters, lubrication, cottonseed processing, rolling steels; AIEE—stability of impregnated paper insulation; AIEE and AWS—welding; University of California—plastic flow of concrete; New York University—wind direction and velocity.

Assistance in non-technical matters related to engineering has been granted to the Engineers' Council for Professional Development and the Personnel Research Federation.

An abstract of the annual report of the Engineering Foundation for the year which ended September 30, 1938, was published in the December 1938 issue of Electrical Engineering, pages 519–21.

Engineering Societies Library

The Engineering Societies Library, which was formed by combining the separate libraries of the four national societies of civil, mining and metallurgical, mechanical, and electrical engineers, and the preparation of a composite card catalog, has been expanded as a single engineering library, which probably constitutes the best collection of its type in the United States.

On September 30, 1938, the library had 144,262 volumes, 7,408 maps, 4,391 bibliographies. Books, pamphlets, and maps totaling 14,041 were received during the year ending at that time. Current issues of 1,369 periodicals were received. Work progressed rapidly on a classified index to periodicals, and the index now contains more than 225,000 references to articles published since 1927.

Special services rendered by the library include: photoprints, searches, abstracts, translations, bibliographies, book loans by mail, etc. An abstract of the annual report of the library appeared on page 519 of Electrical Engineering for December 1938.

Engineers' Council for Professional Development

This council was organized in 1932 to engage in activities leading to the enhancement of the professional status of the engineer. It includes three representatives of each of the seven participating organizations: the national societies of chemical, civil, electrical, mechanical, and mining and metallurgical engineers, the Society for the Promotion of Engineering Education, and the National Council of State Boards of Engineering Examiners.

The principal activities of ECPD include programs for the guidance of young individuals thinking of entering the engineering field, the accrediting of curricula of engineering schools, encouragement and assistance to individuals in their engineering and cultural studies during several years after graduation, and the development of criteria for indicating the attainment of the status of an engineer.

At the annual meeting held on October 22, 1938, the committee on engineering schools submitted recommendations on 63 additional curricula, bringing the total number to 679 curricula in 136 institutions. Of these, 392 were accredited, 107 were accredited provisionally, 179 were not accredited, and action on one was deferred. The complete list of curricula thus far accredited appeared in the December 1938 issue of Electrical Engineering, page 514.

Comprehensive information regarding other committee reports and the various other matters considered and acted upon at the annual meeting appeared in the same issue, pages 513–16.

Representatives

The Institute has continued its representation upon many joint committees and national bodies, with which it co-operates in a wide range of activities of interest and importance to engineers and others.

A list of representatives was published in the September 1938 issue of Electrical Engineering and in the 1939 Year Book.

Finance Committee

The committee, as usual, recommended a detailed budget to the board of directors, passed upon the expenditures for various purposes, made recommendations regarding delinquent members, and performed the other duties prescribed for it in the constitution and bylaws.

Haskins and Sells, certified public accountants, have audited the books, and their report follows.

The board of directors extends to the national committees, and the District, Section, and Branch officers its deep appreciation of their continuing effective services, which have produced a high degree of activity, enthusiasm, and constructive accomplishment in all phases of Institute work. The board of directors has been much encouraged by the generous support which the members have given its efforts on their behalf.

Respectfully submitted for the board of directors.

H. H. Henline,
National Secretary

May 26, 1939

HASKINS & SELLS
CERTIFIED PUBLIC ACCOUNTANTS

22 EAST 40TH STREET
NEW YORK

May 18, 1939

American Institute of Electrical Engineers,
33 West 39th Street, New York.

Dear Sirs:

We have made an examination of your balance sheet as of April 30, 1939, and of your recorded cash receipts and disbursements for the year ended that date. In connection therewith, we examined or tested your accounting records and other supporting evidence in a manner and to the extent which we considered appropriate in view of your system of internal accounting control. We present the following financial statements:

Balance Sheet, April 30, 1939 (Exhibit A).
Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value (Schedule 1).

Statement of Recorded Cash Receipts and Disbursements of General Fund for the Year Ended April 30, 1939 (Exhibit B).

Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1939 (Exhibit C).

In accordance with the terms of our engagement, members and other debtors were not requested to confirm to us the amounts receivable from them at April 30, 1939, and, in accordance with the usual practice of the Institute, no provision has been made for dues which may prove to be uncollectible.

In our opinion, based upon such examination and subject to the comments in the next preceding paragraph, the accompanying Exhibit A fairly presents your financial condition at April 30, 1939, and the accompanying Exhibits B and C fairly present your recorded cash receipts and your disbursements of funds, as indicated, for the year ended that date.

Yours truly,

Haskins & Sells

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Balance Sheet, April 30, 1939

Exhibit A

ASSETS		
Property Fund Investments:		
One-fourth interest in real estate and other assets of United Engineering Trustees, Inc., exclusive of trust funds	$498,448.48	
Equipment:		
Library—volumes and fixtures	37,296.37	
Office furniture and fixtures (less reserve for depreciation, $29,255.45)	5,398.38	
Works of art, etc	3,001.35	
Securities—at cost (market quotation value, $9,597.17)—Schedule 1	10,032.17	
Cash (see Exhibit C)	32.83	
Total property fund investments		$554,209.58
Restricted Fund Investments:		
Securities—at cost, less reserve for securities of doubtful value (Market quotation value, $178,881.07)—Schedule 1	$195,994.82	
Cash (see Exhibit C)	15,738.53	
Accrued interest receivable	145.16	
Total restricted fund investments		211,878.51
Current Assets:		
Cash (see Exhibit B)	$ 61,988.30	
Accounts receivable:		
Members—for dues	19,733.35	
Advertisers	389.25	
Miscellaneous	2,419.80	
Accrued interest on investments	1,733.97	
Inventories:		
TRANSACTIONS, etc	1,984.00	
Text and cover paper	5,560.65	
Work in process (May issue of ELECTRICAL ENGINEERING)	3,061.14	
Badges	920.92	
Total current assets		97,791.38
Total		$863,879.47

LIABILITIES		
Property Fund Reserve		$554,209.58
Restricted Fund Reserves:		
Reserve Capital Fund	$191,634.04	
Life Membership Fund	9,168.34	
International Electrical Congress of St. Louis Library Fund	5,521.17	
Lamme Medal Fund	4,514.73	
Mailloux Fund	1,040.23	
Total restricted fund reserves		211,878.51
Current Liabilities—Accounts payable		11,423.26
Deferred Income:		
Dues received in advance	$ 3,792.27	
Entrance fees and dues advanced by applicants for memberships	539.66	
Deferred credits and other unallocated receipts	428.32	
Subscriptions for TRANSACTIONS received in advance	8.27	
Reserve for prepaid subscriptions for ELECTRICAL ENGINEERING	8,700.00	
Total deferred income		13,468.52
Surplus		72,899.60
Total		$863,879.47

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value, April 30, 1939

Exhibit A, Schedule 1

			Restricted Funds					
	Face Value of Bonds or Number of Shares of Stock	Property Fund (Equipment Replacements)	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total
Railroad Bonds:								
Alleghany Corporation 20-year collateral trust convertible 5%, due 1949	$15,000.00		$ 10,627.50					$ 10,627.50
Baltimore & Ohio Railroad Company 6% refunding and general mortgage series C, due 1995 (certificates of deposit)	12,000.00		8,940.00			$4,330.00		13,270.00
Central of Georgia Railway Company 5% consolidated mortgage, due 1945	3,000.00		1,477.50					1,477.50
Chicago, Burlington & Quincy Railroad Company 5% first and refunding mortgage series A, due 1971	1,000.00		1,010.00					1,010.00
Chicago & Erie Railroad Company 5% first mortgage, due 1982	1,000.00		1,105.00					1,105.00
Chicago & Northwestern Railway Company 6½%, due March 1, 1936	9,000.00		7,202.50					7,202.50
Cleveland Union Terminals Company 5% sinking fund series B, due 1973	4,000.00	$ 4,010.00						
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974 (certificates of deposit)	10,000.00		9,818.75					9,818.75
New York Central Railroad Company 5% refunding and improvement mortgage series C, due 2013	6,000.00		5,742.50					5,742.50
Northern Pacific Railway Company 6% refunding and improvement mortgage series B, due 2047	10,000.00		10,962.50					10,962.50
Pennsylvania Railroad Company 30-year secured serial 4%, due 1944	6,000.00		5,337.50		$1,067.50			6,405.00
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950 (certificates of deposit)	6,000.00		5,497.50					5,497.50
Southern Pacific Company Oregon Lines 4½% first mortgage series A, due 1977	1,000.00				996.25			996.25
Texas and Pacific Railway Company general and refunding series B 5%, due 1977	5,000.00			$5,306.25				5,306.25
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00		7,225.00					7,225.00
Total railroad bonds—(FORWARD)		$ 4,010.00	$ 74,946.25	$5,306.25	$2,063.75	$4,330.00		$ 86,646.25

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Property and Restricted Funds Securities, Less Reserve for Securities of Doubtful Value, April 30, 1939

Exhibit A, Schedule 1 (Concluded)

	Face Value of Bonds or Number of Shares of Stock	Property Fund (Equipment Replacements)	Restricted Funds					
			Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total
Total Railroad Bonds—FORWARD		$ 4,010.00	$ 74,946.25	$5,306.25	$2,063.75	$4,330.00		$ 86,646.25
Public Utility Bonds:								
American Gas & Electric Company 5% debenture, due 2028	6,000.00		$ 6,397.50					$ 6,397.50
Monongahela-West Pennsylvania Public Service Company 6% debentures, due 1965	8,000.00		8,660.00					8,660.00
New York Telephone Company first and general mortgage $4^{1}/_{2}$%, due 1939	1,000.00						$1,000.00	1,000.00
Total public utility bonds			$ 15,057.50				$1,000.00	$ 16,057.50
Industrial and Miscellaneous Bonds, Etc.:								
Fidelity Union Title and Mortgage Guaranty Company first mortgage certificates (on property 75–79 Prospect Street, East Orange, N. J.), 4%, due 1944	$14,507.49	$ 967.17	$ 13,540.32					$ 13,540.32
General Motors Acceptance Corporation $3^{1}/_{4}$%, due 1951	7,000.00		7,140.00					7,140.00
United States Steel Corporation debentures $3^{1}/_{4}$%, due 1948	8,000.00		8,240.00					8,240.00
Total industrial and miscellaneous bonds, etc		$ 967.17	$ 28,920.32					$ 28,920.32
Municipal Bonds—New York City $4^{1}/_{2}$% corporate stock, due 1957	2,000.00				$2,204.05			$ 2,204.05
United States Government Bonds and Notes:								
Federal Farm Mortgage 3%, due 1949/44	12,000.00		$ 12,405.00					$ 12,405.00
Treasury Bonds $3^{1}/_{4}$%, due 1941	10,000.00		10,650.00					10,650.00
Treasury Bonds $2^{3}/_{4}$%, due 1947/45	10,000.00		10,409.38					10,409.38
Treasury Bonds $3^{3}/_{8}$%, due 1943/40	10,000.00		10,537.50					10,537.50
Treasury Bonds $3^{3}/_{8}$%, due 1943/41	10,000.00		10,681.25					10,681.25
Treasury Notes 2% series B, due September 15, 1942	21,000.00		21,756.57					21,756.57
Total United States Government bonds and notes			$ 76,439.70					$ 76,439.70
Capital Stocks:								
Commonwealth Edison Company	48 shares		$ 2,892.00					$ 2,892,00
Commercial Investment Trust Corporation $4^{1}/_{4}$% preferred, series of 1935	100 "		10,100.00					10,100.00
Consolidated Edison Company of New York, Inc. $5.00 cumulative preferred	30 "	$ 3,060.00						
International Match Realization Co., Ltd. voting trust certificates for capital shares of International Match Corporation	6 "		2,274.15					2,274.15
Public Service Corporation of New Jersey $5.00 preferred	30 "		2,958.75					2,958.75
United Gas Improvement Company $5.00 preferred	30 "	1,995.00	997.50					997.50
Total capital stocks		$ 5,055.00	$ 19,222.40					$ 19,222.40
Total		$10,032.17	$214,586.17	$5,306.25	$4,267.80	$4,330.00	$1,000.00	$229,490.22
Less Reserve for Securities of Doubtful Value:								
Central of Georgia Railway Company 5% consolidated mortgage, due 1945	$ 3,000.00		$ 1,477.50					$ 1,477.50
Chicago & Northwestern Railway Company $6^{1}/_{2}$%, due March 1, 1936	9,000.00		7,202.50					7,202.50
Florida East Coast Railway Company 5% first and refunding mortgage series A, due 1974	10,000.00		9,818.75					9,818.75
International Match Realization Company, Ltd. voting trust certificates for capital shares of International March Corporation	6 shares		2,274.15					2,274.15
St. Louis-San Francisco Railway Company 5% prior lien mortgage series B, due 1950	$ 6,000.00		5,497.50					5,497.50
Western Pacific Railroad Company 5% series A, due 1946 (stamped)	15,000.00		7,225.00					7,225.00
Total reserve for securities of doubtful value			$ 33,495.40					$ 33,495.40
Total Securities, Less Reserve		$10,032.17	$181,090.77	$5,306.25	$4,267.80	$4,330.00	$1,000.00	$195,994.82
Total Property Fund Securities		$10,032.17						
Total Restricted Funds Securities			$181,090.77	$5,306.25	$4,267.80	$4,330.00	$1,000.00	$195,994.82

AMERICAN INSTITUTE OF ELECTRICAL ENGINEERS

Statement of Recorded Cash Receipts and Disbursements of General Fund for the Year Ended April 30, 1939

Exhibit B

Cash on Deposit With The National City Bank of New York, May 1, 1938		$ 49,616.89
Receipts:		
Dues (including $86,226.00 allocated to Electrical Engineering subscriptions)	$191,719.44	
Advertising	26,360.83	
Transactions subscriptions	8,262.45	
Electrical Engineering subscriptions	14,990.85	
Miscellaneous publications (preprints, standards, etc.)	8,912.95	
Student fees	11,297.50	
Entrance fees	7,023.70	
Membership badges	1,808.40	
Transfer fees	770.00	
Interest on investments, less purchased interest	8,981.91	
Miscellaneous	16.90	
Total receipts		280,144.93
Total		$329,761.82
Disbursements:		
Publication expense:		
Electrical Engineering	$ 72,893.80	
Transactions	8,843.13	
Year Book	6,567.96	
Miscellaneous publications (preprints, standards, etc.)	12,323.68	
Institute meetings	13,623.38	
Institute Sections	33,853.28	
Institute Branches	3,265.05	
Edison Medal committee	299.11	
Finance committee	1,600.00	
Headquarters committee	34.75	
Membership committee	8,082.47	
Standards committee	6,413.15	
Technical committee	286.41	
Traveling expenses:		
Geographical Districts:		
Executive committees	2,890.85	
Vice-presidents	365.79	
Branch counselors	8,036.08	
President's appropriation	34.85	
Board of directors	3,274.22	
National nominating committee	1,020.79	
Forward	$183,717.75	$329,761.82

Total—(Forward)		$329,761.82
Disbursements—(Forward)	$183,717.75	
Administrative expenses	46,221 53	
Geographical Districts—best paper prizes	185.50	
Geographical Districts—initial paper prizes	85.50	
Institute prizes	109.00	
American Engineering Council	10,500.00	
American Standards Association	1,500.00	
United Engineering Trustees, Inc.:		
Building assessment	10,984.80	
Library assessment	9,689.28	
Engineering Societies Employment Service	1,512.77	
Engineers' Council for Professional Development	435.00	
Engineering Foundation Projects:		
Welding research	250.00	
Research on impregnated paper insulation	250.00	
John Fritz Medal	201.92	
National Fire Protection Association—dues	60.00	
United States Committee of International Commission on Illumination	300.00	
Membership badges	1,503.97	
Legal services	250.00	
Miscellaneous	16.50	
Total disbursements		267,773 52
Cash on Deposit With The National City Bank of New York, April 30, 1939		$ 61 988.30

Statement of Recorded Cash Receipts and Disbursements of Property and Restricted Funds for the Year Ended April 30, 1939

Exhibit C

		Restricted Funds					
	Property Fund (Equipment Replacements)	Reserve Capital Fund	Life Membership Fund	International Electrical Congress of St. Louis Library Fund	Lamme Medal Fund	Mailloux Fund	Total Restricted Funds
Cash on Deposit With East River Savings Bank and The National City Bank of New York, May 1, 1938	$22.47	$ 82.06	$3,794.44	$1,024.19	$148.03	$22.23	$ 5,070.95
Receipts:							
Interest on bonds, and dividends on stocks			$ 250.00	$ 175.00	$240.00	$45.00	$ 710.00
Interest on bank balance			73.66				73.66
Proceeds from sale and redemption of securities	$10.36	$53,639.03					53,639.03
Life membership fee			288.51				288.51
Total receipts	$10.36	$53,639.03	$ 612.17	$ 175.00	$240.00	$45.00	$54,711.20
Total	$32.83	$53,721.09	$4,406.61	$1,199.19	$388.03	$67.23	$59,782.15
Disbursements:							
Annual withdrawal authorized in by-laws			$ 565.35				$ 565.35
Gold and bronze replicas of Lamme Medal and certificate					$239.30		239.30
Purchase of securities		$43,177.82					43,177.82
All other disbursements				$ 11.65		$49.50	61.15
Total disbursements		$43,177.82	$ 565.35	$ 11.65	$239.30	$49.50	$44,043.62
Balance on Deposit With East River Savings Bank and The National City Bank of New York, April 30, 1939	$32.83	$10,543.27	$3,841.26	$1,187.54	$148.73	$17.73	$15,738.53